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WCAP-3251-1

(UC-80 REACTOR TECHNOLOGY)

LIQUID METAL FAST BREEDER REACTOR DESIGN STUDY

MASTER

**WESTINGHOUSE ELECTRIC CORPORATION
ATOMIC POWER DIVISION**

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MADE JUNE 6, 1964.

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LIQUID METAL FAST BREEDER REACTOR DESIGN STUDY

FINAL REPORT

PREPARED FOR:

DIVISION OF REACTOR DEVELOPMENT

U. S. ATOMIC ENERGY COMMISSION

Contract No. AT(30-1)-3251

With New York Operations Office, U.S.A.E.C.

January, 1964

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MODULAR CORE ARRAY

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ABSTRACT

A conceptual design for a carbide fuel 1000 MWe fast breeder reactor has been developed which provides safe and stable operation based on a combination of inherent reactivity coefficients, a controlled expansion fuel assembly, and a modular core. This modular core design can be prototyped in existing fast reactors.

The fuel cycle cost of .28 mills/kwh is achieved with a breeding ratio of 1.57 and a doubling time of 10.5 full power years.

The safety of the reactor is assured by a negative Doppler coefficient, a controlled expansion fuel assembly which provides a negative coefficient of reactivity, and the modular design which provides neutronics similar to a small reactor. The fuel is separated into multiple compartments to minimize the effect of fuel movement.

The mixed carbide fuel is bonded by sodium to the clad, and is vented to the coolant. This design permits high heat fluxes with low fuel temperatures, release of fission gas pressure, space for fuel swelling, and does not require the neutron embrittled clad to accept large strain at the end of a 100,000 MWD/T fuel life.

This modular design is a safe design for an economic large core, and provides the important advantage that it can be prototyped in either the EBR-2 or the Fermi reactors. This would allow early testing of this design. The economics and breeding ratio are based on realistic calculations which can be brought to fruition through a development program which, because of the modular concept, will suffice for the design of reactors from 150 MWe to more than 5000 MWe.

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INTRODUCTION

LIQUID METAL FAST BREEDER REACTOR DESIGN STUDY

INTRODUCTION

Authorization

On June 10, 1963 the New York Operations Office awarded a study contract for the "Conceptual Design of a 1000-Megawatt Fast Breeder Reactor" to the Atomic Power Division of the Westinghouse Electric Corporation. The study was authorized under Contract No. AT(30-1)-3251 which was administered by the AEC New York Operations Office, with technical guidance supplied by the AEC Chicago Operations Office.

Project Objective

The project objective, as defined in the contractual document previously cited, is as follows:

"An over-all objective of the Fast Reactor Program is to develop the technology necessary to produce economic electric power and to conserve the supply of nuclear material. In this connection, the objective of this project is to obtain a conceptual design of a fast breeder sodium cooled reactor suitable for use in a plant of 1000 MWe (net) capacity, turbine inlet steam temperature of 1000°F, which would have as a goal the production of electric power at minimum electric costs consistent with a breeding ratio of approximately 1.20 and a reasonable fuel doubling time."

Scope

The study contract describes the scope of the design in the following manner:

"A conceptual design will be developed and optimized, insofar as possible to yield minimum fuel cycle costs, with proper consideration for the effect on capital costs. It will describe the reactor core

geometry, physics and thermal characteristics, reactor vessel, control and safety mechanical and instrumental devices, fuel element subassemblies, fuel handling mechanisms, and will indicate the heat rating and sodium flow rates of the heat removal system.

"The Study will include analyses, together with supporting calculations, of the static and dynamic nuclear and thermal behavior of the reactor, including, but not limited to, the following coefficients of reactivity: fuel expansion, sodium void, Doppler, internal fuel movement, and long-term changes. Estimates will be made of the uncertainty in the nuclear and thermal parameters, including the above coefficients and the significance of the uncertainty on the static and dynamic behavior of the reactor. Information will be developed on safety characteristics of the reactor system. The number of primary system heat transfer loops will be selected with due consideration of economics and plant reliability. The R & D program necessary to determine the practicality of this design will be described."

Ground Rules

The following are the ground rules by which this design study was performed. They were developed by the Chicago Operations Office of the Atomic Energy Commission and were incorporated into the contract document.

- "a. The reactor will be fueled with plutonium and uranium carbide. The uranium will be depleted uranium (assumed to contain 0.3 w/o U-235).
- b. The coolant will be sodium.
- c. Breeding ratio is defined as the ratio of the weight of fissionable material formed from fertile material, to the weight of fissionable material destroyed by fission and capture over the lifetime of the fuel. Only Pu-239, Pu-241, and U-235 will be considered fissionable.
- d. The reactor fuel cycle will be at equilibrium with respect to isotopic compositions of fuel and fertile material and will need only fertile material makeup from outside the fuel and blanket cycle.

- e. The mixed mean reactor outlet sodium temperature shall be 1200°F or the highest achievable temperature which is compatible with the use of a stainless steel for fuel cladding with due consideration for hot channel factors, variations in flow distribution, and over power allowances.
- f. The reactor thermal energy output required to produce 1000 MWe (net) shall be assumed to be 2500 MWt.
- g. Design and cost estimating shall be based on the design of a prototype beginning in FY 1968 with power generation in FY 1972.
- h. The breeding ratio of 1.2 does not include reprocessing or refabrication losses. Fuel and/or fertile material reprocessing and fabrication may be either on or off site. Fuel reprocessing costs may be taken from the latest costs published in the Federal Register. If reprocessing costs different from these are assumed, the technical and economic bases must be fully developed. Shipping costs will be by rail between St. Louis, Missouri, as the reactor site and Augusta, Georgia, as the reprocessing site.
- i. Ownership of both fissionable and fertile material is to be assumed to rest with the AEC.
- j. The plutonium value to be used will be \$10.00 per gram of plutonium-239 plus plutonium-241 as nitrate.
- k. Interest rate on fuel fabrication costs shall be assumed to be 6% of the book value. The book value of the fabrication costs shall be assumed to decrease linearly from the initial value to zero during the period of the fuel's residence in the core."

Report Plan

This report "Liquid Metal Fast Breeder Reactor Design Study" is presented in four principal sections:

I. Summary of Results

This section contains synopses of the reference plant design, cost analysis, and the proposed research and development program.

II. Reference Plant Design

The reference plant design is described in detail and is organized by functional groupings:

Nuclear Design

Thermal and Hydraulic Analysis

Mechanical Design

Transient Analysis, Safety, and Reactor Control

System and Components Design

Materials Analysis

III. Cost Analysis

This section provides the fuel cycle cost analysis and estimated capital costs of reactor major components, as specified in Contract No. AT(30-1)-3251.

IV. Research and Development Program

A proposed R & D program to establish practicability of the design is described.

I. SUMMARY OF RESULTS

I. SUMMARY OF RESULTS

The Westinghouse 1000 MWe fast breeder reactor conceptual design described in this report is based on plutonium-uranium carbide fuel, contained in controlled expansion fuel assemblies (Figure I-1) and incorporated into a modular core. Fuel cycle costs are 0.28 mills/kwh with a breeding ratio of 1.57 and a doubling time of 10.5 full power years.

The reactor design consists of an array of identical modules (7 modules for 1000 MWe). Each module consists of a central fuel region within a blanket, surrounded by a mechanical and thermal barrier of graphite. An artist's conception of the full array is shown in the frontispiece. The fuel is mixed plutonium-uranium carbide pellets clad in stainless steel with an integral axial blanket of depleted uranium carbide. The radial blanket is composed of uranium oxide pellets clad in stainless steel.

The primary sodium enters the bottom of each module at 979°F and exits at a mixed mean temperature of 1200°F . The sodium is then pumped through the two intermediate heat exchangers and returned to the reactor.

The basic design criteria are safety, economics, and breeding in that order, applied to a reactor concept which is realistic based on reasonable developments from today's technology.

The time schedule for proof testing can be faster than for previous concepts because the unit module can be prototyped in Fermi or EBR-II, and the kinetic characteristics do not depend solely on Doppler for self limiting.

Safety considerations have motivated a) a modular core, which provides neutronics similar to a small reactor in a reactor of large size, b) a negative Doppler coefficient, c) a controlled expansion fuel assembly which provides an inherent negative reactivity coefficient by fuel motion, d) fuel rod separation into multiple compartments to minimize the effect of fuel movement, e) sodium bonding to maintain low fuel temperature and thus inhibit fuel motion by preferential diffusion, f) a loss of reactivity upon complete loss of sodium and a sodium void reactivity effect of less than a

CEX (CONTROLLED EXPANSION) FUEL ASSEMBLY

Figure I-1 CEX (Controlled Expansion) Fuel Assembly

dollar under the worst possible conditions, g) a design to encourage the fast fission of U^{238} so as to increase the overall delayed neutron fraction, h) a coupling between modules which will decrease the positive effect of reactivity added inadvertently to a single module and thus partially compensate for the small delayed neutron fraction and the short fast neutron lifetime, and i) moderation of radial blanket neutrons which provide increased low energy fissions, and some of the benefits of the slow-fast reactor concept.

Economic considerations have resulted in long fuel cycle lifetime, 100 MWD/kg, and sodium bonded pellets, vented to the coolant. The sodium bonding, plus the high thermal conductivity of the carbides, permits low fuel temperatures. The low fuel temperatures reduce fuel swelling and thus allow longer fuel life. The sodium bond allows the use of unground pellets, and permits fuel swelling to be accommodated, with associated cost reductions in fabrication, loss of fissionable materials and allowable lifetime. Vented fuel eliminates fission product gas pressure buildup, hence does not require the neutron embrittled clad to accept large strain at the end of fuel life. The presence of some fission products in the primary sodium adds only a small increment to the cost of maintaining a sodium system, which is already highly radioactive and on a time schedule consistent with 1000 MWe downtime costs. Oxide fuel is specified in the radial blanket because carbide properties are not required there and the oxide is cheaper to fabricate. Moderation between modules provides better heat transfer conditions by flattening blanket power distribution.

A high breeding ratio is desirable from economic considerations, for conservation of natural resources, and to flatten out the reactivity vs. burnup curve for control and safety reasons. The mixed carbide fuel is particularly well adapted for providing a high breeding ratio. The 1.57 breeding ratio of this design provides a doubling time of 10.5 full power years and a 0.55 mills/kwh credit for plutonium production. Breeding ratios above 1.6 can be obtained but the 1.57 was selected as an economic compromise.

Modular core geometry enhances the breeding by providing that most of the radial blanket assemblies, with their high fertile material density, are

exposed to core leakage flux from two sides. The graphite barrier also provides local blanket moderation which enhances the competition of fertile material for neutrons without softening the spectrum of the core region.

The degree of coupling between modules is sufficiently small that the reactor characteristics can be determined in a prototype. Furthermore, having accomplished the necessary research and development work for a single size of reactor, the same basic work suffices for a wide range of sizes. For example, fuel element development for a range of sizes from 150 MWe to greater than 5000 MWe could be covered by a single development program. Because the core height is almost maximum for single stage pumps, and the assembly size is set by the module diameter, a single fuel assembly design could suffice for this entire range of sizes. This is important to the achievement of low fuel fabrication costs.

The economic potential of this concept is so attractive that the research and development required to bring it to fruition should be vigorously pursued. Prototype plant construction could be begun within three years.

I.1 BASIC DESIGN CONSIDERATIONS

The design philosophy for this study is predicated upon the following considerations:

1. Maximum utilization of existing technology;
2. Adoption of advanced concepts that offer significant technological and economic gains and reasonable probability of achievement;
3. Recognition of calculational uncertainties and observance of a prudent course with respect to safety; and
4. Exploitation of the economic potential of the large fast breeder power plant to the maximum, consistent with the above considerations.

The objective of this study is the development of a conceptual design for the nuclear portion of a 1000 MWe fast breeder power reactor such that a prototype plant could be under construction before 1969, and a large power demonstration reactor under construction before 1975. The design meets the five essential goals:

1. The reactor must be safe;
2. The reactor must utilize (Pu, U) C fuel;
3. The reactor must have a high breeding ratio (1.2 or greater) coupled with a short doubling time;
4. The fuel cycle cost must be low; and
5. The primary sodium outlet temperature must be high (1200°F).

I.2 REFERENCE REACTOR DESIGN

The total reactor core is comprised of seven identical hexagonal modules. Each of these modules is composed of a central region of fuel and axial blanket (see Frontispiece) and an annular radial blanket region, with the whole surrounded by an annular barrier of graphite. Each module is approximately 8.5 feet tall by 4.5 feet (across the flats).

The fuel is an intimate mixture of plutonium and uranium carbides, with the composition stabilized just below the stoichiometric composition by the addition of a small quantity of iron. This iron addition combines with the free plutonium and uranium metal to form a high melting point alloy, so that the free metal cannot combine with the clad. The carbon content of the fuel is then high enough to obtain good irradiation performance, but is sufficiently low that carbon will not migrate to the clad.

The stabilized mixed carbides are pressed and sintered into pellets to approximately 90% of theoretical density, and are loaded into compartmented fuel rods of 316L stainless steel, 0.010 inches thick, and 0.300 inches in outer diameter. A radial gap of 0.006 inches, plus the initial internal porosity will absorb the anticipated fuel swelling. Sodium bonding is provided to permit this gap while operating the fuel at a maximum temperature of 2184°F. The fuel rods are vented in order to ensure that the highly irradiated clad does not experience large internal pressures, and can therefore continue to position the fuel throughout life. Depleted uranium carbide pellets are loaded into the same rods to form the axial blanket.

The fuel rods are brazed into hexagonal bundles measuring about 5 inches across the flats. These bundles are then fabricated into CEX (controlled expansion) fuel assemblies (see Figure I-1) to provide a negative power coefficient.

Thirty-six such assemblies comprise the core and axial blanket region of one module, with one control rod in the center and three control rods in the periphery.

The radial blanket consists of depleted uranium oxide pellets clad in 0.020 inches of 316L stainless steel, having an outside diameter of 0.464 inches. These rods are spaced by wire wrap and fabricated into assemblies. The radial blanket region of each module employs fifty-one such assemblies.

The graphite barrier consists of graphite encased in free-standing cans, anchored top and bottom to the reactor vessel. This serves to isolate the modules thermally and mechanically, and to modify the neutron spectrum in the outer blanket to improve the breeding.

A Summary Table of important reactor characteristics is found in Section II.1.4.

I.3 REFERENCE PLANT DESIGN

Figure II.1.4-1 schematically illustrates the reactor primary system.

Two intermediate heat exchangers are required, each fed by two primary variable speed pumps. No isolation or check valves are included in the main primary loops.

The cover gas system is a nearly-stagnant volume of argon gas which is utilized as a collecting volume for fission gases. Sodium level control and purging are combined to hold the gas storage decay volume at 1100 cubic feet.

Cold and hot traps are provided for impurity control and removal of fission products.

The reactor vessel is an 18-ft. diameter vessel with a 15-ft. diameter liner. The annulus is utilized as the sodium downcomer. Variable orifices throttle the sodium flow to adjust the relative flow between radial blanket regions and core regions.

The fuel handling machine is a traveling bridge crane device with remote locating and indicating devices. Visual operation is possible through a periscope. The fuel handling machine operates within a hot cell which also provides storage space for the vessel plug, and access to spent fuel storage.

The primary system is designed with the simplest mechanical equipment possible to minimize maintenance, and each item is removable remotely for decontamination if maintenance is required.

I.4 COST ANALYSIS

The fuel cycle cost analysis has been based on current AEC ground rules. Fabrication has been estimated on the basis of work previously reported by the AEC. A tabulation of selected capital cost items specified in the contract is presented in Section III.3.1.

The summary below presents the results of the fuel cycle analysis.

Reference Fuel Cycle Cost

Fabrication(\$189/kg core, \$50/kg bl.)	0.31
Working Capital (6%)	0.01
Inventory (\$10/g Pu at 4.75%)	0.32
Shipping	0.04
Processing (\$17,500/day)	0.15
Plutonium Credit (net)	<u>(0.55)</u>
Total	0.28

The importance of the fuel cycle cost computed does not lie in the exact magnitude, which will vary with the ground rules. Rather, it lies in the fact that, based on a reasonable set of ground rules, the calculated magnitude is so low that an overall change by a factor of two -- or even three -- would still leave the value attractive.

I.5 ADVANTAGES OF CONCEPT

Safe Design

The basic safety criteria have been met. Total loss of sodium results in a reduction in reactivity of 0.1%. The worst case of partial voiding results in an increase in reactivity of only 0.32% (75 cents). This would be more than compensated for by either Doppler or CEX separately, and the two together are greatly in excess of this.

The sodium temperature coefficient of reactivity is negative except with 4% plutonium in the blanket, when it is positive ($+0.45 \times 10^{-6}/^{\circ}\text{F}$), but is still an order of magnitude lower than either the Doppler or the CEX coefficients. As a result, it will not be possible to observe any significant effect from the sodium in the overall transient behavior of the reactor. The Doppler power coefficient becomes more negative as blanket power increases, providing an additional safety margin for long life.

Changes in reactivity due to preferential migration of plutonium during operation will be negligible due to the low fuel temperature. The absence of the "pore-sweeping" mechanism, seen in oxide, further ensures against plutonium migration.

Changes in reactivity due to fuel redistribution during operation will also be negligible. The normal fuel temperatures are sufficiently low that significant resintering is not likely to occur, and compartmentation restricts any such effect to a local region. Calculations show that the reactivity effect, if the fuel should redistribute within the compartments, is very small. The small dependence of reactivity on fuel distribution within the compartments results from the large radial leakage, characteristic of the small core module diameter.

The control requirements for burnup are small (less than 0.9% for six months), and Doppler plus CEX can readily provide more negative reactivity than this.

Sodium bonding permits the pellet-to-clad clearance to absorb fuel

swelling, hence the clad need not sustain large strains after high neutron flux exposure. This greatly reduces the probability of gross clad failure.

The proven sodium compatibility of carbide fuels minimizes the effect of clad failure. Clad failure on oxide fuel, for example, may result in reducing the fuel to a powder which the coolant would carry all through the primary system.

Compatibility of carbide fuel with stainless steel will be achieved by small additions of iron to the (slightly) hypostoichiometric fuel. This ensures that the clad will be neither carburized nor attacked.

The heavy-walled cans of graphite installed between modules are securely anchored at top and bottom, thus providing appreciable mechanical isolation between modules. Any mechanical, thermal, or neutron disturbance originating in one module would be highly damped by the graphite barrier between modules.

The Doppler effect is calculated to be strongly negative. The CEX fuel assembly provides an additional margin of fast-acting control beyond that produced by the Doppler effect. Partial or total loss of sodium enhances the CEX negative reactivity effect.

The modular array has several important safety characteristics which cannot be duplicated in single cores. The effective neutron lifetime is increased because some of the neutrons leave each module, traversing blanket and graphite, and result in fission in an adjacent module. The resulting fission neutrons must then return to the original module to maintain its neutron population. These time delays result in an increased effective neutron lifetime. Under equilibrium cycle conditions, each module behaves in a manner analogous to a slow-fast reactor. The blanket assemblies next to the graphite function as a very small thermal or resonance region, and the inner blanket assemblies function as a buffer region to protect the core spectrum. This results in a further increase in effective neutron lifetime.

Each module is slightly subcritical alone, so that even a complete failure of the entire control system of one module will not prevent shutdown of the reactor.

Each module does not display a kinetic behavior like that of a single core. If excess reactivity is inserted into one module, the total effective reactivity of that module decreases as soon as its power level rises. The leakage between modules provides this effect. The decrease in reactivity in the first module appears (after a small delay) distributed over adjacent modules. Thus the introduction of one dollar into one module does not make it behave like a prompt critical reactor: rather, it behaves more like a reactor with significantly less than one dollar insertion. This is accompanied by small increases in effective reactivity in adjacent modules. Thus each module, controlled separately, is much more tractable in operation than a single core operating in isolation. The mechanical and control separation of the modules makes them statistically independent for the analysis of most accidental reactivity insertions; and the reactivity sharing effect described above therefore results in a more sluggish response to such accidental insertions than a single core in isolation.

Breeding Ratio and Doubling Time

The breeding ratio is calculated to be 1.57. This high value results from a combination of several factors. The high uranium and plutonium density in carbide fuel provides a high density of fertile material, which results in fertile material competing very effectively for neutrons. Use of vented fuel permits clad-to-fuel volume ratios approaching those used for low burnup reactors, hence minimizes parasitic capture in the clad. The high radial leakage moves neutrons from the core into the blanket, where the fertile material concentration is even higher than in the core. The "internal" blanket regions greatly reduce neutron leakage out of the reactor. This factor alone contributes about 0.07 to the total breeding ratio.

The reactor has been optimized to achieve a minimum fuel cycle cost. Increasing the radial and axial blanket thicknesses by about six inches would increase the total breeding ratio by an amount of the order of 0.05. This, however, would increase both fuel cycle and capital costs of the plant. The breeding ratio could also be increased slightly by the use of fuel, rather than poison, for control.

The magnitude of the breeding ratio is believed realistic and is based on the best available data. The commonly used YOM cross section set predicts a larger breeding ratio, but it was discarded because it does not check with critical experiments as well as the WAPD-FI cross section set.

The doubling time -- the time required for the reactor to produce as much fissile material as required for the reactor inventory -- is just as important as breeding ratio. Based on reactor inventory only, the doubling time is 7.7 full power years; thus one reactor of this type will generate a full load of fuel from fertile material in 7.7 years. This figure includes a pessimistic 2% loss in each reprocessing cycle.

The doubling time based on the sum of core inventory and out of core inventory is dependent upon many factors in the recycle process. The reference value for this conceptual plant is 10.5 full power years. This value can be lowered by decreasing the time required for the recycle processes, or, as mentioned above, by increasing the breeding ratio.

Fuel Cycle Cost

The calculated fuel cycle cost is very low compared to that for thermal reactors or for fossil fuel. The value is sensitive to the many assumptions which lie behind the final figures. The significance of the important assumptions can be seen from the curves in Section III.3.2. A value of 0.28 mills/kwh with a possible range of 0.15 to 0.43 mills/kwh, is a best estimate, based on the present ground rules.

Expansion, Prototyping, and Critical Experiments

The modular island array concept is virtually unlimited in total output. Using the modules of the reference design, a wide range of sizes is available. For example, some convenient combinations are:

No. of modules	1	3	4	7	10	12	14	19	----	37
Electrical Output, MW	150,	450,	575,	1000,	1430,	1710,	2000,	2700	---	5300

Some of these combinations of modules may require oval vessels or a departure from the full annulus downcomer system used in the reference design. The low system pressure makes this feasible.

The physics, thermal, and hydraulic characteristics and the mechanical design of the modules in the whole range of sizes would be so nearly identical that no basically new phenomena would be encountered. Thus, once the necessary research and development work has been performed for one modular reactor, the basis for the design of the core portion of virtually any size plant would be established.

It follows directly that the modular island core is easily prototyped. This is a unique characteristic of this type of core. No large single core type can be accurately and completely prototyped in a small size.

Existing critical facilities can adequately mock-up a module. The difference in reactivity between a single isolated module and a module in a large array is only 0.0032 with a fresh blanket, increasing to about 0.025 with an equilibrium blanket.

Designs which depend exclusively on Doppler effect appear to require construction and operation of a large experimental reactor prior to the prototype. The CEX assembly can be demonstrated much more simply, and would permit construction and operation of a prototype before such extensive Doppler measurements were performed.

Early core tests of a single module in Fermi or EBR-II could be implemented provided the necessary design modifications are made to the reactors.

I.6 UNCERTAINTIES IN REFERENCE DESIGN

There is no area of technical or safety uncertainty which prohibits the early construction of a plant of the general configuration of the reference design. The significance of the uncertainties lies in their effect upon the economic attractiveness of the plant. These uncertainties can best be resolved by actual construction and operation of a prototype plant. Some of the major areas are noted below.

The temperature levels of the reference design are in excess of current technology. Based on the meager data currently available, the temperature levels proposed are possible; but the uncertainty is such that it might be necessary to drop the reactor temperatures by as much as 100^oF. The two major problem areas here are mass transfer of clad and lack of large mechanical pump experience.

Iron-stabilized carbide fuel has not been tested, and no other carbide fuel has been tested to the burnup levels proposed. Although analysis and extrapolation indicate feasibility, it has not as yet been demonstrated. Similarly, clad behavior, braze material, and CEX type assemblies have not yet been demonstrated.

The large components needed have not been developed but appear feasible.

Some of the desired instrumentation is not currently available. High temperature neutron detectors are preferred to avoid large instrumentation cooling requirements.

The magnitudes of the nuclear numbers have a greater uncertainty than would be the case for all uranium reactors. The small module size, however, brings the calculations closer to past and current experimental data than is the case for large single cores.

Transient studies have really only begun. The uncertainty here is primarily the question of how much better behaved is the reactor than overly conservative

initial studies indicate.

The conceptual design is based entirely on the equilibrium fuel cycle, consequently startup considerations may require some design modifications.

The design of fission product traps for the primary system is not well understood. Successful cleanup has been demonstrated, but adequate design methods must still be developed.

Two areas merit mention here because the available data may not be widely known. Behavior of the cermet rods in the CEX fuel assemblies is not uncertain, as data are available at the temperatures and burnups required. The stability and general behavior of loosely coupled fuel regions in a common blanket is not uncertain, because this was investigated by the Bettis Laboratory in connection with the Shippingport reactor. The differences between thermal and fast reactors do not influence the fundamental kinetic characteristics of the coupling.

1.7 RECOMMENDATIONS AND R & D PROGRAM

1.7.1 Recommendations

The economic and breeding potential of this modular concept is so great that the research and development required to bring it to fruition should be vigorously pursued.

It is recommended that a single module be tested in Fermi or EBR-II, where the basic physics of the design can be verified as well as fuel element behavior. Because of the combination of design features, the safety and control characteristics of one module are a small extrapolation from existing reactors, making such testing possible.

A prototype fast reactor is recommended to test the full concept, including the components and systems under design conditions. A single module plant would require only minor extrapolation from existing plant apparatus. A three-module plant could be built as a three-loop plant by reasonable extrapolation of existing equipment. The plant equipment at Hallam, EBR-II, and Fermi would all have produced several years of valuable operational experience before actual construction of the prototype equipment was begun.

Such a three-module power reactor could demonstrate the technical feasibility of very large fast breeder reactors before 1975, and would establish the engineering data sufficiently well to provide confidence in the overall economics of large fast breeder power plants.

The combination of a number of modules is more tractable than a single module, hence the degree of uncertainty of the transient behavior is small from the standpoint of safety. The only basic uncertainties are those related to the achievement of the potential economics. Since these can only be fully demonstrated in an operating reactor environment, it is recommended that construction of a prototype plant be begun at the earliest possible date. Such a plant could be in operation by 1970.

Mixed plutonium and uranium carbide fuel should be developed with a mixed oxide program continued as a backup. Based on a 1970 operation date, fuel

development could extend through 1967. Advanced cores would follow further development.

Neutron-controlled expansion of fuel should be investigated and developed in detail, and alternate designs explored as a backup.

In support of this practical engineering demonstration, a program of research and development must be conducted. Such a program is outlined briefly in the following section.

I.7.2 Research and Development Requirements

The research and development effort required to demonstrate the practicability of this 1000 MWe plant can be conveniently summarized under the categories of fuel, physics, transient studies, and plant prototyping. Note that the modular design permits full demonstration of practicability of all but large non-nuclear components in either a prototype, or in a medium-sized power reactor.

Establishment of the physical behavior of mixed carbide fuel to high burnups is needed to permit confident prediction of the fuel performance. The use of the stabilized mixed carbide must be demonstrated to high burnup. Fundamental mass transfer experiments are needed to firmly establish temperature limits. Additional work on braze materials is required to demonstrate or develop a braze alloy and technique compatible with sodium for the full fuel assembly life. Additional analysis and experimental verification of the behavior of CEX fuel assemblies are required.

Fundamental investigations must be continued in physics in order to achieve greater confidence in predictions about nuclear behavior. Additional basic cross-section measurements and critical experiments are required. These must be supplemented by further development of theoretical methods, particularly methods for evaluation of coefficients.

Extensive transient studies should be carried out. The first emphasis in these studies should be the establishment of criteria for accident analysis.

For example, the physical limitations on the rate at which reactivity can be introduced have not been established, but are required for rational transient analysis. The second step should be the investigation of a sufficient number of representative cases so that a clear understanding of the behavior of the plant under all conditions can be achieved.

The development and testing of large pumps, heat exchangers and steam generators must be conducted. The basic theory is well understood, but material selection and the practical engineering details required to reduce theory to practice are not yet established for large sodium components. High temperature nuclear instrumentation is feasible, but the development work remains to be performed. Additional development is required on cold and hot traps to better understand the mechanisms involved -- particularly for fission product removal.

Prototyping is required before a plant as large as 1000 MWe is committed for construction and operation at power.

II. REFERENCE PLANT DESIGN

II. REFERENCE PLANT DESIGN

II.1 PLANT AND REACTOR DESCRIPTION

II.1.1 Basic Design Considerations

The objective of this study is the development of a conceptual design for the nuclear portion of a 1000 MWe fast breeder power reactor. The design is such that a prototype plant could be under construction before 1969, and a large power demonstration reactor under construction before 1975. The design utilizes as much established technology as is feasible, while meeting the five essential goals:

1. The reactor must be safe;
2. The reactor must utilize (Pu,U)C fuel;
3. The reactor must have a high breeding ratio coupled with a short doubling time;
4. The fuel cycle cost must be low; and
5. The steam temperature must be high.

The first goal is over-riding. A sharp difference exists between fast reactors and typical thermal (water) reactors. A severe excursion in a large water reactor can always be terminated by expulsion of the water. The fuel enrichment is such that the fuel then becomes completely inert and the dry fuel cannot be reassembled into a critical array. The same is not usually true for a fast reactor, where the core fuel can be reassembled into a more reactive configuration than the original array, dry or not. Thus the events subsequent to a major accidental excursion in a fast reactor may be significantly different from those in a water reactor. Further, in some large fast cores, loss of coolant in itself results in a large increase in reactivity.

The plant will be engineered to reduce the probability of loss of sodium from the core to the lowest realistically attainable level. Because the probability of sodium void formation cannot be exactly zero, the accident considerations mentioned above make it prudent to design to minimize the consequences of sodium loss or void.

Reasonable design criteria have been chosen for sodium effects. Total loss of sodium should yield a reduction in reactivity. The worst case of partial loss should not introduce more reactivity than can be over-ridden by inherent prompt coefficients, and should preferably be less than one dollar of reactivity. The sodium temperature coefficient should be negative; or, if positive, be directly over-ridden by a prompt negative effect.

The second goal--the use of the mixed carbide as a fuel--is an important one. The obvious advantage of high thermal conductivity permits low average fuel temperature, with a large margin to melting for the Doppler coefficient to act upon, and reduces the probability of plutonium migration and fuel slumping. Low vapor pressure further reduces the probability of plutonium migration. The high (Pu,U) density of the carbide enhances the competition of fertile material capture over structural material and increases the breeding ratio. The demonstrated long-term compatibility of carbide with sodium permits sodium bonding, with the associated low pellet surface temperature. This, combined with the low average fuel temperature, permits the use of unground pellets without clad swaging, and high burnup can thus be obtained without requiring large terminal strain in the clad.

The third and fourth goals are sufficiently obvious that no amplification is required at this point.

The fifth goal--high steam temperature-- is essential for high efficiency. The fuel cycle cost is sufficiently low that a change in efficiency of 10%, for example, would have a very small effect on total power cost. But a change in efficiency of 10% alters the size of the entire plant from reactor core through pumps and heat exchangers to the turbine. Even the off-gas system and containment building are affected. Thus a 10% reduction in efficiency, for instance, tends to increase the plant capital by 3 to 5%. Ultimately, this must be weighed against changes in unit cost due to changing the temperature levels, and a plant optimization must be performed.

The achievement of high steam temperatures with the accompanying high cycle efficiency is of sufficient importance that the reference design conditions were taken to be 1200°F mixed mean sodium outlet temperature in order that a determination might be made of the feasibility of achieving this level. Establishing this temperature level as even marginally feasible permits great confidence in designs at lower temperature.

II.1.2 Fuel System

A brief review of the problems inherent in fixed fuel (clad rod assemblies), and mobile fuel (pebble bed, paste, etc.) systems was made. The state of development of fixed fuel systems versus mobile fuel systems makes the choice of fixed fuel almost mandatory in view of the time schedule proposed.

The basic problems of fuel assembly design are well understood, and designers have a considerable background of successful experience upon which future designs may be based. The same cannot be said for any of the mobile fuel systems. Because the location of the fuel material is better defined in the fixed fuel systems, reactor behavior is more consistently predictable.

Fixed fuel provides the ability to selectively remove and insert fuel assemblies, and permits close control of the burnup in one dimension. The fuel clad serves as a retainer for the fuel so that cracking and fragmentation of the fuel material becomes a far less severe problem with fixed fuel than with, for example, pebble bed fuel. It is to be expected, therefore, that clad fuel can be retained in the core to higher burnup levels than possible with many mobile fuel systems. Clad pellets of a comparable size can be driven harder (to higher specific power) than unclad pellets because damage (e.g., chipping) to the fuel material is of lesser importance.

Breeding ratios of 1.4 to 1.6 appear feasible with large (Pu,U)C fueled cores utilizing stainless steel clad. Fuel cycle costs well below those of current large thermal reactors also appear attainable. Thus the fixed fuel system, utilizing mixed plutonium-uranium carbide clad in stainless steel, can foreseeably meet goals of high breeding ratio and low fuel cycle cost. The reference design is therefore of this type.

II.1.3 Special Design Features

Modular Core

Four basically different core configurations were examined for suitability. These four types cover the major geometric possibilities of core design.

The large cylindrical core with a height-to-diameter ratio of approximately unity seemed to be an obvious first choice. The pancake core, a flattened cylinder with very low height-to-diameter ratio, was investigated, both singly and stacked with intervening blanket. The physics behavior of various arrays of vertical slabs is similar to that of pancakes. Annular cores, with blanket inside and out, were investigated. Finally, modular island cores, immersed in a sea of blanket, were examined.

The results of these studies are presented in qualitative form in Table II.1.3-1. The quantitative details may be found in Section II.2.1.

The 1:1 cylinder shows up quite well except in two areas. The sodium void effect is always positive and of large magnitude (7 to 15 dollars); and the ratio of peak burnup to average burnup is high because of the high internal breeding ratio.

The pancake cores are not bad in any of the areas noted, but neither are they excellent in any area.

The annular core rates well, except for one major drawback. The calculated inventory requirements, with satisfactory sodium characteristics, are very high.

TABLE II.1.3-1

Qualitative Comparison of Core Configurations

<u>Core Type</u>	<u>Sodium Void</u>	<u>Breeding Ratio</u>	<u>Core Inventory</u>	<u>Control Requirements</u>	<u>Core Hot Channel Factors</u>	<u>Peak/Average Burnup</u>
Cylinder, 1:1	Bad	Good	Excellent	Excellent	Good	Bad
Pancake	Good	Good	Good	Good	Good	Good
Annular	Good	Excellent	Bad	Excellent	Excellent	Good
Modular Island	Good	Excellent	Good	Excellent	Excellent	Good

The modular island core is considered good in half the categories listed in Table II.1.3-1. and excellent in the other half.

Eliminating the 1:1 cylinder and the annular cores because of the bad features, the pancake is seen to be acceptable, and the modular island core is the most attractive.

This, then, has been chosen as the reference design.

CEX Assembly

The fuel assembly type chosen for the reference design is the controlled expansion (CEX) assembly. The CEX assembly is a simple modification of a conventional ceramic fuel assembly. Each fuel rod is manufactured in separate halves rather than one single rod. The upper half rods are assembled by brazing into an upper bundle, and the lower half rods into a lower bundle. Each of these two bundles has seven rods missing from the center of the bundle. An array of seven full-length cermet rods is inserted through the two fuel bundles and the two fuel bundles are then attached to the cermet rods at the outer ends of the cermet rods by end fittings.

The two fuel bundles are thus in series with each other and are attached only at the extremities of the cermet rod group. This total assembly is then attached to the assembly can at one end, with the other end left free to expand and contract. Resilient stabilizers are provided along the length of the bundle to maintain the bundle centered within the can.

The cermet rods expand in response to power or temperature changes, just as any metallic or cermet fuel rods. The fuel content of the cermet ensures that the cermet rods expand more with power than does the clad around the ceramic fuel. Therefore, an increase in power, which causes the cermet rods to expand, results in pulling the two ceramic fuel bundles apart at the center. This reduces the total reactivity of the core in the same way that pulling apart both halves of a split-table critical reduces reactivity.

The CEX assembly introduces a fast-acting negative reactivity coefficient into the core.

The brazed bundles are restrained to be parallel to the can at the core top and bottom. When the bundles bow due to a power gradient across the bundle, the bowing moves the center ends of the bundles away from the hot spot. This is the reverse of the behavior of a bundle of full-length rods, which move their centers toward the hot spot. This action results in a negative bowing coefficient.

Vented Fuel

To achieve the maximum potential of fast breeder reactors, it is necessary to achieve high burnup in the fuel, high temperatures in the primary coolant, and to use a clad alloy of the austenitic stainless steel type. Sacrificing any one of these reduces the economic potential. Failure to achieve high burnup directly increases fuel cycle cost. Failure to achieve high temperature reduces cycle efficiency, raising both fuel cycle and capital cost. Use of other high temperature alloys either reduces the breeding and, in most cases, makes the reactivity coefficients less attractive; or increases the fabrication cost.

The requirements of high burnup and high temperature place demands on steel clad which makes conventional sealed rod designs have a low probability of success. The steel is relatively weak at high temperatures, and the steel is expected to be brittle toward end-of-life. High burnup and high temperatures create high fission gas pressures. High burnup results in fuel swelling and high temperatures result in a thinning of the clad at the hot spot. Thus conventional design would require that the clad accept significant amounts of strain near end-of-life, when it is weak, brittle, and thin.

To maximize the probability of success in achieving the economic potential of the reactor, the duty demanded of the clad has been reduced to a single function--that of locating the fuel.

The use of a large radial gap permits most of the fuel swelling to occur without straining the clad. Use of sodium as a bonding agent permits this large initial gap (0.006-in. radially) while maintaining quite low fuel temperatures (below 2200°F at hot spot center).

Opening a vent from the inside sodium bond to the sodium coolant permits the escape of gases, so that the clad never experiences high gas pressure.

The clad duty is thus minimized to the maximum possible extent. Gaseous fission products diffuse out of the fuel material, into the sodium bond, then migrate up the bond to the vent, and escape to the main coolant stream. These gases are then captured in the cover gas. At each refueling period, the cover gas and the collected fission gases are purged to a hold-up tank. After several months of decay in storage, the gas is released through filters into the stack, and exhausted at high dilution.

Non-gaseous fission products which are soluble in sodium will diffuse through the fuel and eventually be leached out into the sodium bond. Some of this dissolved material will then diffuse out through the vent opening into the main coolant stream. Additional quantities will escape when temperature changes cause interchange of the sodium bond with the coolant.

The evidence indicates that these dissolved fission products will be removed from the coolant by the cold and hot traps. The SRE loop, for instance, was cleaned in this manner after the melt-down incident. Continuous cleaning will maintain an equilibrium level of fission products. This equilibrium level can be varied by varying the fraction of the flow bypassed to the traps. Complete cleanup can be achieved, if necessary, with the reactor shut down and the core assemblies removed from the vessel.

The activity resulting from the equilibrium level of fission products need not be large compared to the sum of the equilibrium sodium activity and the activity from dissolved iron, chrome, nickel, and impurities. The

shielding requirements are altered very little. The major primary loop components must be designed for remote lifting and remote insertion into a decontamination cell. This, however, is required by the sodium activity itself. The provisions required for a large plant cooled with sodium are very nearly those required for a similar plant employing vented fuel.

The principal exceptions to this are the clean-up facilities. A plant with unvented fuel is customarily designed for only a small percentage of fuel failures, and must shut down for an extended period if a large number of failures occur. Consequently, a plant designed for vented fuel has full facilities for handling the maximum fission product release. These extra facilities are a good investment to ensure high burnup fuel, high coolant temperature, stainless steel clad, and high plant availability.

II.1.4 General Reactor Design

The reactor consists of seven identical modules. Each module is composed of a core region, a blanket region, control, and a graphite barrier. The active core region in each module is a right cylinder approximately 6.25 feet high and 2.75 feet in diameter. This is surrounded by approximately one foot of blanket on all sides. Surrounding the blanket region, along the vertical sides only, is a graphite barrier. The frontispiece is an artist's conception of the reactor core.

The fuel, blanket, and graphite subassemblies are externally identical. Each subassembly is a hexagonal can about five inches across the flats and eleven feet long (including end fittings). Each can latches into the lower core plate by a mechanism which functions in a manner analogous to certain ball point pens. After the assembly is lowered into place, pressing down on the assembly from the top activates the latch, and latching is tested by an upward pull. To remove an assembly, it must again be pressed down from the top to deactivate the latch. The subassembly may then be removed.

The fuel material in the core region is mixed plutonium-uranium carbide. The axial blanket is integral with the core, and is depleted uranium carbide. The radial blanket is depleted uranium oxide. Use of oxide in the radial blanket results primarily from the low fabrication costs characteristic of depleted uranium oxide.

The core and axial blanket is in the form of pellets, clad in 0.010 inch 316L stainless steel. Rod O.D. is 0.3 inches. The rods are arranged on a triangular pitch within the hexagonal cans and are spaced apart by short ferrules at regular intervals along the rods. The rods and ferrules are brazed together to form bundles which are then assembled to form controlled-expansion (CEX) fuel assemblies (see Section II.1.3). Each of the core rods has a vent which permits fission gases to escape through the coolant to the gas plenum. This release of gas permits design of thin clad with greater confidence than could be achieved with thick clad if the fission gases were retained.

The carbide fuel pellets are bonded to the clad with sodium. This permits a large initial radial gap (0.006 inches) to accommodate fuel swelling from burnup. A conservative estimate of 20% volumetric swelling at 10a/o burnup is accommodated by the excess volume around the pellets, and porosity within the pellets. Loss of the sodium bond is not likely, but if it should occur, neither clad nor pellet will melt at full power. The fuel may exhibit increased swelling under these conditions, resulting in some clad deformation.

The radial blanket is in the form of oxide pellets clad in 0.020 inch 316L stainless steel. Rod O.D. is 0.464 inch. The rods are arranged on a triangular pitch within the hexagonal cans with spacing maintained by wire-wrap. The blanket assembly life is set by physics characteristics at lower burnup than the core; therefore thick unvented clad is employed. The large volume fraction of fertile material in the blanket permits use of thick clad with only minor penalty. The radial blanket pellets are not sodium bonded because the oxide pellets may not be compatible with sodium.

The graphite barrier region consists of graphite blocks sealed into free-standing hexagonal cans. The graphite cans are locked into the vessel by an upper beam structure in addition to being latched to the lower core plate. The graphite thus provides a firm mechanical separation of the modules, in addition to providing thermal and neutronic separation.

The reference control system for the reactor consists of three peripheral rods and one central rod in each module. Although the control functions for each module operate independently, they are coupled electronically so that they function in unison to raise or lower total reactor power. The three safety rods in each module in the reference design are located in the blanket on the periphery of the core region. Each rod occupies a full blanket subassembly position. Shim control is located in the center of the core region, occupying one fuel subassembly position. The control rods are operated from above through the reactor vessel plug.

The sodium coolant enters the reactor vessel above the top of the core, and flows down an annulus within the vessel to maintain the outer vessel wall at the lowest possible temperature. From the annular downcomer, the sodium flows through variable orifices into two plenum regions in the bottom of the vessel. One plenum feeds the core regions; the other, lower pressure plenum, feeds the blanket regions. This is illustrated schematically in Figure II.1.4-1.

The important parameters for the reactor are shown in Table II.1.4-1.

BASIC PRIMARY SYSTEM

Figure II.1.4-1 Basic Primary System

TABLE II.1.4-1

Reactor Parameters

Reactor Power	
Net Electrical Efficiency (40%)	1000 MWe
Total Thermal	2500 MWt
Core Regions	2170
Blanket Regions	330
Reactor Size	
Diameter	14 ft
Active Core Height	6.25 ft
Assembly Length	11 ft
Module Size	
Height	8.5 ft
Diameter (Across Flats)	4.5 ft
Number of Modules	7
Composition	
Core	UC, 22.2% PuC
Fraction fissile Material (fissile/U+Pu)	0.1557
Axial Blanket	UC
Radial Blanket	UO ₂
Fuel Performance	
Average Exposure	100 MWD _{th} /kg(U+Pu)
Average Heat Flux	0.52 x 10 ⁶ Btu/hr ft ²
Average Rod Performance	12 kw/ft
Effective F _{ΔH} (Including orificing)	1.61
Primary System Temperatures	
Sodium Inlet	979°F
Sodium Outlet	1200°F
Sodium Outlet, Hot Channel	1372°F
Clad Surface, Hot Spot	1400°F
Fuel Surface, Hot Spot	1486°F
Fuel Center, Hot Spot	2184°F

Reactor Parameters (Cont.)

Primary Sodium Flow	128 x 10 ⁶ lbs/hr
Average Velocity	26.5 ft/sec
Core and Axial Blanket	
Clad, O.D.	0.300 in
Clad Thickness	0.010 in
Pellet Diameter	0.268 in
Radial Blanket	
Clad O.D.	0.465 in
Clad Thickness	0.020 in
Pellet Diameter	0.460 in
Control	
Excess Operating Reactivity (6 months)	+0.0090
Minimum Shutdown Margin	-0.0300
Delayed Neutron Fraction	0.0040
Doppler (1325°F to 4000°F)	-0.0084
CEX (600°F rise)	-0.0045
Total Loss of Sodium	-0.0015
Partial Loss of Sodium	+0.0032
Reactivity Coefficients	
CEX Cermet Expansion	-0.75 x 10 ⁻⁵ /°F
Clad Expansion	+0.30 x 10 ⁻⁵ /°F
Structure Expansion	-1.00 x 10 ⁻⁵ /°F
Doppler (@ 1325°F)	-0.69 x 10 ⁻⁵ /°F

II.1.5 General Plant Design

Figure II.1.4-1 (see Section II.1.4) schematically illustrates the reactor primary loops and the associated equipment. The plant will utilize two intermediate heat exchangers, each of which is fed by two primary system pumps.

Use of two intermediate heat exchangers offers minimum capital cost without compromising plant safety. The use of two pumps per heat exchanger results from two considerations. Pumps are less reliable than heat exchangers, so employment of two pumps reduces the probability of complete loss of flow through one heat exchanger with the accompanying effect on the core. Pumps are more difficult to scale up than heat exchangers because of bearing and seal difficulties. Therefore, the two smaller pumps should individually be somewhat more reliable than one large pump. The pumps are located in the hot leg to keep the reactor cover gas and seal pressure low. This restricts the available net positive suction head, imposing limits on pump impeller tip speed. The smaller pumps meet these requirements best.

No isolation or check valves are provided in the primary system. Closed cycle water reactors have been successfully designed without main coolant valves, and the systems are hydraulically similar. The complete lack of primary valves eliminates a difficult maintenance problem; and deletion of check valves eliminates a hydraulic shock problem.

The cover gas is a low pressure argon system. The cover gas plenum is a nearly stagnant gas volume which functions as a sodium expansion volume as well as a fission gas collection point. Before removing the vessel plug, the sodium level is raised sufficiently to force about 90% of the inert gas-fission gas mixture into a storage tank. The remaining small volume purged repeatedly with clean inert gas, and the stored gas is held for 60 days before release through the stack with large dilution. The use of the nearly stagnant gas system, coupled with the raising of the sodium level before purging, reduces the total storage volume requirement to 1100 cubic feet. A small flow of cool gas is provided for control and instrumentation cooling and vapor protection.

Cold traps are provided for gross removal of oxides and non-gaseous fission products. Hot traps provide further reduction in oxygen, fission products, and carbon. To minimize the attack of sodium on the fuel clad, full temperature operation would not be permitted until the oxygen and carbon content of the sodium were reduced to design values. The refueling scheme minimizes the introduction of oxides into the system and there is no source of oxygen during operation.

The reactor vessel is of stainless steel, 18 feet in diameter. Cold sodium flows from the intermediate heat exchanger in through the annuli of the pair of two concentric nozzles, and down between the vessel wall and an inner steel liner which is 15 feet in diameter. Hot sodium flows out through the inner nozzle of each pair of nozzles to the pumps, thence to the intermediate heat exchangers.

The reactor vessel closure is a circular, steel and borated graphite plug, 16.5 feet in maximum diameter. The 75-ton plug rests on a double O-ring seal with leak detection between the rings. Should leakage occur past both seals, it would be contained safely by the steel-lined concrete refueling hot cell located above the vessel. The plug contains the control rod drives and instrumentation thimbles. At the time of refueling, these items are partially retracted and the plug is removed remotely by hoists on a traveling bridge.

The hot cell is sized to permit convenient removal and storage of the plug and to allow fuel handling machine operation with direct access to the spent fuel storage pools, and to air locks for introduction and removal of fuel from the plant.

The fuel handling machine is a simple traveling-bridge device with remote locating and position indicating equipment. Visual location can also be performed through a periscope.

The primary system is designed with the simplest mechanical equipment possible to minimize maintenance, and the internals of pumps and heat exchangers are designed to permit remote removal for decontamination if maintenance is required.

II.2 REACTOR CORE DEVELOPMENT

II.2.1 NUCLEAR DESIGN

II.2.1.1 Basic Characteristics and Significant Parameters of Fast Reactors

II.2.1.1.1 Introduction

As the technology of nuclear power reactor design advances, the feasibility of developing reactors of an advanced type becomes increasingly evident. Moreover, economic motives associated with the long range need to utilize the potentiality of fertile materials, make such development an attractive prospect. A reactor concept which appears to fulfill most of the objectives of an advanced design is the liquid metal cooled fast ceramic reactor. For the purpose of this study, the reactor has been designated to be a sodium cooled plutonium-uranium carbide fueled reactor. However, the general characteristics and problems to be discussed in the following sections are common to a variety of possible core and coolant combinations that might be developed for the exploitation of the attractive properties of nuclear fuel when utilized in a fast neutron spectrum.

In the discussion to follow immediately, the important properties and problems of fast reactors will be discussed in somewhat idealized terms in order to emphasize qualitative aspects and general trends. Following this discussion, the detailed analytical procedures will be presented and the results of their application to the selection and evaluation of a specific reference configuration will be described.

II.2.1.1.2 Breeding Ratio

The principal motivation for the development of a power reactor in which the majority of the fissions are initiated by fast neutrons is to produce fissile material at a rate greater than it is consumed. This possibility is attractive because it not only permits the reactor to sustain itself and to produce power continuously from a feed of the naturally occurring fertile materials, U-238 or Th-232, but it also leads to the accumulation of fissile material suitable for the initial fueling of an expanding number of similar reactors. The net production of fuel in a power reactor is referred to as "breeding" and is measured by the "breeding ratio" which can be defined as the ratio of the rate of production of fissile material to the rate of consumption of fissile material. The maximum value for the breeding ratio would be achieved if all neutrons not absorbed in fissile material were captured in fertile material. An expression for the maximum breeding ratio, $(BR)_{\max}$, can be developed as follows:

Let

ν_1 = the average number of neutrons per fission for fertile material

α_1 = the capture to fission ratio for fertile material

ν_2 = the average number of neutrons per fission for fissile material

α_2 = the capture to fission ratio for fissile material

δ = the ratio of fissions in fertile material to fissions in fissile material.

$$(BR)_{\max} = \frac{\nu_2 + \delta (\nu_1 - 1 - \alpha_1)}{1 + \alpha_2} \geq 1$$

The possibility of achieving a breeding ratio substantially greater than unity for a fast reactor arises from the following reasons:

- 1) The number of neutrons per fission is significantly greater when the energy carried by the fast neutron is available for the fission process.
- 2) The capture to fission ratio is relatively low -- particularly for Pu-239 -- for fast neutrons as compared with epithermal or thermal neutrons.
- 3) A substantial fraction of the power, hence of the fission neutrons, is due to direct fission of fertile materials.
- 4) The cross sections of structural materials and fission products are relatively low, permitting a close approach to the maximum value for the breeding ratio as given above.

A principal objective of the nuclear designer is to exploit the breeding potential of the fuel to the maximum extent consistent with safety and economics. Inasmuch as criteria for economics are less well defined and more subject to transitory influences than the breeding ratio, an approach to design which first aims for a high breeding ratio and then adjusts in the direction of improved economic characteristics is most likely to lead to a design which exhibits long term potential as well as short term acceptability.

II.2.1.1.3 Doubling Time

Doubling time is a second characteristic of a fuel-producing reactor which is closely related to the breeding ratio. This is the time necessary to accumulate excess fuel material equal to that required for the reactor and the various inventories associated with the fuel cycle. The doubling time indicates the frequency with which the reactor will make possible the start-up of an additional similar reactor in a breeder economy. If nuclear power is to become a chief source of energy in the future, the doubling time should be comparable with, or shorter than, the time during which world energy consumption increases by a factor of two. Two major factors affect the doubling time. The first of these is the breeding gain, equal

to $(BR-1)$, which is the net fissile material produced per unit of fissile material consumed. The second major factor influencing the doubling time is the ratio of fuel consumption per unit time to the total fuel inventory. Since the fuel consumption per unit time is proportional to the total power, P , this ratio can be written $\frac{C_1 P}{L+I}$ where L is the core loading, I the external inventory and C_1 an appropriate constant of proportionality. The doubling time, T_2 , can then be expressed as follows in terms of the above factors for the ideal case in which no fuel is lost in the recycling process and the breeding ratio is independent of time.

$$T_2 = \left(\frac{1}{BR-1} \right) \left(\frac{L+I}{C_1 P} \right)$$

It is clear from this expression that the doubling time is inversely proportional to the power density determined on the basis of the total amount of fuel involved and to the breeding gain. Therefore, design objectives of a high breeding ratio and high specific power should be utilized to achieve a short doubling time. It should be noted that the doubling time is a measure of the rate at which a given fissile material inventory can be expanded. It is principally of significance in a situation in which a deficiency in fissile material supply exists.

II.2.1.1.4 Fertile Material Utilization

From the point of view of conservation of resources, neither the breeding ratio nor the doubling time is an unambiguous indicator of the effectiveness of the reactor in the utilization of fertile material either for the direct production of power or for production of fissile material. If we designate U as the fertile material consumed or converted per unit fissile material consumed, then

$$U = BR + \frac{\delta}{1+\alpha_1}$$

An important factor in the fuel cycle which affects the conservation of fuel resources is the reprocessing loss, r , measured in fissile material lost

per unit of fissile material consumed in the reactor. If L designates the loading of fissile material in the core, T the total loading of fissile and fertile material, and B the average burnup (in MWD/T), then the fuel (fissile and fertile) consumed in fission per reprocessing cycle is $C_2 BT$ where C_2 is an appropriate constant and the assumption is made that the energy yield in fission is the same for all materials. $\frac{C_2 BT}{1+\delta}$ is the fissile material consumed per reprocessing cycle. The net fissile material production during a single reactor cycle is then $\frac{C_2 BT}{1+\delta} (BR-1)$, and the fissile material to be reprocessed per cycle is $L + \frac{C_2 BT}{1+\delta} (BR-1)$.

If we designate by f the fraction of fissile material lost during reprocessing,

$$r = f \left[L + \left(\frac{C_2 BT}{1+\delta} \right) (BR-1) \right] \times \frac{1}{\left(\frac{C_2 BT}{1+\delta} \right)}$$

$$= f \left[\frac{L(1+\delta)}{C_2 BT} + (BR-1) \right]$$

Normally the first term in brackets is substantially larger than the second. The expression for r emphasizes the fact that reprocessing losses are more significant in cores with a high ratio of loading to consumption of fissile material per cycle. For the comparative evaluation of the breeding potential of different reactors, a meaningful quantity is the net breeding gain given by $(BR-1)-r$. The net breeding gain is the reprocessed fissile material produced per unit of fissile material consumed in the reactor.

II.2.1.1.5 Power Distribution

The fast reactor is characterized by having relatively long diffusion lengths for neutrons and a corresponding absence of localized neutron flux peaks due to heterogeneities in the core structure. Reflector savings associated with blanket regions can be substantial in comparison with core size, so that significant improvements in peak to average power ratios can

be obtained in small cores. A principal source of variation in power density through the core region is associated with variations in fissile material concentrations, inasmuch as the desire for high burnups and small reactivity variations usually leads to a process of partial core reloading with consequent non-uniformities in fuel distribution.

II.2.1.1.6 Reactivity Coefficients

General Safety Considerations

There are a number of characteristics of fast reactors which lead to a heavy emphasis, in design projects, on problems related to safety. This is particularly true when the principal fuel is Pu-239, because of its exceptionally low value (.0021) of the delayed neutron fraction β . (However, presence of U-238 and higher Pu isotopes can result in an effective delayed neutron fraction of about .004.) The low value of the delayed neutron fraction for plutonium results in a correspondingly small margin for reactor operation between the conditions of delayed and prompt criticality. The small value for the prompt neutron lifetime ($< 10^{-6}$ seconds) means that, in the event prompt criticality is attained, the rate of change of power with time will be much more rapid than the response of a mechanical control system, so that some intrinsic control mechanism associated with the production of heat should be operative.

In addition to the tight constraints imposed by the operating margins for the Pu fueled fast reactor, an additional severe problem is imposed by the potential consequences of an excursion involving melting of fuel or clad. The characteristics of the fast reactor designs as usually discussed are such that the possibility exists of a reassembly of the fuel material into a more reactive configuration as a consequence of fuel element meltdown. A central problem in fast reactor design is the selection and justification of design "ground rules" imposed for the purpose of assuring adequate safeguards against reactivity excursion yet which are sufficiently flexible to permit exploitation of the attractive features of the fast reactor fuel cycle.

Doppler Coefficient

In considering the transient behavior and safety characteristic of fast reactor cores, prime importance is attached to the shutdown mechanisms that operate in response to an uncontrolled reactivity insertion. A key parameter in such consideration is the prompt temperature coefficient of reactivity associated with Doppler broadening of neutron absorption resonances in the fissile and fertile materials of the fuel rod. The Doppler broadening phenomenon results from the variation in relative velocities of neutron and nuclei, due to the random thermal motion of nuclei in the fuel rod. Because the cross section for resonance absorption is very high, a large fraction of the neutrons at resonance energy is absorbed and a saturation effect is produced. Broadening the band of acceptable energies therefore results in an increase in the total number of neutrons absorbed. Because such broadening is proportional to the velocity of the neutron, the broadening generally increases with increasing energy of the resonances. However, when the broadening of individual resonances is sufficiently great that adjacent resonances of different individual nuclei are competing for neutrons at the same neutron velocity, the effect of broadening on the absorption rate diminishes. Because the Doppler effect varies with neutron energy, the magnitude of the associated temperature coefficient is quite sensitive to the effects of the neutron spectrum. Moreover, since the detailed characteristics of the resonances and their spacing differ with different materials, the relative effects of Doppler broadening on the reaction rates in different materials are affected by changes in spectrum. For a reactor fueled with plutonium with U-238 as a fertile material, the greater spacing of the U-238 resonances (18 eV) as compared with Pu-239 resonances (2 eV) results in a relatively greater Doppler effect in U-238 when compared with Pu-239 as the spectrum hardens since overlap occurs at lower energies in Pu. However, the magnitude of the Doppler effect in both materials decreases with spectrum hardening due to the reduced energy self shielding for the Doppler broadened resonances at higher energies.

The effects of Doppler broadening for fertile materials are well understood in principle, and the phenomenon clearly corresponds to a negative reactivity effect. The magnitude is strongly dependent on the reactor spectrum and on the neutron importance as a function of energy, as well as on the characteristics of the resonances in the unresolved energy range, rendering precise calculations difficult. In the case of fissile materials wherein the capture to fission ratio varies from level to level, both the magnitude and sign of the Doppler coefficient of reactivity are subject to question. However, it is generally believed that in the presence of significant amounts of non-fissionable neutron absorbers which are competing for neutrons, the Doppler effect in Pu-239 is positive. In calculating Doppler coefficients, effects associated with the overlap of adjacent resonances in a given material or interaction of different materials are difficult to evaluate. In the following studies, Monte Carlo calculations have been utilized in order that such effects may be treated with reasonable accuracy.

Sodium Temperature and Void Coefficient

A second reactivity coefficient of great significance in a sodium cooled fast reactor is that associated with a change in coolant density due to temperature change or the introduction of voids. Such a change in sodium density affects the reactivity in two qualitatively different ways. First, by changing the slowing down power per unit volume of core, it affects the core leakage rate. This effect reduces reactivity if a decrease occurs in sodium density. Second, by changing the spectrum of neutrons in the core, a change in sodium density affects the fast fission rate in fertile materials, the capture to fission ratio in fissile materials, and the competition for neutrons between fuel and the fertile and structural materials of the core. This effect results in an increase in reactivity if there is a decrease in sodium density. The magnitude and sign of the net effect on reactivity produced by changes in sodium density depend strongly on the details of the core design, as well as upon the magnitude and spatial distribution of the density variation.

Mechanical Coefficients of Reactivity

The fast reactor is sensitive to changes in the ratios of material concentrations within the core, as well as to leakage if the core is small. As a result, the mechanical coefficients of reactivity play a significant role in determining reactor response to temperature changes. In the process of mechanical design, considerable effort must be expended to assure acceptable mechanical response to transient variations in temperature. Moreover, by deliberately utilizing this inherent property of the fast reactor, special safety features can be incorporated in the design to improve the response of the core as otherwise determined by the Doppler and sodium temperature coefficients of reactivity.

II.2.1.1.7 Spectral Effects

The marked sensitivity of reactor characteristics to shifts in the neutron spectrum is one of the predominant features of the Pu fueled fast reactor. This spectral sensitivity results from important specific processes occurring in the reactor that are individually sensitive to the spectrum. For example, the rate of direct fission of fertile material exerts an important influence upon the breeding ratio and on the utilization of fertile material. In U-238, this process has a sharp threshold near 1 Mev, so that the presence of moderating materials in the core affects it adversely. The values of ν for both fertile and fissile materials increase substantially over the energy range 1 Mev to 10 Mev. Thus a hard neutron spectrum results in a higher average neutron yield per fission and a corresponding high value for the breeding ratio. The capture-to-fission ratio for Pu-239 is markedly sensitive to spectrum, so that moderation of neutrons in a Pu fueled reactor has a very deleterious effect on the breeding ratio. Consequently, from a neutron economy point of view, maintenance of a high energy neutron spectrum is of prime importance to the reactor designer. As mentioned previously, however, the Doppler coefficient of reactivity is quite sensitive to the importance of neutrons in the neighborhood of 1 kev-energy. Augmentation of this important safety parameter can therefore only be accomplished at the

cost of a decrease in breeding ratio. In a plutonium fueled core, the spectral sensitivity is somewhat dependent on the isotopic composition of the fuel. Reactivity coefficients will be a function of the concentration of higher plutonium isotopes and reactor spectrum. Coolant coefficients of the reference design tend to be more positive when large amounts of Pu-240 are present due to the fission threshold at ~ 0.5 Mev, which is near the median of the spectrum.

II.2.1.1.8 Control Requirements

Because of the relatively small reactivity effects associated with fission products, the control requirements for a fast reactor are modest by comparison with those of thermal reactors. The variation of reactivity with fuel burnup is principally determined by the actual depletion of fissile material in the core and the effectiveness of fissionable material in the blanket. In a large core the loss of reactivity due to depletion of fissile material is strongly dependent on the "in core" breeding ratio. Reactor designs with low leakage and high internal breeding ratio may even have an increasing reactivity characteristic with burnup. Small leaky cores as in the reference modular design, while having low "in core" breeding ratios, may have relatively flat reactivity characteristics if the overall breeding ratio is high due to the effectiveness of the plutonium being produced in the blanket. Since this plutonium is produced in a region of relatively high importance due to the small diameter of the core module, it contributes substantially to the reactivity of the reactor.

II.2.1.2 Analytical Procedures

II.2.1.2.1 Basic Nuclear Data

General

At the present state of the art of fast reactor design, deficiencies in the basic nuclear data required for analysis of reactor performance introduce considerable difficulties in the evaluation of design concepts. The sensitivity of reactor parameters to changes in values of specific cross sections is such that substantial adjustments in design are required to compensate for the range of possible values if particular constraints have been established for reactor characteristics. In fact, the relative merits of different concepts cannot be clearly established on the basis of experimental data currently available. In order to investigate the sensitivity of design parameters to changes in nuclear data, calculations have been conducted utilizing data from several tabulations, each of which has been quite widely utilized in fast reactor calculations.

In our preliminary studies of the 1000 MWe fast reactor, the neutron cross section library proposed by S. Yiftah, D. Okrent and P. A. Moldauer (1), as well as the APDA (18-R2) library which is based on the C. B. Mills library (2), were used in the calculations. The C. B. Mills library is a 18-group library in which the group averaged neutron cross sections for the heavy elements are based on those proposed by G. Hansen and W. Roach (3). The latter library has 16 groups; the first seven coincide with the corresponding ones of the C. B. Mills library. For the light elements as moderators, Mills used the conventional procedure of weighting by neutron flux. The resonance analysis of J. Devaney and G. Bell was also taken into account by Mills.

Since these two libraries were proposed, new experimental and theoretical data have become available. Experimental results (4, 5) have also shown that the calculation performed with the S. Yiftah, D. Okrent and P. A. Moldauer library (henceforth called the YOM library) yields too hard a spectrum in the reactor core. As a result, newer libraries have been utilized in later

calculations. These libraries are: 1) a modified version of the YOM set based fundamentally on the 635 set of Davey (6); and 2) a new 18-group library called WAPD-F1.

WAPD-F1 18-Group Library

This library was proposed in order to utilize new experimental and theoretical data generated since the formation of the YOM and APDA libraries. For a better account of the Doppler effect, the 18 groups were selected with more energy break points below 10 kev than the YOM set provided. At these low energies, the neutron source is due mainly to the moderation of the light elements, indicating the importance of having accurate values of σ_{er} and σ_{tr} for light elements (4). The introduction of light elements in the reactor causes a variation in the leakage and in the flux that is difficult to calculate because of the fluctuations of the neutron energy spectrum near the resonance energies of these elements. For energies where the resonances are pronounced, the light element cross sections show large variation with the composition of the reactor. H. Hummel and H. Rago (4, 5) use the ELMOE Code to treat these effects of moderation and scattering by light elements in fast reactors, and assert that a more accurate calculation was obtained by this code than has previously been possible. In the WAPD library, the results of the work of Hummel and Rago were taken into account.

In compiling the WAPD-F1 library efforts were made to obtain realistic values of $\bar{\nu}(E)$ and $\alpha(E)$, because fast reactor parameters are most sensitive to these values (7). The $\bar{\nu}$ values for U^{235} , U^{233} , U^{238} , and Pu^{239} were obtained from the work of A. B. Smith presented at the International Symposium 1961 (8). Straight lines for $\bar{\nu}$ versus energy were obtained by Smith by applying a least square fit procedure to the data available in 1961. This data includes the experimental study of D. Butler et al. (9) and B. C. Diven and J. C. Hopkins (10).

For U^{235} , the experiments indicate a non-linearity of $\bar{\nu}$ versus energy. The data could be fitted to two straight lines: one for energies up to 2 Mev,

and the other for energies greater than 2 Mev. The WAPD 18-group library has adopted the first straight line, namely

$$\bar{\nu} = 2.43 + 0.138E$$

These values of $\bar{\nu}$ are based on the U.S. normalization criterion (i.e., the value 2.43 is taken for U^{235} at thermal energies). As shown in Figure II.2.1-1, these values of $\bar{\nu}$ for U^{235} are lower than those given by the YOM library. However, this is in agreement with the results of C. Cook and W. O. Davey (11).

For Pu^{240} , $\bar{\nu}(E)$ was obtained by taking $\bar{\nu}(1.67 \text{ Mev}) = 3.1$ rather than 3.3 as used in YOM. The new values soften the spectrum which is in agreement with the measurements obtained in the core of Zephyr (12). In Figures II.2.1-1 through II.2.1-4, $\bar{\nu}(E)$ is plotted for the different libraries. The WAPD values of ν for U^{238} are greater than those of YOM. The resulting WAPD values for Pu^{239} in the range of zero to 1 Mev are slightly lower than those of YOM, but are greater than YOM above 1 Mev.

The values of α for U^{233} , U^{235} , Pu^{239} are based on ANL set 635 (13) which is the YOM set modified to agree with the measurements of Diven and Hopkins (14). For Pu^{240} , values of capture cross section reported in GEAP 3981 were used for energies less than 30 kev; these values were obtained from results with RAPTURE Code which employs the Breit-Wigner single level resonance theory and averages the resonance integrals over the Porter-Thomas distribution of resonance parameters.

Figure II.2.1-5 is a plot of $\eta(E)$ ($\eta = \nu/(1+\alpha) = \nu\sigma_f/\sigma_a$) for U^{238} and Pu^{239} as a function of energy for the YOM and WAPD libraries. From 0.1 Mev to 1 Mev the slope of $\eta(E)$ is noted to be less pronounced for the WAPD set than for the YOM and APDA sets. Because the reference reactor has a flux distribution that peaks at energies near 0.23 Mev, the sodium coefficient therefore is expected to be somewhat less for this new library than for YOM or APDA (as the concentration of sodium is reduced in the core, the neutron spectrum shifts toward higher energies).

The sensitivity of the calculated results to the detailed treatment of the scattering and slowing down cross sections of light elements and to the α and ν for Pu-239 lends emphasis to the need for further investigation in these areas.

In the absence of definitive cross section data for use in analysis of fast reactor cores, it is apparent that calculated performance characteristics will be subject to doubt. The order of magnitude of such uncertainties can be assessed by carrying out the comparative evaluations as previously described. Inasmuch as the parameter studies described in Section II.2.1.3 were initiated concurrently with the studies of nuclear data and the development of the WAPD-F1 library, they were carried to completion utilizing the APDA (18R2) library. Utilization of a consistent set of data is considered mandatory in a study of this nature if meaningful qualitative and semi-quantitative conclusions about the relative characteristics of various designs are to be derived. For later studies, as reported in Section II.2.1.4, calculations were made employing what is regarded as the best available library appropriate to the design, the WAPD-F1 set. However, substantial further work -- both theoretical and experimental -- should be performed in order to provide data that is sufficiently accurate to permit effective design optimization studies.

Reactor Parameters Calculated with the Different Libraries

Table II.2.1-2 summarizes the results of the calculations performed with the YOM, APDA (18-R2) modified YOM, and WAPD-F1 libraries.

The reactor on which this basic study was conducted is a reflected cylinder with a central core of 33 inches effective diameter, surrounded by a blanket 9.32 inches thick. The composition of these different regions and other physical parameters are given in Table II.2.1-1. From Table II.2.1-2 and from Figures II.2.1-24a through II.2.1-26b, it is evident that the WAPD-F1 library produces more negative values of sodium coefficients than the other libraries. The enrichment required to achieve criticality is also lower with the WAPD library than with the APDA set, but is greater than that required with the YOM library.

Figures II.2.1-6 and II.2.1-7 show the flux per unit of lethargy versus lethargy for the WAPD-F1 and APDA libraries. The difference in smoothness between the curves obtained from the WAPD-F1 library and from the other two libraries is noteworthy. The primary reason for the irregularities in the spectrum based on the WAPD-F1 library lies in the sodium resonances. This phenomenon is a direct consequence of having taken into account the results from the ELMOE Code as obtained by H. Hummel and H. Rago. The bump that can be observed near 2 Mev in the flux spectrum given by the APDA library (Figure II.2.1-7) is due mainly to the depression of σ_{er} for carbon in the second group. These effects are examples which emphasize the importance of having an accurate treatment of σ_{er} as well as σ_{tr} for light elements.

According to the calculations performed with the WAPD-F1 library, the sodium void does not present as serious a problem as that suggested by other libraries. With respect to the YOM library, the new library increases the number of slow neutrons (energy below 30 kev) and results in an improvement in the Doppler coefficient. It should be emphasized here that the WAPD-F1 cross section set was developed for a reactor typical of the composition and spectrum of the reference design. In this sense, it represents a more refined approach to the analysis than the use of general purpose data sets.

TABLE II.2.1-1

Reactor Composition for Library Comparison Study

	<u>Volume Fractions</u>		
	CORE	BLANKET	REFLECTOR
Na	0.554	0.25	0.05
SS	0.155	0.19	0.05
Fuel	0.291	0.56	--
Graphite	--	--	0.90

NOTE:

Core radius = 16.5";

blanket thickness = 9.32";

reflector thickness = 2.44"

TABLE II.2.1-2

Sensitivity of Reactor Parameters to Cross Section Libraries

<u>Library</u>	$(\frac{\Delta k}{k})_{\text{Na Void}}$		$(\frac{\Delta k}{k})_{-10\% \text{ Na}}$		Isothermal Na Coef.		Core Enrichment <u>% Pu²³⁹</u>	Partial Breeding Ratio
	<u>Core</u>	<u>Reactor</u>	<u>Core</u>	<u>Reactor</u>	$\Delta k \Delta T, /^{\circ}\text{F}$			
APDA	-.0128	-.0162	-.0006	-.0009	-1.0x10 ⁻⁶	-1.4x10 ⁻⁶	17.57	1.36
YOM	-.0152	-.0192	-.0011	-.0014	-1.7x10 ⁻⁶	-2.2x10 ⁻⁶	15.01	1.41
Mod. YOM	-.0150	-.0173	-.0009	-.0011	-1.4x10 ⁻⁶	-1.7x10 ⁻⁶	15.02	1.53
WAPD-F1	-.0276	-.0320	-.0024	-.0028	-3.9x10 ⁻⁶	-4.5x10 ⁻⁶	15.38	1.44

NOTE:

- 1) 1% Pu²³⁹ in radial blanket
- 2) Reflected cell geometry
- 3) Breeding ratio for core and radial blanket

Figure II.2.1-1 Variation of ν with Energy for U^{235}

Figure II.2.1-2 Variation of ν with Energy for Pu^{239}

Figure II.2.1-3 Variation of ν with Energy for U^{238}

Figure II.2.1-4 Variation of ν with Energy for Pu^{240}

Figure II.2.1-5 Variation of η with Energy for U²³⁸ and Pu²³⁹

Figure II.2.1-6 Flux Energy Spectrum of Core and Blanket With WAPD-F1 Library

Figure II.2.1-7 Flux Energy Spectrum of Core and Blanket With APDA-F1 Library

II.2.1.2.2 Criticality, Power Distribution, and Spectrum Analysis

The primary design tool used in the studies discussed in the following sections was a one-dimensional multigroup diffusion theory code called W-MABI. This is a Westinghouse version of the AIM series of codes (15). Its distinctive features are a buckling iteration option, a more complete input library, and significant output information for few group calculations.

The buckling iteration option provides for the coupling between two one-dimensional problems by means of the group and region dependent transverse buckling in multigroup diffusion equations. Therefore, a limited, but rapid, investigation of two-dimensional effects is possible. The form and content of the input library to W-MABI was selected such that all cross section and group parameters were self-contained in order to allow flexibility and interchangeability of libraries. The input libraries contain values of energy, lethargy, and fission spectrum for the multigroup structure. Nomenclature and atomic masses of the elements (or their isotopes) are also in the library. In addition, multigroup microscopic cross sections for absorption, fission, nu-fission, transport (or total, depending on the option used in calculation of the diffusion coefficient) and scattering removal (downscatter to ≤ 5 groups) are provided. Special FORTRAN and FAP programs for the IBM 7090 computer were used to construct libraries in binary cards from decimal information. With this system, input libraries for W-MABI were assembled from the cross section information discussed in the previous subsection. These data could be used to investigate the dependence of steady state and kinetic characteristics of reactor design on cross section sets.

The third special feature of W-MABI is an option which calculates flux-averaged, few-group parameters and punches binary cards that serve as input to another special program called ZILCH. These binary cards contain multigroup fluxes integrated over each region of the W-MABI problems. ZILCH is a semi-automatic link between one-dimensional multigroup calculations and two-dimensional few group calculations. Its input is in the form of multigroup binary libraries and region integrated fluxes, and its output is in the form of decimal cards

containing flux-averaged, few group parameters for input to PDQ-3 (16). ZILCH gives complete flexibility in few group compaction and material make-up for each PDQ composition. Four few groups were used in the two-dimensional calculations which provide detailed evaluation of power distributions and balance of power between modules of the multiple seed design, and of control rod worths.

The W-MABI code was used to predict criticality for the various reactor configurations and material compositions examined in the fast breeder reactor. It provides for searches of material compositions and geometries to attain the critical condition. The validity of using multigroup diffusion theory has been examined previously (17) by comparing calculations of criticality with critical experiments and transport theory calculations. Based on the investigations of Davey (6), Long (18), and Hummel and Rago (4), it is reasonable to state that cross sections present greater uncertainty in predicting critical mass and flux spectrum than do the analytical techniques.

As part of the editing routine of W-MABI, the spatial and energy distribution of neutron flux is printed out. Another feature is the calculation of the spatially integrated flux energy spectrum for each region of the reactor design. The instantaneous conversion ratio is calculated by determining the ratio of capture rate in fertile materials to the absorption rate in fissile materials for the whole reactor, and the contribution of each region to this quantity. Although a spatial power distribution is not part of the editing routine in the current version of W-MABI, the spatial source distribution provides a good substitution for the power shape and is used in the design calculations. The validity of this approximation was investigated and is illustrated in Figure II.2.1-8. The true power distribution was calculated as being the sum of the product of multigroup fluxes and macroscopic fission cross sections ($\sum_{\text{NOG}} \Sigma_f \phi$). (The source is $\sum_{\text{NOG}} \nu \Sigma_f \phi$ at "just critical.") Both curves are normalized to the same peak value. For the two-dimensional calculations, the power distributions are also approximated by the source distribution. The balance of power between the core and blanket is influenced by this approximation; however, this error is

small in comparison to the change in balance of power between core and blanket over a fuel cycle due to depletion and buildup of fuel.

The SIZZLE depletion analysis code described in the following sections was used to determine temporal behavior of reactivity, power distribution, and instantaneous breeding ratio as well as changes in isotopic concentrations of fuel.

Figure II.2.1-8 Comparison of Source and Power Distribution

II.2.1.2.3 Fuel Depletion and Accretion

The long-term temporal studies involving burnup and buildup of fuel and changes of reactivity were investigated by means of a one-dimensional few group diffusion theory code with depletion analysis called SIZZLE (19). The code contains (as input) a multigroup microscopic library which is employed in performing an initial multigroup diffusion theory calculation to determine the fluxes and criticality constant. Using the multigroup fluxes, compaction of microscopic cross sections to six groups or fewer is performed. These few-group cross sections are then utilized in subsequent analysis. Standard depletion and accretion equations of U^{235} , U^{236} , U^{238} , Np^{239} , Pu^{239} , Pu^{240} , Pu^{241} and Pu^{242} are solved for finite time steps over which the flux is assumed constant. In the calculation, region-wise depletion analysis for small time steps (to ensure the constant flux assumption is valid) are alternated with the reactivity calculations. The portion of the code performing the diffusion theory calculations is based on the AIM series of codes. Spatial distribution of source (or power, as discussed in previous section) and few group fluxes are printed out at each time step, as well as masses for each isotope in each region. SIZZLE was modified to also give fission and capture rates for each isotope in each region and instantaneous conversion ratio at every time step. Furthermore, normalization of the fluxes before each time step was corrected to allow for non-unity eigenvalue and to permit use of a flux averaged ν .

II.2.1.2.4 Reactivity Coefficients & Kinetic Characteristics

W-MABI was used to determine, or to help determine, the various reactivity coefficients investigated in this study. As discussed at the end of this section, W-MABI provides spectral information which is used in conjunction with the REPETITIOUS 3 Monte Carlo code in determining the Doppler coefficient.

The sodium temperature coefficient and sodium void coefficient were evaluated for several conditions. The temperature coefficient was evaluated by repeating W-MABI runs, but with the sodium number density varied by $\pm 10\%$ about the normal case in the specified reactor regions. Assuming linearity -- Δk is generally considered as small -- this change in sodium represents about $\pm 625^\circ\text{F}$ change in Na temperature about the average operating temperature of the reactor. The sodium temperature coefficient is then given by $\Delta k/\Delta T$. The sodium void coefficient is determined by evaluating the change in k from the results of W-MABI runs upon voiding the specified region of sodium. In certain cases, the effect of a partial voiding of 50% of the sodium was investigated. The effects on both sodium coefficients due to local, whole core, core and blanket, and whole reactor sodium changes were also investigated.

The technique of calculating prompt neutron lifetime and the effective delayed neutron fraction is based on the method reported by J. K. Long, et al. (20). This method depends on knowledge of the rate of production and spectrum of prompt neutrons and delayed neutrons, and their importance. The direct and adjoint fluxes are calculated by W-MABI and serve as input to PERT (21), which calculates neutron lifetime and β -effective as well as effects on k due to small perturbations. The delayed neutron fraction and spectrum for fissile and fissionable isotopes of uranium and plutonium were taken from the data of Keepin, et al., as reported in ANL-5800 (22) except for Pu-241, which is given by Cox (23), and for Pu-242 which was estimated by Smith, as reported by Okrent (24).

A new technique for the evaluation of Doppler coefficients in large, dilute fast reactors was used in this study. It involved application of the

Monte Carlo method. Monte Carlo has been used successfully for the evaluation of Doppler coefficients in thermal reactors since 1958 (25 ... 28) and it is natural to extend its use to fast reactors.

The effect of overlapping resonances of different species on the Doppler coefficient has been discussed by Codd (29). He showed that in considering the overlap of a single U^{238} resonance and a single Pu^{239} resonance, the change in fission rate due to Doppler broadening may be either an increase or decrease, depending on the energy spacing between the two resonances. This effect was investigated using the standard techniques of evaluation of resonance integrals (Dresner (30), Chernick and Vernon (31), and Adler, et al. (32)), but modified to include the interference of a U^{238} resonance on the Pu^{239} resonance under consideration. In practice, many resonances of U^{238} and Pu isotopes overlap; and when attempts are made to include this effect, further modification of the standard techniques becomes unmanageable. Furthermore, the resonances of light elements renders the general problem intractable since they may be very broad and hence modify the slowing down density. They may also be present in non-fuel regions (e.g., the resonances of sodium and stainless steel components).

The Monte Carlo method can in principle rigorously include all of these interacting effects. Neutrons can be introduced into the reactor system, distributed in energy according to the fission spectrum, and followed in spatial propagation and in energy degradation as they suffer scattering collisions with various nuclei until absorption occurs. The collection of statistical data as the neutrons slow down can provide information of capture rates, fission rates, scattering rates, flux distribution and spectrum; also values of derivatives of absorption rates with respect to some parameter (i.e., temperature) can be determined. In principle, Monte Carlo can be as accurate as the theoretical models describing microscopic interactions between neutrons and nuclei. The resonances of the light elements as well as heavy elements could be included in a Monte Carlo algorithm.

Available for this study was an advanced version of the Westinghouse REPETITIOUS Monte Carlo code, which is an improvement of the REP (25)

code of Richtmyer and Van Norton. REPETITIOUS 3 is a multiregion, cell-geometry, slowing-down, Monte Carlo code which permits inclusion of one primary isotope having strong isolated resonances characteristic of U^{238} or other fertile material, and of another secondary (lesser amount present) isotope which may have resonance characteristics of fissile materials. Thus, the competition which occurs when two resonance absorbers are present is included in the REPETITIOUS calculation. Because of limitations in the core storage available on the IBM 7094, and in order to obtain reasonable computation times, the methods for accounting for Doppler broadening in the two absorbers are somewhat different; greater accuracy is obtained in the calculation for U^{238} . Effects arising from overlap of resonances in a single absorber or from competition between the resonances of the two materials are calculated to the accuracy permitted by a Breit-Wigner single level representation for the resonances.

The relative probabilities of interactions of various kinds at a given point in the calculation are determined by the cross section of the individual materials for various processes. The cross sections are determined as follows:

1) Resonance Absorber No. 1 (e.g., U^{238})

It is assumed that, in addition to a background $1/v$ component to the cross section, only the two adjacent resonances (i.e., the first resonance having the energy of its maximum greater than E and the first resonance having the energy of its maximum less than E) contribute to the cross section at energy E . It has been found that such a treatment is quite adequate for U^{238} in which the average level spacing is of the order of 18 ev.

For energies less than a designated energy E_R (which separates the resolved from the unresolved resonances), the cross sections are given by

$$\sigma_a(E) = \frac{\text{const}}{\sqrt{E}} + \left[\sqrt{\frac{E}{E_0}} \psi(x, \theta) \sigma_0 \frac{\Gamma_n}{\Gamma} \right]_1 + \left[\sqrt{\frac{E}{E_0}} \psi(x, \theta) \sigma_0 \frac{\Gamma_n}{\Gamma} \right]_2$$

$$\begin{aligned} \sigma_s = \sigma_{\text{pot}} + & \left[\psi(x, \theta) \sigma_0 \frac{\Gamma_n}{\Gamma} + \sqrt{\sigma_{\text{pot}} \sigma_0 \frac{\Gamma_n}{\Gamma}} \chi(x, \theta) \right]_1 \\ & + \left[\psi(x, \theta) \sigma_0 \frac{\Gamma_n}{\Gamma} + \sqrt{\sigma_{\text{pot}} \sigma_0 \frac{\Gamma_n}{\Gamma}} \chi(x, \theta) \right]_2 \end{aligned}$$

where

$$x = \frac{2}{\Gamma} (E - E_0)$$

$$\sigma_0 = 4\pi \lambda_0^2 g \frac{\Gamma_n}{\Gamma}$$

$$\theta = \frac{4E_0 kT}{A\Gamma^2}$$

λ_0 = the wave length of the neutron in the CM system at energy E_0 .

T = the absolute temperature.

A = ratio of nuclear mass to neutron mass.

E_0 = the energy of the neutron at the maximum of the appropriate resonance.

E = the energy of the neutron at which the cross section is calculated.

Γ = the total resonance width.

k = Boltzmann's constant.

g = the statistical spin factor for the nuclide and level involved.

Here Doppler broadening is explicitly contained in the development of the ψ and χ functions through the parameter Θ .

In the unresolved region ($E > E_R$), resonances are determined statistically. The energies corresponding to the maxima for the resonances are fixed with uniform spacing corresponding to the average level spacing determined from data on resolved resonances (33). For each resonance, the value of Γ_γ is constant; and the value of Γ_n is determined on the basis of a random selection from a Porter-Thomas distribution. The same formulas used for the determination of the values for the cross sections of resolved resonances are applicable to those of the unresolved resonances.

2) Resonance Absorber No. 2 (e.g., Pu²³⁹)

For this material, an approximation was made in the treatment of Doppler broadening. It is assumed that the broadened shape is a Gaussian having the correct area and peak value for the cross section. This assumption eliminates the necessity for calculation of the ψ and χ functions at each energy, E , for which the cross section is required.

The cross sections for the resolved region are given by

$$\sigma_c = \sum_{\text{resolved}} \left[\sqrt{\frac{E}{E_0}} \sigma_0 \psi(0, \Theta) \frac{\Gamma_\gamma}{\Gamma} \frac{1}{1+x'^2} \right]$$

$$\sigma_f = \sum_{\text{resolved}} \left[\sqrt{\frac{E}{E_0}} \sigma_0 \psi(0, \Theta) \frac{\Gamma_f}{\Gamma} \frac{1}{1+x'^2} \right]$$

$$\sigma_s = \sigma_{\text{pot}} + \sum_{\text{resolved}} \left[\sigma_0 \psi(0, \Theta) \frac{\Gamma_n}{\Gamma} \frac{1}{1+x'^2} \right]$$

$$\text{where } x' = \frac{2(E-E_0)\psi(0, \Theta)}{\Gamma}$$

Above the region of resolved resonances, statistical resonances are assumed. For each such resonance, Γ_γ is taken to be constant and Γ_f , Γ_n and D (energy spacing between resonances) are drawn from χ -squared distributions with degrees of freedom selected to obtain a good fit to the corresponding distributions determined from experimental data for the resolved resonances. The g values for the statistical resonances are determined by a random number sampling such that the frequency of values matches that of resolved resonances. The cross sections at energy E are then calculated as for the resolved energy range except that the sum is limited to twenty resonances: ten above, and ten below the energy E. The actual distribution in energy and values of parameters for the statistical resonances is determined at the beginning of the calculation, and a redetermination is made periodically after a specified number of histories. Above the statistical region, the cross sections for the second resonance absorber are given by

$$\sigma_f = \frac{\text{const}}{\sqrt{E}} \quad \text{or} \quad \text{const.}$$

$$\sigma_\gamma = \alpha \sigma_f$$

$$\sigma_s = \sigma_{\text{pot}}$$

Both α and the constant in σ_f are input quantities.

3) Non-resonance Materials

The cross sections for non-resonance materials are obtained at all energies as follows:

$$\sigma_a = \frac{\text{constant}}{\sqrt{E}}$$

$$\sigma_s = \sigma_{\text{potential}}$$

Values for the constant and for $\sigma_{\text{potential}}$ are provided for each material. Detailed studies of the degree of validity of the Gaussian approximation to the Breit-Wigner line shape and of the limitation, in the statistical region, to a sum over twenty resonances are still in progress. However, preliminary indications are that these approximations are not as limiting as the uncertainties in nuclear data involved. This method of calculation is also more comprehensive and accurate than computations based on separate calculations for each resonance absorber or on the assumption of non-overlapping resonances.

Previous work on the study of individual isotopes with isolated resonances (5, 34) indicated that in large, dilute fast reactors the primary contribution to the Doppler coefficient is due to neutrons of energy less than 30 kev for U^{238} (fertile materials) and less than 4 kev for Pu^{239} (fissile materials). In consideration of these results, neutrons of about 30 kev energy were injected into the Monte Carlo model representing a geometrical cell containing sodium coolant and a fuel rod. Results indicated that less than 0.2% of neutron absorptions occur below about 100 ev.

Since REPETITIOUS 3 is limited to only two resonance species in its present version, the effect of the 2.85 kev resonance of sodium was investigated separately. Because U^{238} has strong isolated resonances and sodium is a broad resonance (primarily scattering) encompassing on the order of 100 U^{238} resonances, it was believed that the sodium resonance would interfere more strongly with U^{238} than Pu^{239} . Results indicate that the neutron spectrum is depleted near the sodium resonance peak and builds up below 1 kev; however, the difference in the total absorption rate in U^{238} between the resonance treatment of sodium and the constant scattering cross section treatment of sodium proved to be less than the probable error of the sums. Therefore, neglect of the sodium resonances in the Monte Carlo calculations for this study was not considered to be a serious omission.

REPETITIOUS 3 was run for several temperatures. Changes in flux spectrum and distribution, in capture rates in U^{238} and in capture and fission rates in Pu^{239} , due to Doppler broadening of the resonances with temperature, and the interdependences of all these factors are automatically taken into account in producing the Monte Carlo results. The resultant effect of temperature changes on the reactivity is obtained from the product of the relative change in fissioning rate below 30 kev (and, hence, of the relative change in contribution to the neutron source for this energy range since v is effectively constant at these low energies) and the relative importance of this change on reactivity (which is given by the relative contribution to the total source of those neutrons with energy less than 30 kev) in the reactor region under consideration. The Doppler coefficient of reactivity is thus dependent on multigroup calculations only to the extent of obtaining (to the first order) the total contributions of all neutrons of energy less than 30 kev to the source. It is not essential to predict the spectrum below 30 kev, as the main contribution to the source is from neutrons of energy greater than 30 kev. Because Monte Carlo calculations generate their own spectrum, the Doppler coefficients calculated in this study are not nearly as sensitive to spectrum calculated by multigroup diffusion theory as are the other techniques of analysis. In fact, the techniques previously mentioned rely on perturbation theory utilizing direct and adjoint fluxes from multigroup calculations for changes in reactivity. In this regard, there is some question as to the correct formulation of perturbation theory for this application (35). As a by-product, Monte Carlo can also provide effective spatial and energy-averaged, multigroup cross sections.

II.2.1.3 Nuclear Design Analyses

II.2.1.3.1 Scoping Studies of Various Concepts

The initial portion of the nuclear design effort was devoted to a brief survey of the characteristics of various core configurations in order to acquire information about the general characteristics of plutonium-uranium carbide fueled reactors. Inasmuch as the fuel material to be employed was fixed from the outset, some possible variations in design were immediately eliminated. A further restriction was the imposed adoption of a fuel assembly composed of stainless steel clad, cylindrical fuel rods. As in the case of U-Pu C fuel, the utilization of sodium coolant was a prespecified condition for the study. The scoping studies had as their objectives the determination of the orders of magnitude for important parameters such as the breeding ratio, the Doppler coefficient, the sodium void and temperature coefficients and the plutonium-to-uranium ratio for various arrangements of core and blanket, and the study of the sensitivity of the parameters to changes in design. For a given core composition and fuel rod size, the Doppler coefficient is primarily sensitive to Pu-239 enrichment and neutron spectrum. For this reason, spectrum indicators are included in the comparison of various concepts.

The reactor configurations studied can be classified under four general types: isolated cylinders (including pancake cores); stacked pancakes (multiple pancake cores separated by blanket); clustered cylinders; and annular cores. An isolated cylindrical core 6 ft. in diameter x 6 ft. in height, surrounded by a 2 ft. thick blanket, would approximate a carbide fueled 1000 MWe reactor which would be nearly ideal from a mechanical design aspect. This design has virtues of low enrichment and in-core breeding ratios of about one. Unfortunately, however, the sodium temperature and void coefficients are relatively large and positive. Because of the competition, upon loss of sodium, between an increase in reactivity due to hardening of the neutron spectrum and a decrease in reactivity due to

increased leakage, non-positive Na coefficients can generally be achieved in reactor designs with relatively high leakage. If one is restricted to single high leakage cores, unusual shaped cores result, such as pancakes or tall doughnuts, because of the large core volume required to generate 1000 MWe. Considerably more freedom and flexibility are obtained from multiple core concepts.

The results of the preliminary investigation of the various concepts discussed above are summarized in Table II.2.1-3. Cylindrical cores (isolated and/or reflected) with various diameters (1 ft. to ∞) and heights (1 ft. to 18 ft.) were studied, providing L/D ratios from 0 to 18. The characteristics of stacks of pancake core of various diameters (6 ft. to 15 ft.) and thicknesses (9 in., 1 ft), separated by varying blanket thicknesses (6 in. to 18 in.) were examined; as well as annular cores 6 ft. and 9 ft. high and varying core thicknesses (6 in. to 2 ft.). The annular cores were sized to provide the same core volume as a 6 ft. x 6 ft. core. Generally, the core composition was the same for the cases reported in Table II.2.1-3; however, in a few cases it was varied to observe the influence of composition. The fuel was Pu²³⁹ C-U²³⁸C in both core and blanket (1% Pu²³⁹ in blanket).

The first case presented in Table II.2.1-3 is for a core 6 ft x 6 ft surrounded by blanket. It is noted that the enrichment is low, and hence the fuel inventory. The in-core breeding ratio is near unity; therefore, little control is required to compensate for fuel and fission product buildup over a fuel cycle life. The total breeding ratio, including both radial and axial blankets, is considerably larger than the minimum required in the ground rules of this study. Unfortunately, the sodium isothermal temperature coefficient and void coefficient for the whole core are both strongly positive, and it is anticipated that the local sodium void coefficient would also be considerably larger.

Two alternatives were investigated to circumvent the undesirable safety aspects of the large single core with L/D equal to one; namely, the design

of single and multiple high leakage cores. Several annular cores with equal core volumes were studied. In examining the summary of results, it is noted that relatively high enrichments are required in annular cores which have negative sodium coefficients. Such cores also have a large blanket-to-core volume. The taller cores were examined for purposes of providing a different sodium flow pattern but likewise did not give desirable characteristics.

Pancake cores with several L/D ratios were examined, but single cores large enough to meet the power requirements exhibited strongly positive sodium coefficients. It was noted that of all the pancake cores studied, only those with diameters about 6 ft. (or less) gave negative sodium coefficients; but these, of course, were too small to meet the power requirements. This led to the concept of a stack of pancake cores separated by blanket. The characteristics for stacks having 3 cores each are also presented in the summary table. The results indicate that blanket thicknesses separating the cores would need to be at least 1-1/2 ft., and cores less than 9 in. thick or less than 9 ft. in diameter would be required to achieve a reactor design having non-positive sodium coefficients. Under these conditions, a relatively high enrichment is required resulting in a reactor with large blanket to core volume ratio. Furthermore, stacks of more than three cores would tend to result in large pressure drops in coolant flow through the core. Loss of sodium from the cores and blankets generally resulted in more positive sodium coefficients than loss from core alone, due to the coupling effect in the stacked pancakes. This was generally an opposite trend from that noted in the other configurations studied. From the results discussed above, non-positive sodium coefficients appeared achievable only in reactor designs having less desirable mechanical and thermal-hydraulic characteristics.

Rather than introduce additional moderator into the core composition to obtain acceptable sodium coefficients, which tends to increase fuel inventory requirements and decrease core breeding ratio, the achievement of

desirable sodium coefficients was attempted by first investigating other reactor configurations. It is well known that non-positive coefficients are achievable for sodium changes in the entire core with small cores. Therefore, in order to meet power requirements, a study was undertaken to ascertain whether small cores could be clustered in such a manner as to produce desirable safety and reactor physics parameters. The clustering of cores was then investigated by use of one-dimensional multigroup radial calculations performed on cylindrical cores. An infinite lattice of identical cores (surrounded by blanket material) was approximated by using reflected boundary conditions. Isolated reactors of the same geometry and composition were also examined to measure the degree of coupling. The volume fractions of sodium, fuel and stainless steel were varied to study resulting effects on various core parameters. The results of a preliminary study are also presented in Table II.2.1-3. It is noted that a reflected core 3 ft. in diameter and 6 ft. high surrounded by a 2 ft. thick blanket possesses negative sodium void and temperature coefficients for sodium changes in the entire core. It is also noted that the required enrichment is higher than for the 6 ft. x 6 ft. core; but so is the breeding ratio, which would tend to compensate economically for the increased loading. In comparison with other reactor configurations of equal core volume having negative sodium coefficients and discussed previously, the clustered core generally exhibited the lowest enrichment and the highest breeding ratio. The mechanical and thermal-hydraulic aspects of the cluster core design are also more favorable. Because the cluster core design is comprised of identical units of core and blanket, prototyping of smaller sizes to study reactor physics, safety, and mechanical characteristics is especially attractive.

TABLE II.2.1-3
CONCEPTUAL STUDY

	Core Size D x H feet	Volume Fractions						$\frac{\Delta k}{k}$ Na Void in Core	$\frac{\Delta k}{k}$ -10% Na in Core	$\frac{\Delta k}{\Delta T},/^{\circ}F$ Isothermal Core	Pu ²³⁹ Enrichment in Core	Breeding Ratio		Median Energy (Mev) for				Peak/Avg. Power in Core (Radial)
		Core			Blanket							Core	Core & Radial Blanket	Fission	Abs.	Flux		
		Na	Fuel	SS	Na	Fuel	SS											
<u>Isolated Cylinder</u>																		
6	6x6	.50	.40	.10	.30	.56	.14	+0.0137	+0.0031	+5.0x10 ⁻⁶	.105	1.062	1.392	.268	.060	.181	1.72	
1-I	3x6	.50	.40	.10	.30	.56	.14	-0.0075	-0.0002	-.32x10 ⁻⁶	.145	.668	1.428	.318	.066	.186	1.47	
<u>Reflected Cylindrical Cell</u>																		
1	3x6	.50	.40	.10	.30	.56	.14	-0.0075	-0.0002	-.32x10 ⁻⁶	.145	.667	1.471	.317	.064	.184	1.47	
14	3x6	.40	.40	.20	.30	.56	.14	-0.0012	+0.0002	+.32x10 ⁻⁶	.146	.673	1.429	.294	.063	.183	1.50	
10	3x6	.30	.56	.14	.30	.56	.14		+0.0007	+1.1x10 ⁻⁶	.114	.876	1.555	.331	.071	.185	1.54	
11	3x6	.70	.24	.06	.30	.56	.14		-0.0044	-7.0x10 ⁻⁶	.238	.364	1.338	.297	.055	.185	1.36	
12	3x6	.50	.40	.20	.10	.72	.18		-0.0001	-.16x10 ⁻⁶	.144	.667	1.489	.319	.066	.190	1.46	
13	3x6	.50	.40	.20	.50	.40	.20		-0.0003	-.48x10 ⁻⁶	.146	.666	1.438	.313	.060	.174	1.47	
4	1x6	.50	.40	.20	.30	.56	.14	-.0256	-0.0024	-3.8x10 ⁻⁶	.438	.111	1.493	.539	.077	.184	1.18	
7	3x3	.50	.40	.20	.30	.56	.14		-0.0027	-4.3x10 ⁻⁶	.168	.543	1.205	.346	.081	.199	1.47	
8	3x9	.50	.40	.20	.30	.56	.14		+0.0005	+8.0x10 ⁻⁶	.140	.701	1.545	.310	.060	.180	1.46	
9	3x18	.50	.40	.20	.30	.56	.14		+0.0010	+1.6x10 ⁻⁶	.136	.726	1.601	.306	.058	.178	1.46	
<u>Annular Core</u> Volumes equivalent to 6x6 core																		
21	Two 6" Thick Cores I.D.=4', ~12', respectively 6'H	.50	.40	.10	.30	.56	.14	-0.0044	-0.0002	-.32x10 ⁻⁶	.332	.195	1.526	.420	.074	.187	1.04	
22	6" Thick Core, 17.4' ID, 6'H	.50	.40	.10	.30	.56	.14	-0.0044	-0.0003	-.48x10 ⁻⁶	.333	.194	1.516	.421	.075	.187	1.04	
23	1' Thick Core, 8' ID, 6'H	.50	.40	.10	.30	.56	.14	-0.0014	+0.0001	+.16x10 ⁻⁶	.193	.448	1.472	.351	.069	.187	1.09	
24	2' Thick Core, ~ 2 1/2' ID, 6'H	.50	.40	.10	.30	.56	.14	+0.0144	+0.0018	+2.9x10 ⁻⁶	.126	.822	1.419	.291	.063	.183	1.22	
25	1' Thick Core, ~ 5' ID, 9'H	.50	.40	.10	.30	.56	.14	+0.0053	+0.0007	+1.1x10 ⁻⁶	.185	.475	1.539	.342	.065	.183	1.09	
26	Same but Int. blkt. replaced by Na & SS	.50	.40	.10	.30	.56	.14	+0.0081	+0.0010	+1.6x10 ⁻⁶	.167	.623	1.252	.278	.056	.185		
27	Same but Int. blkt. replaced by Na.	.50	.40	.10	.30	.56	.14	+0.0088	+0.0010	+1.6x10 ⁻⁶	.161	.681	1.303	.267	.048	.182		

TABLE II 2 1-3 (Continued)

	Core Size	Volume Fractions						$\frac{\Delta k}{k}$	$\frac{\Delta k}{k}$	$\frac{\Delta k}{\Delta T}$, /°F	Pu ²³⁹ Enrichment in Core	Breeding Ratio		Median Energy (Mev) for				
		Core			Blanket							Core	Core & Blanket (Radial or Axial)	Fission	Abs.	Flux	Peak/Avg. Power in Core (axial)	
		D X H	Na	Fuel SS	Na	Fuel SS	Na Void in Core											-10% Na in Core
<u>Isolated Cores - Axial Calc.</u>																		
100	∞x6	.50	.40	.10	.50	.40	.10	+0.0768	+0.0036	+5.7x10 ⁻⁶	.087	1.382	1.482	.243	.052	.172	1.35	
102	6x6	.50	.40	.10	.50	.40	.10	+0.0137	+0.0013	+2.1x10 ⁻⁶	.106	1.062	1.183	.268	.068	.188	1.35	
<u>Pancake Cores - Axial Calc.</u>																		
111	6x3	.50	.40	.10	.50	.40	.10	-0.0027	+0.0004	+6.4x10 ⁻⁶	.122	.867	1.182	.288	.070	.188	1.24	
112	12x3	.50	.40	.10	.50	.40	.10	+0.0338	+0.0019	+3.0x10 ⁻⁶	.107	1.038	1.422	.268	.057	.175	1.23	
121	6x2	.50	.40	.10	.50	.40	.10	-0.0118	-0.0001	-.16x10 ⁻⁶	.142	.700	1.185	.310	.073	.189	1.17	
122	12x2	.50	.40	.10	.50	.40	.10	+0.0192	+0.0011	+1.8x10 ⁻⁶	.125	.834	1.417	.287	.059	.176	1.17	
123	15x2	.50	.40	.10	.50	.40	.10	+0.0241	+0.0025	+4.0x10 ⁻⁶	.123	.855	1.454	.284	.057	.174	1.17	
131 1' Thick Blanket	6x2	.50	.40	.10	.50	.40	.10	-0.0147	-0.0003	-.48x10 ⁻⁶	.145	.847	1.184	.315	.090	.205	1.19	
132	12x2	.50	.40	.10	.50	.40	.10	+0.0156	+0.0010	+1.6x10 ⁻⁶	.127	.986	1.380	.293	.072	.191	1.18	
133	15x2	.50	.40	.10	.50	.40	.10	+0.0203	+0.0012	+1.9x10 ⁻⁶	.125	1.008	1.411	.290	.070	.189	1.18	

TABLE II 2 1 3 (Continued)

(Case)	Core Thick (ft)	Blk Thick (ft)	Core and Plkt 1a (ft)	Volume fractions						$\frac{\Delta k}{k}$ Na Void in Core/ Reactor	$\frac{\Delta k}{k}$ -10% Na in Core	$\frac{\Delta k}{\Delta T},/^{\circ}\text{F}$ Isothermal Core	Pu ²³⁹ Enrichment in Core	Breeding Ratio		Median Energy (Mev) for			Peak/Avg Power in Core (axial)
				Core Na	Core Fuel	Core S	Blanket Na	Blanket Fuel	Blanket S					Core	Core & Blanket (Axial)	Fission	Abs	Flux	
Stacked Pancake Cores (3) - Axial Calc																			
204	1	5	6	50	40	10	30	56	14	- 0048 - 0044	+ 0001	+ 16x10 ⁻⁶	167	577	1 053	313	068	206	1 19
205	1	5	9	50	40	10	30	56	14	+ 0124 + 0156	+ 0008	+ 1 3x10 ⁻⁶	173	673	1 181	298	077	197	1 19
206	1	5	12	50	40	10	30	56	14	+ 0198 + 0242	+ 0011	+ 1 8x10 ⁻⁶	14	686	1 235	292	073	193	1 19
207	1	5	15	50	40	10	30	56	14	+ 0234 + 02	+ 0013	+ 2 1x10 ⁻⁶	144	704	1 265	289	071	191	1 19
200	1	1	6	50	40	10	30	56	14	- 0087 - 0049	- 0002	- 32x10 ⁻⁶	191	467	1 218	340	081	199	1 08
201	1	1	9	50	40	10	30	56	14	+ 006 + 0143	+ 0005	+ 80x10 ⁻⁶	1 5	529	1 358	323	071	190	1 08
202	1	1	12	50	40	10	30	56	14	+ 0133 + 0226	+ 0008	+ 1 3x10 ⁻⁶	169	555	1 418	316	068	187	1 08
203	1	1	15	50	40	10	30	56	14	+ 016 + 0269	+ 0009	+ 1 4x10 ⁻⁶	165	574	1 460	313	066	185	1 08
208	1	1 5	6	50	40	10	30	56	14	- 0138 - 0138	- 0005	- 80x10 ⁻⁶	203	418	1 290	359	079	196	1 08
209	1	1 5	9	50	40	10	30	56	14	+ 0009 + 0045	+ 0002	+ 32x10 ⁻⁶	187	472	1 435	341	069	188	1 08
210	1	1 5	12	50	40	10	30	56	14	+ 0071 + 0123	+ 0004	+ 64x10 ⁻⁶	181	495	1 498	334	066	185	1 08
211	1	1 5	15	50	40	10	30	56	14	+ 0103 + 0163	+ 0006	+ 96x10 ⁻⁶	178	507	1 530	330	064	183	1 08
220	75	5	6	50	40	10	30	56	14	- 0077 - 0098	- 0001	- 16x10 ⁻⁶	195	465	1 009	327	096	211	1 18
221	75	5	9	50	40	10	30	56	14	+ 0068 + 00	+ 0005	+ 80x10 ⁻⁶	179	525	1 125	312	084	202	1 18
222	75	5	12	50	40	10	30	56	14	+ 0129 + 0152	+ 0008	+ 1 3x10 ⁻⁶	173	551	1 175	300	080	199	1 18
223	75	5	15	50	40	10	30	56	14	+ 0161 + 0191	+ 0009	+ 1 4x10 ⁻⁶	170	564	1 201	303	078	197	1 18
224	75	1	6	50	40	10	30	56	14	- 0090 - 0051	- 0002	- 32x10 ⁻⁶	228	362	1 221	358	085	200	1 07
225	75	1	9	50	40	10	30	56	14	+ 0039 + 0122	+ 0003	+ 48x10 ⁻⁶	210	409	1 354	340	074	192	1 07

TABLE II.2.1-3 (Continued)

(Case)	Core Thick. (ft)	Blkt. Thick. (ft)	Core and Blkt. Dia. (ft)	Volume Fractions						$\frac{\Delta k}{k}$ Na Void in Core/ Reactor	$\frac{\Delta k}{k}$ -10% Na in Core	$\frac{\Delta k}{\Delta T}, ^\circ F$ Isothermal Core	Pu ²³⁹ Enrichment in Core	Breeding Ratio		Median Energy (Mev) for			Peak/Avg. Power in Core (axial)	
				Core			Blanket							Core	Core & Blanket (Axial)	Fission	Abs.	Flux		
				Na	Fuel	SS	Na	Fuel	SS											
<u>Stacked Pancake Cores (Cont'd)</u>																				
226	.75	1	12	.50	.40	.10	.30	.56	.14	+0.0093 +0.0196	+0.0005	+0.80x10 ⁻⁶	.203	.430	1.412	.333	.071	.189	1.07	
227	.75	1	15	.50	.40	.10	.30	.56	.14	+0.0121 +0.0233	+0.0006	+0.96x10 ⁻⁶	.200	.440	1.441	.329	.069	.188	1.07	
228	.75	1.5	6	.50	.40	.10	.30	.56	.14	-0.0134 -0.0136	-0.0007	-1.1x10 ⁻⁶	.248	.313	1.308	.383	.082	.196	1.06	
229	.75	1.5	9	.50	.40	.10	.30	.56	.14	-0.0013 +0.0029	+0.0001	+0.16x10 ⁻⁶	.229	.353	1.449	.363	.072	.189	1.06	
230	.75	1.5	12	.50	.40	.10	.30	.56	.14	+0.0038 +0.0099	+0.0004	+0.64x10 ⁻⁶	.221	.370	1.509	.356	.068	.186	1.06	
231	.75	1.5	15	.50	.40	.10	.30	.56	.14	+0.0064 +0.0135	+0.0005	+0.80x10 ⁻⁶	.218	.379	1.540	.352	.067	.184	1.06	

II.2.1.3.2 Selection of Concept for Intensive Analysis

As a consequence of the investigations described above, and of a parallel study of available reports on programs conducted elsewhere, certain design criteria were established that led to the selection of a specific concept for further intensive analysis. The design criteria selected were, in part, determined by the characteristics of fast reactors in general; and, in part, were determined by the special characteristics of plutonium carbide fuel. For this reason, the design evolved in this study is considered particularly well adapted to the fuel under consideration. However, the attractive features of the design concept are such that, in the event that an alternate fuel appears to be more promising, the application of such a fuel to the design selected in this study should receive serious consideration.

Consideration of the special characteristics of plutonium carbide as a fast reactor fuel leads to the following conclusions. First, with regard to Pu-239 as the principal component of the fuel, it is significant that the capture-to-fission ratio of the isotope increases rapidly with decreasing energy in the range of energies significant in fast reactor spectra. Consequently, designs selected to maintain a hard spectrum produce more neutrons per absorption in fuel material, so that criticality can be maintained with reduced loadings or higher breeding ratios can be attained. Also, the energy dependence of α is such that the shift of the mean fission energy toward higher energies results in a reduced positive spectral component in the sodium density coefficient of reactivity. Secondly, the special characteristics of the carbide of plutonium, as compared with the oxide, are high density and high thermal conductivity. The high density is a factor which permits increased concentration of fertile material in the presence of sufficient fuel to maintain criticality. Direct fission of fertile material leads to appreciable improvements in fuel utilization and breeding gain, and increased capture in fertile material at low energies has a favorable influence on the Doppler coefficient. The most significant effect of the

high thermal conductivity of Pu C is its potential for achieving higher power densities in the fuel elements. However, for comparable power densities when compared with oxide fuel, lower fuel operating temperatures are obtained and larger temperature changes can be accommodated. Thus, for a given value of Doppler temperature coefficient of reactivity, a larger reactivity change can be achieved with Doppler effect without core damage in a carbide fueled reactor than in an oxide fueled reactor.

To exploit to a maximum extent the favorable properties of plutonium carbide fuel, the ground rules for nuclear design were specified as follows:

- 1) The core should have a non-positive sodium temperature coefficient if possible, and should exhibit a negative reactivity insertion upon complete removal of sodium from the core. This condition was later extended to state that the greatest positive reactivity insertion associated with partial sodium voiding of the core should be small enough to be compensated for by Doppler effect without core damage.
- 2) The Doppler coefficient of reactivity should be negative. This would be valid for any reasonable plutonium to uranium ratio.
- 3) The core should be a "hard spectrum" design, so that the breeding ratio can be as large as possible.
- 4) The fuel management program would involve loading of approximately one-sixth of the core at a time, with a loading frequency of no more than once every six months.
- 5) The controlled reactivity would be the minimum consistent with the preceeding conditions.

Because of the stated requirements with regard to sodium coefficient, a large central core with surrounding blanket was not deemed acceptable. The achievement of a negative void coefficient depends upon a substantial

leakage of neutrons from the core to a blanket region, so that segmentation of the core by regions of blanket material is required. A segmented core design was also found to have attractive breeding features. In the absence of such segmentation, the loading of fuel in the reactor is determined by the condition of criticality, so that as core size increases the plutonium to uranium ratio decreases and the reactor spectrum becomes softer. This effect tends to reduce the breeding ratio due to the high capture to fission ratio of Pu-239 at low energies. By utilization of a segmented core, it is possible to control the spectrum by adjustment of the size of the core regions, so that a high breeding ratio can be maintained as well as a negative sodium coefficient. The disadvantages of the segmented core design are the reduction of the Doppler effect in the harder spectrum and the increased fuel inventory; although the latter tends to be balanced, economically, by the high breeding ratio. However, there was sufficient flexibility in the design to meet the first criterion stated above with regard to interaction of the Doppler and sodium void effects. Furthermore, the control specifications were considered sufficiently adaptable to handle the net loss of reactivity associated with changes in isotopic composition of fuel in core and blanket. To provide further assurance of good transient response to temperature increases, the CEX fuel assembly with a strong negative mechanical temperature coefficient of reactivity was selected. The above considerations led naturally to a modular design consisting of a number of identical core regions arranged symmetrically within a common blanket. This design was recognized as having the additional advantage of being almost ideally adapted to prototyping at a fraction of the 1000 MWe capacity.

II.2.1.3.3 Parametric Studies of the Unit Cell Core

The procedures and results as presented here indicate that arrival at a good design is a relatively straightforward matter, and that reasonable values for design parameters are obtainable by application of currently available calculational methods and nuclear data. In undertaking the final design, extensive studies leading to a quantitative validation of nuclear data and analytical models will be required in order to select the proper fuel composition, core volume fraction, core unit sizes and spacing, control requirements, etc. However, it is believed that the final design general characteristics will be within the ranges of parameters covered in the studies discussed below. Actual characteristics of the representative design discussed here present a unified final result for the study and are subject to the same basic uncertainties described above.

Table II.2.1-3 also included characteristics of the 3 ft. x 6 ft. reflected cell core for various compositions of sodium, fuel, and stainless steel for core and blanket. In comparison to the first case of a 3 ft. x 6 ft. reflected core (volume fractions of .5, .4, .1 for sodium, fuel, and stainless steel, respectively, for the core and of .3, .56, .14, respectively, for the blanket), change in blanket composition has only a relatively small effect on sodium temperature coefficient, enrichment, radial power peaking and core breeding ratio, but has a large effect on blanket breeding. For this reason, a blanket composition with maximum fuel content attainable, consistent with thermo-hydraulic design, is desirable. The blanket composition stated above is therefore used for the remainder of the study. The influence of core composition on the various parameters is also presented graphically in Figures II.2.1-9 through II.2.1-12. Variations of isothermal sodium temperature coefficient, enrichment, and breeding ratios for core and core plus radial blanket with changes in volume fraction of sodium in the core are illustrated for reflected cores 3 ft. x 6 ft. and 2 ft. x 6 ft.

The curves are drawn for fuel and stainless steel volume fractions of 80% and 20%, respectively, of the non-sodium volume fraction. Points "a, b, c and d" in the figures indicate the effect on the various core parameters of increases of the amount of stainless steel present from the normal points on the curve. Points "a, b, and d" represent a doubling of the stainless steel volume fraction from .10 to .20, and point "c" from .06 to .12. To compensate for the change in stainless steel present, the sodium volume fraction is reduced for conditions "a, b and c", and the fuel volume is reduced for condition "d". The core breeding ratio and enrichment are significantly changed only for condition d; whereas significant changes are evident for core plus blanket breeding ratio and sodium temperature coefficient for all conditions.

In order to perform a degree of optimization on a unit cell of the clustered core design, further studies of core characteristics were made. Core diameters were varied from 6 in. to 3 ft., and radial blanket thicknesses from 0 in. to 2 ft. The height remained constant at 6 ft. Due to coupling between cores, the thickness of the blanket separating cores influences the various parameters. The sodium temperature coefficient is especially sensitive to blanket thickness associated with each cell, as shown in Figure II.2.1-13. In order to demonstrate this phenomenon and to gain insight toward minimizing the ratio of blanket volume to core volume, the dependence on this ratio of sodium temperature coefficient, sodium void coefficient (for entire core and entire reactor), Pu enrichment in core, breeding ratio (core and core plus radial blanket), and radial peak to average power factor is illustrated for various core sizes in Figures II.2.1-15 through II.2.1-21. Since the results are based on a one-dimensional study, the abscissa in the figures is the ratio of blanket area-to-core area, taken at midplane. Figure II.2.1-14 relates radial blanket thickness surrounding each core to this same ratio. Core and blanket compositions were fixed at the nominal values presented earlier in this section. In examining these figures, a saturation effect is observed. Also it is noted for the four diameters of cores studied that in

using a value of zero as a reference for the reactivity coefficient, the minimum blanket-to-core ratio occurs with a 2 ft. core for the sodium temperature coefficient, and the minimum ratio occurs with a 3 ft. core for the sodium void coefficients. However, for the latter parameters, the difference between the blanket to core ratios for the 2 ft. and 3 ft. cores is small near the condition of zero reactivity change in voiding the entire core. The 3 ft. core has a lower enrichment requirement and gives a higher core breeding ratio, but has a lower blanket breeding ratio and a higher peak-to-average power ratio.

Figure II.2.1-9 Variation of Isothermal Sodium Temperature Coefficient with Sodium Volume Fraction

Figure II.2.1-10 Variation of Enrichment with Sodium Volume Fraction

Figure II.2.1-11 Variation of Core Breeding Ratio with Sodium Volume Fraction

Figure II.2.1-12 Variation of Core & Radial Blanket Breeding Ratio with Sodium Volume Fraction

Figure II.2.1-13 Variation of Isothermal Sodium Coefficient with Blanket Thickness

Figure II.2.1-14 Relation Between Blanket Thickness and A_b/A_c

Figure II.2.1-15 Variation of Isothermal Sodium Coefficient with A_b/A_c

Figure II.2.1-16 Variation of Sodium Void Coefficient (Core) with A_b/A_c

Figure II.2.1-17 Variation of Sodium Void Coefficient (Reactor) with A_b/A_c

Figure II.2.1-18 Variation of Enrichment with A_b/A_c

Figure II.2.1-19 Variation of Core Breeding Ratio with A_b/A_c

Figure II.2.1-20 Variation of Core & Radial Blanket Breeding Ratio with A_b/A_c

F. C. R. - STUDY (PEAK/ AVE.) POWER VS. A_b/A_c

Figure II.2.1-21 Variation of Radial Peak to Average Power Density Ratio with A_b/A_c

Figure II.2.1-22 Radial Distribution of Neutron Source for Various Blanket Thicknesses

Figure II.2.1-23 Radial Distributions of Neutron Flux for Various Blanket Thicknesses

II.2.1.3.4 Development of Reference Design

With insight gained from the studies reported in the previous sections, and as a result of preliminary investigations concerned with mechanical, materials and thermo-hydraulic considerations, a fast reactor design consisting of a cluster of seven core units 6 ft. high with ~ 3 ft. effective diameter immersed in blanket material evolved to at least meet the ground rules laid down in this study. This section describes the results of further studies performed to more thoroughly investigate the characteristics of the reference design. As a result of specification of fuel rod size, pitch, clad thickness, and carbide pellet size, homogenized compositions for the model used in reactor physics analysis were determined. The core composition is based on the CEX design for fuel assemblies. Composition of core, blanket, reflector, and control regions are shown in Table II.2.1-4.

The design of the seven-cluster core was first examined in one-dimensional geometry. The central core and the inner portion of the outer cores are represented by reflected cell geometry; i.e., a $2\frac{3}{4}$ ft. diameter core surrounded by a blanket about 1 ft. thick or less, with a boundary condition at the outer edge of the cell described by a zero flux gradient. The validity of this approximation is discussed in Section II.2.1.3.6 where comparison is made with two-dimensional calculations. The outer portions of the outer cores are represented by a conventional isolated cylinder geometry.

Summarized results of one-dimensional multi-group calculations using the reference design compositions are presented in Table II.2.1-5. Control regions are provided near the center of the core and near the core blanket interface primarily to produce few group cross sections for two-dimensional studies of control rod worth. The first case presented is for a blanket thickness of about 1 ft. The effects of replacing the outer portion of blanket by other materials, such as nickel and graphite reflectors, were also investigated. As indicated in the summary of results (Table II.2.1-5) the use of a graphite reflector shows distinct advantages in that the blanket

volume is substantially reduced to the extent that the ratio of radial blanket area to core area at the center plane is less than 1.5. This is achieved without significantly reducing the core plus radial blanket breeding ratio. Furthermore, the sodium coefficients are improved (made more negative) due to the decreased coupling caused by a softened spectrum near the graphite. This decrease in coupling between cores also has the effect of only slightly increasing enrichment requirements. Because the benefits were considered to more than compensate for the penalty of increased enrichments, the graphite reflected core was selected as the reference design. The placement of a moderator outside the radial blanket does not appreciably effect the core spectrum, but does improve the specific blanket breeding ratio. The effect of Pu-239 buildup in the radial blanket on the sodium coefficients is presented in Table II.2.1-6. Results presented previously were for 1% Pu-239 in the blanket. The effect on reactivity of Pu-239 buildup in the blanket is observed to be nearly linear, and the effect on sodium coefficients is to make them less negative as was expected.

The design studies presented up to this point were performed with the APDA (18-R2) cross section library. With the availability of the new Westinghouse WAPD-F1 library, described and discussed in Section II.2.1.2.1, further investigations of the reference design were performed. The results are summarized in Table II.2.1-7. In addition to the Pu-239-U-238 carbide fuel, which permitted rapid and convenient concentration searches for criticality and were used in the preliminary calculations to observe reactor characteristics and trends, mixed Pu fueled cores were also examined. The Pu mixtures were obtained from the temporal studies discussed later and represent equilibrium conditions. Since the new WAPD-F1 library leads to higher core reactivities than the library used in the SIZZLE burnup code, the amount of Pu was reduced in the core but the relative amounts of plutonium isotopes were kept constant.

With the Roundelay refueling scheme, the sodium void coefficients for the entire core are negative -- even with 4% Pu-239 content in the blanket; however, further studies were made to investigate reactivity coefficients for local sodium changes. The results of this study are presented in Figures II.2.1-24a through II.2.1-26b, and indicate the dependence of sodium coefficients on sodium changes that occur throughout a partial volume of the core. The sodium changes (10% decrease in sodium density, sodium void and increase in sodium temperature) occur uniformly from the center of the core outward to the value of the radius on the abscissa. It is seen also that the local sodium coefficients are very sensitive to Pu content in the blanket and to cross section libraries. It was noted in Figure II.2.1-25b that replacement of a fraction of the sodium coolant (20%) by a solid moderator (graphite) causes about a 15% reduction in the maximum value of the local sodium void coefficient; however, this is not deemed necessary for the Roundelay refueling scheme.

TABLE II.2.1-4

Region Composition

<u>Region</u>	<u>Material</u>	<u>Volume Fraction</u>	<u>Number Density 10²⁴ (atoms or mol/cc)</u>
Core (No F.P. unless noted)	Na	.551	.0121
	SS	.155	.0132
	(U-Pu)C	.294	.00856
Radial Blanket (No F.P. unless noted)	Na	.25	.0054
	SS	.20	.0162
	(U-Pu)O ₂	.55	.0120
Axial Blanket (No F.P. unless noted)	Na	.551	.0121
	SS	.155	.0132
	(U-Pu)C	.294	.00856
Control Channels (No Poison)	Na	.75	.0162
	SS	.25	.0213
Control Channels (Poison)	Na	.5	.0108
	SS	.2	.0170
	B ₄ C	.3	.0063
Radial Graphite Reflector	Na	.05	.00108
	SS	.05	.00426
	C	.90	.0720
Radial and Axial Reflectors	Na	.75	.0162
	SS	.25	.0213

TABLE II.2.1-5
PARAMETRIC SURVEY

		k_{eff} (Normal)	$(\frac{\Delta k}{k})_{Na}$ Core	Void Reactor	$(\frac{\Delta k}{k})_{-10\% Na}$ Core	Reactor	Isothermal Na Coef. $\Delta k/\Delta T, /^{\circ}F$		Pu^{239} Enrichment	Breeding Ratio		Peak/Avg. Power in Core (Radial)
							Core	Reactor		Core	Core & Radial Blanket	
<u>Reflected Cylindrical Cell</u>												
401	29.86 cm blanket	1.0000	-.0118	-.0132	-.0006	-.0007	$-.9 * 10^{-6}$	$-1.1 * 10^{-6}$	.1772	.54	1.36	1.39
402	29.86 cm blkt., Poison inserted	.9868	-.0084	-.0082	-.0003	-.0004	$-.4 * 10^{-6}$	$-.6 * 10^{-6}$	.1772			1.34
403	29.86 cm blkt., Also C.C. at core periphery	.9996	-.0125	-.0189	-.0006	-.0007	$-1.0 * 10^{-6}$	$-1.1 * 10^{-6}$	.1772	.54	1.34	1.39
404	29.86 cm blkt., Poison inserted outer C.C.	.9714	-.0179	-.0203	-.0012	-.0011	$-1.9 * 10^{-6}$	$-1.8 * 10^{-6}$	.1772			1.49
411	23.66 cm blkt., 6.20 cm Ni reflector	1.0000	-.0117	-.0131	-.0006	-.0007	$-.9 * 10^{-6}$	$-1.2 * 10^{-6}$	.1772	.54	1.31	1.39
412	23.66 cm blkt., 6.20 cm Graphite reflector	1.0000	-.0127	-.0161	-.0006	-.0009	$-1.0 * 10^{-6}$	$-1.4 * 10^{-6}$	.1780	.53	1.35	1.40
<u>Isolated Cylinder</u>												
427	23.66 cm blkt. 12.40 cm Graphite reflector	.9966	-.0146	-.0208	-.0008	-.0012	$-1.2 * 10^{-6}$	$-1.9 * 10^{-6}$	.1780	.53	1.21	1.40

NOTE:

- (1) Control Channel (C.C.) near center of core (normally no poison)
- (2) 1% Pu^{239} in blanket
- (3) Calculations performed using APDA (18-R2) Library

TABLE II.2.1-6

Effect of Pu²³⁹ Buildup in Blanket on Na Coefficients

Pu ²³⁹ Content in Blanket	$(\frac{\Delta k}{k})$ Na Void		$(\frac{\Delta k}{k})$ -10% Na		<u>Isothermal Na Coeff.</u>	
	Core	Reactor	Core	Reactor	$\Delta k/\Delta T, /^{\circ}\text{F}$	
1%	-.0127	-.0161	-.0006	-.0009	-1.0×10^{-6}	-1.4×10^{-6}
2%	-.0093	-.0114	-.0003	-.0005	$-.5 \times 10^{-6}$	$-.8 \times 10^{-6}$
3%	-.0057	-.0066	-.00004	-.0001	$-.02 \times 10^{-6}$	$-.2 \times 10^{-6}$
4%	-.0023	-.0019	+.0003	+.0003	$+.5 \times 10^{-6}$	$+.5 \times 10^{-6}$
5%	+.0012	+.0028	+.0006	+.0007	$+.9 \times 10^{-6}$	$+1.1 \times 10^{-6}$

NOTE:

- (1) Calculations performed using APDA (18-R2) Library
- (2) 23.66 cm. blanket, 6.20 cm. Graphite reflector
- (3) Reflected cell geometry

TABLE II.2.1-7

Sensitivity of Na Coefficient to Fuel Composition

	$(\frac{\Delta k}{k})_{\text{Na Void}}$		$(\frac{\Delta k}{k})_{-10\% \text{ Na}}$		$\frac{\text{Isothermal Na Coef.}}{\Delta k / \Delta T, / ^\circ \text{F}}$	
	<u>Core</u>	<u>Reactor</u>	<u>Core</u>	<u>Reactor</u>	<u>Core</u>	<u>Reactor</u>
Pu is only Pu ²³⁹ , No F.P., 1% Pu ²³⁹ in blanket	-.0276	-.0320	-.0024	-.0028	-3.9x10 ⁻⁶	-4.5x10 ⁻⁶
Pu is only Pu ²³⁹ , 5.5% F.P. pairs, 4% Pu ²³⁹ in blanket	-.0091		-.0005		-.8x10 ⁻⁶	
Mixed Pu, No F.P., 1% Pu ²³⁹ in blanket	-.0188	-.0230	-.0014	-.0018	-2.3x10 ⁻⁶	-2.9x10 ⁻⁶
Mixed Pu, 5.5% F.P. pairs, 4% Pu ²³⁹ in blanket	-.0011	-.0015	+.0003	+.0002	+.45x10 ⁻⁶	+.38x10 ⁻⁶

NOTE:

1) Calculations performed using WAPD-F1 library.

Figure II.2.1-24a Variation of Partial Sodium Coefficient (-10% in density) with Core Radius

Figure II.2.1-24b Variation of Partial Sodium Coefficient (-10% in density) with Core Radius

Figure II.2.1-25a Variation of Partial Sodium Void Coefficient with Core Radius

Figure II.2.1-25b Variation of Partial Sodium Void Coefficient with Core Radius

Figure II.2.1-26a Variation of Local Sodium Temperature Coefficient with Core Radius

Figure II.2.1-26b Variation of Local Sodium Temperature Coefficient with Core Radius

2.1.3.5 Characteristics of the CEX Assembly

The CEX assembly has the potential for providing a rapid negative temperature (or power) coefficient. In order to provide information for the transient analysis performed on the CEX assembly, its nuclear characteristics were studied and the results are summarized in Table II.2.1-8. The expansion coefficients were obtained from one-dimensional, multigroup analysis in the axial direction. Various components of expansion were investigated separately in the steady state calculations and were combined in the proper time relationship in the transient analysis. The fast-acting expansion of the cermet structure of each assembly produces a gap in fuel at the center of the core, a fact which causes a decrease in reactivity.

Two sodium gap sizes were investigated, 1/2 in. and 1 in. total gap. From these results, the coefficient of reactivity for gap expansion is - .0078 per 1% expansion of the cermet rods. Tall cores have the advantage over short cores of producing more gap for a given temperature rise throughout the core. Expansion of the fuel cladding takes longer than cermet expansion because of the time delay in carbide. Time delays for expanding the core structure would be even greater. For comparative purposes, these expansion coefficients are also presented. After expansion of the cermet due to an increase in temperature, clad expansion tends to cancel the cermet effect; but, as shown in the prescribed table, the resultant total core isothermal expansion coefficient is negative.

TABLE II.2.1-8

Reactivity Characteristics of CEX Assembly

	<u>$\delta k/k$</u>
1% expansion of cermet causing a .72" Na gap at center plane of CEX ass'ys.	-.0078
1% expansion of core height (Na density kept constant)	-.0044
1% expansion of core radius (Na density kept constant)	-.0056

NOTE: Calculations performed with WAPD-F1 library in axial geometry

II.2.1.3.6 Two-Dimensional Studies

In order to investigate the degree of coupling between the core modules, the effect of control rods, and the details of the power distribution, the PDQ-03 (16) two-dimensional diffusion theory program was utilized. The calculation was performed with four neutron groups with group constants compacted from the 18-group constants employed for the majority of the survey and design calculations. Results of calculations for a single core module were found to agree well with those determined by W-MABI, a one-dimensional diffusion theory program.

Figures II.2.1-27 and II.2.1-28 (which represent one quarter of the reactor) show the geometry used to represent the reactor within the framework of the rectangular mesh of the PDQ code. The zones corresponding to the different compositions describe, in the rectangular mesh, the elementary hexagonal fuel assemblies of the reference design.

Figures II.2.1-27 and II.2.1-28 correspond to two different locations of the control rods. It was found that, for the rods in the blanket (Figure II.2.1-27), the decrease in reactivity due to insertion of the rods was $\Delta k/k = 0.035$. For the rods in the center of the core (Figure II.2.1-28) $\Delta k/k = 0.029$. Although the same compositions for control rods were used in both instances, those rods in the blanket are much larger. Figures II.2.1-29 through II.2.1-34 show the fission source distribution for the reactor in the x and y direction. Figures II.2.1-29 and II.2.1-30 correspond to the configuration shown in Figure II.2.1-28 except that the control channels are replaced with core material. Besides the PDQ-03 two-dimensional source distribution, the corresponding curves for the source of the one-dimensional case are shown in this figure. Slight deviations were found at the interface between the blanket and core and at the center of the blanket. The first difference is thought to be explained by the method of source-averaging in a mesh rectangle of PDQ. The second is a two-dimensional geometry effect, as may be discerned by examining the source in the blanket of Figure II.2.1-30.

In Figures II.2.1-31 through II.2.1-34 the effect of the insertion of control rods on the source distribution is shown. The results indicate that practically the same power is developed by each of the core seeds, with a slight predominance for the one centered at the origin of the coordinate system.

Analysis of the two-dimensional calculations indicates that the modules of the reactor are coupled very loosely. The differences in reactivity between a single module in a blanket and reflector and the multiple seed array was found to be .003 and .025 for 1% and 4%, respectively, Pu accrual in the blanket. For this reason, careful consideration would be required in developing a control system capable of maintaining properly matched power loads in the modules. Coupling can be increased, at some loss to the sodium coefficient, by decreasing the separation between modules, or by increasing seed diameters, or by extending radial blanket life. The nearly identical characteristics of individual modules emphasize the possibility of varying core power rating by merely varying the number of modules, and of prototyping a large reactor by the construction of one or a few modules.

Figure II.2.1-27 Quarter-Core Drawing of PDQ Mesh & Region - Control Channel in Core & Blanket

Figure II.2.1-28 Quarter-Core Drawing of PDQ Mesh & Region -- All Control Channels in Core

Figure II.2.1-29 Source Distribution, "Y" Direction - No Control Channels

Figure II.2.1-30 Source Distribution, "X" Direction - No Control Channels

Figure II.2.1-31 Source Distribution, "Y" Direction -
Control Channels In Core & Blanket

Figure II.2.1-32 Source Distribution, "X" Direction - Control Channels in Core & Blanket

Figure II.2.1-33 Source Distribution, "Y" Direction - All Control Channels in Core

Figure II.2.1-34 Source Distribution, "X" Direction - All Control Channels in Core

II.2.1.4 Nuclear Studies - Reference Design

In the course of developing and investigating the reference design, it became apparent that design characteristics, reactivity coefficients, and fuel management were intimately related, yet there existed sufficient flexibility for optimization. Features of reactor geometry, fuel management of both core and blanket control specifications, and composition of core, blanket and reflector could be varied to provide the optimum combination with respect to safety and economics. As discussed previously, breeding ratios, fuel enrichment, and coefficients of reactivity are dependent on cross section values; therefore, the actual optimum design and management scheme are sensitive to new information concerning cross sections (determined experimentally or theoretically in the future); and to new advanced methods of analysis of reactivity coefficients, as well as to employing more elaborate and rigorous computational techniques in depletion analysis.

In the reference design which utilizes internal leakage to obtain desirable sodium coefficients of reactivity, the values of these coefficients tend to become less negative as plutonium accrues in the blanket. Flattening of the power distribution, which is desirable from the aspect of power generation, also tends to produce less negative sodium coefficients. These observations will be discussed in detail in the following sections.

II.2.1.4.1 Fuel Management and Composition and Power Distribution

Since the coupling between core modules is weak, for purposes of fuel management studies each module may be considered as an independent reactor. Calculations indicate that the center core module would be 0.3% more reactive than the outer modules with the same feed fuel composition with a fresh blanket. This would be accommodated by reducing the plutonium concentration of the feed fuel by about 0.6%. This reactivity difference increases with blanket age to about 1.2%.

Buildup of plutonium and power production in the blanket region between modules is substantially different than in the outer blanket regions.

This was investigated in the study. A three-year life at full power was assumed for the core fuel elements, corresponding to an average fuel exposure of 100,000 MWD/MT. The allowable life of blanket elements depends on the core fuel management scheme selected since the core void coefficient is sensitive to blanket age and fuel management scheme. Both 6-month and 1-year refueling intervals were considered.

Two methods of fuel management were considered for the core region: the Roundelay scheme; and out-in zoned reshuffling.

With the Roundelay (36) refueling technique, the fresh fuel elements are distributed uniformly throughout the core, rather than being placed in a discrete region. At the beginning of an equilibrium cycle, with Roundelay-6-Batch refueling corresponding to a 6 month refueling interval, a mixture of elements with 0, 1, 2, 3, 4, and 5 cycles of exposure exists throughout the core. During refueling, it may be desirable to perform some local reshuffling, moving elements with long exposure to positions in the core support with smaller flow orifices. Basically, however, an element remains in the same vicinity throughout its life in the core.

Using the out-in zoned refueling technique, fresh fuel is added to a region at the outside of the core and subsequently moved inward to a new zone. At each refueling, the fuel is removed from the central zone after receiving its full exposure. In the studies, a three-zone core was assumed with a 1-year refueling interval. With a 6-month refueling interval, the three-zone arrangement would be maintained, but only half of the elements in each zone would be shifted during a refueling. The main advantage of the zone loading over the Roundelay technique is that a much lower radial maximum-to-average power density ratio is obtained in the core. A value of 1.27 is obtained for the zone method at a start of a cycle, compared to 1.39 for Roundelay. In addition, a local peaking of power occurs in the fresh fuel adjacent to burned fuel with the Roundelay scheme. This peaking factor can be estimated by comparing the spectrum-weighted macroscopic fission cross section of fresh and burned fuel. The peaking factor from this source was calculated to be 1.14, giving a total radial peaking factor of 1.58 with Roundelay

reshuffling. The radial core power distribution for the equilibrium core module is shown in Figure II.2.1-35 for the three-zone case, and in Figure II.2.1-36 for the Roundelay case. Axial power distributions would be similar for either method of refueling and were calculated as a function of burnup using SIZZLE. The computed axial power distribution is shown in Figure II.2.1-37.

A major disadvantage of the zone-loaded core is that the void coefficient of the core is much more positive than with the Roundelay core. The effect of voiding the zoned core (but not the blanket) at the start of a cycle with a two-year average blanket age was an increase in reactivity of 0.75%. The effect with the Roundelay core was a decrease of reactivity of 0.2%. These calculations were performed with the 18-group SIZZLE code and library, with the U-238 and Pu-239 constants changed to agree with the WAPD-F1 library. While different cross section libraries may show different calculated values for voiding of the core, these calculations indicate that the flattening of core power as a result of the zoned loading results in an increase in the sodium coefficient. Since the increase of plutonium content in the blanket with average age also tends to drive the sodium coefficient more positive, the choice of a zoned core fuel management scheme would undesirably limit the allowable blanket element lifetime. A more detailed study of blanket management methods may show that the effective Pu content in the blanket may be reduced sufficiently to permit zoned loading by shifting blanket elements from the central region of the reactor to the outer edge, and always placing fresh blanket elements next to the core modules in the central part of the reactor. However, based on calculations performed to date, the Roundelay technique was selected for the reference design because of the importance of the sodium coefficients during transient conditions.

The mixing of fresh and burned elements by the Roundelay method provides close coupling between them. If one assumes the flux spectrum is not appreciably different from element to element, then the infinite multiplication constant (k_{∞}) of the mixture can be estimated by averaging the k_{∞} of

the individual elements. To estimate the reactivity behavior of a core module over a burnup cycle, the effective multiplication constant of a batch loaded core was calculated as a function of fuel burnout, using a radial SIZZLE calculation. This curve is shown in Figure II.2.1-38. The calculation automatically takes into account the buildup of plutonium in the blanket over a cycle since both the buildup of plutonium with blanket age and the reactivity effect of plutonium in the blanket are approximately linear functions. Assuming a Roundelay-3-batch refueling, the k_{eff} of the core module at the beginning of a cycle can be obtained by averaging the value of k_{eff} for the batch loaded core at 0, 1, and 2 years. Similarly, the k_{eff} of the module at the end of the one year cycle is obtained by averaging the batch k_{eff} values for 1, 2, and 3 years. Intermediate values are obtained in a similar fashion. Curves of k_{eff} vs. time as shown in Figure II.2.1-38 are for both a 1-year cycle and a 6-month cycle. The plutonium loading for the base case was chosen to give a k_{eff} at the end of a year cycle of 1.0025. The initial k_{eff} for a 6-month cycle has been lowered by $0.005 \Delta k_{eff}$ to give a k_{eff} of 1.0025 at the end of the cycle also. Although this would correspond to a reduction of the initial plutonium loading by 1.0%, the shape of the curve would not be significantly affected. The cases correspond to an average in-place blanket age of 1.5 years. Adjustment of the feed fuel plutonium loading would be necessary to match other blanket ages or management schemes.

A curve of k_{eff} vs. time for the three-zone core is shown in Figure II.2.1-39 for a one-year refueling cycle. Curves are also shown for both a fresh blanket at the start of the cycle and a two year old blanket at the start of the cycle, assuming the same core composition. Depending on the blanket age selected, it would be necessary to adjust the plutonium loading to obtain a k_{eff} of 1.0025 at the end of the cycle. The shape of the curve (A) obtained would be similar to the two year old blanket case since an average blanket age of two years at the start of a cycle corresponds to a blanket element life of 4 years. Curve (A) is included on Figure II.2.1-39.

The reactivity requirements for burnup over a 6-month cycle using Roundelay is $0.90\% \Delta k_{eff}$, and $1.85\% \Delta k_{eff}$ for a 1-year cycle. Corresponding values

for the three-zone refueling scheme are 1.0% for 6 months and 1.8% for one year.

It is of interest to note that the buildup of plutonium in the blanket over a 1-year cycle is about 1.5%, which flattens the reactivity curve of the core by contributing about $2.0\% \Delta k_{\text{eff}}$ to the reactivity of the system. Hence, the reactivity behavior with burnup of a modular core is similar to that of a large core with an internal breeding ratio near unity. In such a system, the reactivity loss over a year cycle would be approximately $1.5\% \Delta k_{\text{eff}}$ to accommodate fission product buildup. In the modular core, reactivity loss is only $1.8\% \Delta k_{\text{eff}}$ per year.

Power density distribution in the radial blanket was determined as a part of the fuel management studies. Radial SIZZLE calculations were made on single core modules with appropriate boundary conditions to describe the inner blanket regions and the outer blanket regions. The power density in the blanket around the central core module and inner half of the outer modules is shown in Figure II.2.1-40 for various blanket ages. The curves are normalized to the average power in a core module at the start of a 1 year cycle. A similar set of curves is shown in Figure II.2.1-41 for the outer blanket. To find the fraction of power coming from the blanket as a function of average blanket age, the curves were numerically integrated over the radial blanket volume of the reactor and are shown in Figure II.2.1-42.

The concentration of Pu-239 in the radial blanket in the central region of the core is relatively constant with radial position. In the SIZZLE calculation, the blanket was divided into four regions and an average value for the Pu-239 concentration was calculated in each region at each time step. These region averaged Pu-239 concentrations are shown in Figure II.2.1-43 as a function of position. The inner two regions shown in the plot represent the two halves of a blanket element adjacent to a core module, while the outer two regions represent the blanket element adjacent to the graphite. Total Pu-239 in the inner two regions is approximately the same as in the outer two regions. It would be possible to maintain a nearly uniform plutonium concentration in the inner blanket by rotating the blanket elements

180° once during their stay in the reactor. A blanket management scheme whereby fresh blanket fuel is always added to the positions next to the core modules and burned elements are moved to the outer blanket zones will have a desirable affect on the sodium coefficient, hence this scheme will be used in the reference design.

The material balance on the Roundelay cycle with a three-year blanket life is shown in Table II.2.1-9. An inventory of the various isotopes in the fuel and blanket elements normalized to the entire core inventory is shown in Tables II.2.1-10, II.2.1-11, and II.2.1-12 for the core, radial blanket and axial blanket, respectively.

The inventory of the various isotopes in the core are shown as a function of time for the three-zone loading scheme in Table II.2.1-13, normalized to 1/3 of the core module.

TABLE II.2.1-9

Material Balance on Roundelay Fuel Cycle

Three Year Core & Blanket Life

(Kg) in Reactor

Isotope	<u>Core</u>		<u>Radial Blanket</u>		<u>Axial Blanket</u>	
	In	Out	In	Out	In	Out
Pu ²³⁹ + Np ²³⁹	3469.4	2785.7		2067.0		157.5
Pu ²⁴⁰	1526.6	1503.2		75.6		2.93
Pu ²⁴¹	216.2	214.4		3.84		0.032
Pu ²⁴²	52.8	52.8		0.096		0.0003
U ²³⁵			169.0	108.5	23.6	20.1
U ²³⁶				12.08		0.74
U ²³⁸	18,412.4	16728.0	56886.3	53815.8	7932.5	7741.5

TABLE II.2.1-10,

Composition of Fuel vs. Time - Roundelay Refueling(Kg) in Reactor

Time (days)	Pu ²³⁹	Pu ²⁴⁰	Pu ²⁴¹	Pu ²⁴²	Np ²³⁹	U ²³⁸
0	3469.4	1526.6	216.2	52.8	0	18412
201	3306.3	1527.9	214.9	52.8	4.6	18077
381	3178.7	1526.4	214.3	52.9	4.5	17785
561	3063.4	1522.7	214.1	52.9	4.3	17503
741	2959.5	1517.4	214.0	52.8	4.2	17233
921	2866.0	1510.8	214.2	52.8	3.97	16974
1095	2781.9	1503.2	214.4	52.8	3.8	16728

TABLE II.2.1-11

Composition of Radial Blanket Vs. Time - Roundelay Refueling(Kg) in Reactor

t (days)	Pu ²³⁹	Pu ²⁴⁰	Pu ²⁴¹	Pu ²⁴²	Np ²³⁹	U ²³⁸	U ²³⁵	U ²³⁶
0	0	0	0	0	0	56886	169.0	0
201	405.6	2.77	0.0028	0.00019	7.16	56362	157.0	2.44
381	764.1	9.58	0.181	0.0017	7.35	55877	146.5	4.54
561	1110.6	20.4	0.559	0.0074	7.52	55378	136.4	6.57
741	1442.9	35.2	1.25	0.022	7.66	54866	126.6	8.50
921	1759.6	53.6	2.32	0.051	7.76	54345	117.3	10.35
1095	2059.2	75.6	3.84	0.101	7.84	53816	108.5	12.08
1138	2126.1	80.7	4.17	0.108	7.87	53716	107.3	12.48
1326	2405.4	105.9	6.17	0.185	7.89	53271	102.0	14.11
1518	2669.7	134.6	8.85	0.315	8.06	52819	97.0	15.67

TABLE II .2.1-12

Composition of Axial Blanket Vs. Time - Roundelay Refueling

Three Year Fuel Life

(Kg) in Reactor

Time (Days)	Pu ²³⁹	Pu ²⁴⁰	Pu ²⁴¹	Pu ²⁴²	Np ²³⁹	U ²³⁸	U ²³⁵	U ²³⁶
0	.0	.0	.0	.0	.0	7932.5	23.6	.0
155	19.6	.0465	6.3×10^{-5}	7.8×10^{-8}	.447	7910.4	23.2	.094
356	46.8	.252	7.9×10^{-4}	2.15×10^{-6}	.486	7880.8	22.6	.222
557	75.4	.652	3.30×10^{-3}	1.44×10^{-5}	.526	7845.5	21.9	.356
758	105.1	1.28	9.10×10^{-3}	5.61×10^{-5}	.631	7808.9	21.3	.496
959	135.9	2.17	2.01×10^{-2}	1.63×10^{-4}	.609	7769.5	20.6	.641
1095	156.8	2.93	3.16×10^{-2}	3.00×10^{-4}	.637	7741.5	20.0	.740

TABLE II.2.1-13

Composition of Fuel Vs. Time - Three-Zone Out-In Refueling
 (1 Year Cycle)
 (Kg) in One Core Module

Time (days)	Pu ²³⁹	Pu ²⁴⁰	Pu ²⁴¹	Pu ²⁴²	Np ²³⁹	U ²³⁸
<u>Outer Zone</u>						
0	165.3	72.7	10.3	2.56	0	877.0
21	164.4	72.7	10.3	2.56	.196	875.6
111	161.6	72.9	10.3	2.55	.195	869.5
201	158.9	72.9	10.3	2.55	.193	863.4
291	156.3	73.0	10.3	2.55	.191	857.3
381	154.2	73.0	10.2	2.54	.189	852.8
<u>Middle Zone</u>						
0	154.1	72.7	10.2	2.54	.215	852.8
21	153.6	72.7	10.2	2.54	.229	852.5
111	150.4	72.6	10.2	2.54	.227	845.1
201	147.4	72.5	10.2	2.53	.223	837.8
291	144.6	72.4	10.2	2.53	.219	830.7
381	142.5	72.3	10.2	2.52	.215	825.7
<u>Inner Zone</u>						
0	142.5	72.3	10.2	2.52	.201	825.7
21	142.4	72.3	10.2	2.52	.263	824.7
111	139.0	72.0	10.1	2.52	.260	816.2
201	135.9	71.7	10.1	2.51	.253	807.8
291	133.0	71.4	10.1	2.51	.247	799.8
381	130.2	71.1	10.1	2.50	.240	791.9

Figure II.2.1-35 Radial Power Distribution with 3 Zone Out-In Refueling

Figure II.2.1-36 Radial Power Distribution with Roundelay Refueling

Figure II.2.1-37 Axial Power Distribution

Figure II.2.1-38 K_{eff} vs. Time - Roundelay Refueling

Figure II.2.1-39 K_{eff} vs. Time - 3 - Zone Out-In Refueling

Figure II.2.1-40 Power Distribution in Inner Blanket

Figure II.2.1-41 Power Distribution in Outer Blanket

Figure II.2.1-42 Fraction of Power from Core and Blanket vs. Average Blanket Age

Figure II.2.1-43 Region Averaged Plutonium Concentration in the Blanket

II.2.1.4.2 Breeding Ratio and Doubling Time

The definition of breeding ratio used in this study is

$$BR = 1 + \frac{A_1 - A_2}{A_2} = \frac{A_1}{A_2}$$

Where

A_1 = Mass of fissionable material produced during a cycle by neutron captures in U-238 and Pu-240.

A_2 = Mass of fissionable material (U-235, Pu-239, and Pu-241) destroyed by neutron capture or fission during a cycle.

Calculations using the SIZZLE code and cross section library were made to determine the composition of equilibrium fuel and are discussed in detail in Section II.2.1.4.1. SIZZLE calculations were performed in both the radial and axial directions using an initial fuel composition corresponding to that required with the Roundelay fuel management scheme with a three-year core and blanket life. A radial case (1) with a zero derivative boundary condition was run to simulate the central core module and the inner half of the outer core modules. A radial case (2) with a boundary condition of the flux going to zero at the extrapolated core boundary was also run to simulate the outer half of the outer core modules.

SIZZLE calculates the instantaneous breeding ratio as defined above at each time step in the calculation. It was noted that the instantaneous breeding ratio varied by less than 3% over the three-year core cycle. Since this variation is small, the assumption was made that the breeding ratio integrated over the fuel cycle could be approximated by a time interval weighted average of the instantaneous breeding ratio.

The total breeding ratio determined from radial case (1) is 1.506 and 1.386 for radial case (2). The contribution to the breeding ratio from the axial blanket was determined from the axial SIZZLE calculation and is 0.065. An

additional correction was made for the contribution to plutonium production from the corners of the radial blanket. These are neglected when spatial separability is assumed in the one-dimensional studies. It was assumed that the plutonium production per unit volume in these corners is 35% of the average radial blanket production. The contribution to the breeding ratio from this part of the blanket is 0.054 for case (1) and 0.045 for case (2).

The overall breeding ratio for the central $4/7$ of the reactor is then 1.625 and 1.50 for the outer $3/7$ of the reactor, giving an effective breeding ratio of 1.57 for the whole reactor.

To compare the breeding ratio calculated using different cross sections, a series of radial W-MABI calculations were made. The cases are described in Section II.2.1.2.1. A single reflected core module with only the Pu-239 isotope was considered. The Pu-239 enrichment in the blanket was 1%. The results of these comparisons are shown in Table II.2.1-2. A direct comparison of the breeding ratio calculated by the SIZZLE code and library with the WAPD-F1 library was also made. The breeding ratio calculated for the core and inner radial blanket in the Roundelay refueling study at the 471 day time step was 1.521. A W-MABI calculation was made using the WAPD-F1 library using the identical geometry and compositions corresponding to the 471 day time step and a breeding ratio of 1.472 was obtained. Since the effective multiplication constant using the WAPD-F1 library was 1.5% greater than that with SIZZLE, one would anticipate a breeding ratio of 1.50 for the same effective multiplication constant using WAPD-F1.

The theoretical aspects of doubling time are discussed in Section II.2.1.1.3. As a practical matter, the doubling time can be obtained by examining the material balance of the reactor over a fuel cycle. The material balance for a three-year fuel and blanket cycle are given in Table II.2.1-9. Neglecting the U-235 contained in the depleted uranium of the blanket, the initial inventory of Pu-239 and Pu-241 is 3685.6 kg. After three years, the core and blanket material removed contains 5228.5 kg of Pu-239 and Pu-241. The production rate is then 514.3 kg/yr; and 7.2 years would be needed to double the inventory of fissionable material.

If a 2% loss of fissionable material is assumed during reprocessing, the quantity of reprocessed material at the end of the cycle is 5123.9 kg and the production rate is 479.4 kg/yr, thus giving an 7.7 year doubling time.

If it is further assumed that an additional 1/3 of the in-core inventory must be tied up in reprocessing and fabrication, then the effective initial inventory is 4914.1 kg of Pu-239 and Pu-241 and the doubling times are 9.55 years and 10.25 years, respectively, for the cases of no reprocessing loss and 2% loss.

II.2.1.4.3 Control Requirements

The weak coupling between core modules requires that each module be controlled separately. The in-core monitoring required is discussed in Section II.2.4.6. The reactor control system required to accomplish this separate control is described in Section II.2.4.7.1. For purposes of nuclear design, the control requirements of each core module can be considered independently.

The control system must be able to rapidly adjust the reactivity by small amounts in order to accommodate load changes and short term system transients. The amount of reactivity available for this purpose in a fast-acting regulating rod will be limited to less than one dollar. Approximately 0.1% reactivity will be needed for this purpose.

Sufficient excess reactivity must be provided to accommodate the reactivity changes associated with sodium, fuel, and structural materials in bringing the reactor from 300°F to operating conditions. This excess reactivity will be held down by a slow-moving central control rod. The withdrawal speed of this rod will be sufficiently slow such that it will be impossible to put the reactor on a short period before a scram is initiated.

In addition to the reactivity required for normal operation, sufficient reactivity must be provided for long-term reactivity losses due to fuel burnup over a fuel cycle. As was discussed in Section II.2.1.4.1, approximately 0.9% excess reactivity is needed for a 6-month cycle and 1.85% for a one-year cycle.

Reactivity control requirements for normal operation are summarized in Table II.2.1-14.

In addition to control for normal operation, the control system must provide a reasonable shutdown margin. The reactor should be at least 3% subcritical with the control rods inserted and be subcritical at zero power and 300°F with the most reactive rod stuck at any time during the fuel cycle. The

safety philosophy assumed is that the most reactive rod, which in this case is the central shim-safety rod, becomes stuck in a position where it is holding down the excess reactivity required for burnout; and is not in its full out position.

Replacement of a central spent fuel element by a fresh fuel element during refueling increases the reactivity of the core by 0.3%. Replacement of 1/3 of the core, as would be necessary for a 1 year fuel cycle, increases the multiplication constant by 1.85%. The shutdown margin of 3% Δk_{eff} after refueling should provide an adequate margin of safety for refueling.

The design depends on the rapid response of the fuel Doppler coefficient and the CEX fuel element to absorb rapid insertions of reactivity and not the control system. The response of the safety rods during scram can therefore be relatively slow (in tenths of seconds).

The reference control rod pattern consists of three large rods at the core blanket interface. These would serve as safety rods and be fully withdrawn during normal operation. The results described in Section II.2.1.3.5 indicate that these rods can be made worth about 1% Δk_{eff} each if the volume fraction of B_4C is increased from the 29.4% used in those calculations to 54%, which is the volume fraction of UO_2 in the blanket. The calculations indicate that a central rod having a cross sectional area of 1/4 a fuel bundle and a B_4C volume of 29.4% is worth approximately 1% Δk_{eff} . By using the space allotted to the whole central fuel bundle and using a higher volume fraction of B_4C this central rod can be made worth 4% Δk_{eff} giving a total rod worth of 7% Δk_{eff} if necessary. With the central rod stuck holding down the excess reactivity for fuel burnup the outer three rods can render the reactor 1.2% subcritical at zero power and 300° F.

An alternate control rod pattern was investigated and has several advantages over the reference design. In this alternate pattern, the control rods at the core-blanket interface are moved to in-core locations. Results of Section II.2.1.3.5 show that four rods (per module) having a B_4C volume fraction of 29% and a cross sectional area of 1/4 a fuel bundle hold down

TABLE II.2.1-14

Reactivity Control Requirements in One Core Module

(6 Month Fuel Cycle)

<u>Requirement</u>	<u>Reactivity (Δk_{eff})</u>
Load Following	0.1%
300°F to Operating Temperature	
Na and Structural Coefficient	1.0%
Doppler Coefficient	0.8%
	<hr/> 1.8%
Fuel Burnout	0.9%
Shutdown Margin at Beginning of Cycle	3.0%
	<hr/>
Total Control Requirement	5.8%

0.0294 Δk_{eff} . By increasing the boron volume fraction to 54%, the worth of the rods can be increased to 5%, with the central rod worth slightly less than 2%. This system should adequately fulfill the control requirements; but if additional rod worth is required, the size of the rods can be increased. The alternate pattern has the additional advantage that the rod worth will be relatively insensitive to blanket plutonium concentrations and a smaller fraction of the reactor volume is taken up by control rods, which will be lighter.

II.2.1.4.4 Reactivity Coefficients and Kinetic Characteristics

In Section II.2.1.4.1, two types of fuel management schemes were discussed. However since Roundelay refueling produced more negative sodium reactivity coefficients than the alternative, it was selected as the reference design. For the Roundelay refueling scheme, the characteristics are given in Table II.2.1-15 for the reactor having an average blanket age of about 2-1/2 years. The sodium coefficients for voiding the entire core and the entire reactor are negative under the conditions described above. It is noted that the isothermal sodium temperature coefficients are less than the isothermal Doppler temperature coefficient, and that the maximum local sodium void coefficient is less than the Doppler effect due to a change in temperature from average operating temperature to melting of the fuel. It is further noted that the maximum local sodium temperature coefficient is less than the Doppler coefficient.

The characteristics presented in the summary and discussed above are typical for the central core of the reference design, and do not take into account judicious shuffling of the blanket to obtain more negative sodium coefficients. The coefficients were calculated in one-dimensional reflected cell geometry.

The negative temperature coefficient of the CEX assembly design is an additional quick-acting coefficient which aids the Doppler in counteracting any positive reactivity effects due to the sodium.

As previously discussed in Section II.2.1.2.4, the REPETITIOUS 3 Monte Carlo code analyses the interacting Doppler effect of two resonance materials simultaneously. Pu^{239} and U^{238} are the most important isotopes in the reference design to be examined to this effect. The Monte Carlo calculations were performed at various temperatures for two atom ratios of Pu^{239} and U^{238} (1/9 and 1/4), each under the condition of sodium present and sodium void.

The results presented in Figures II.2.1-44 through II.2.1-46 give the variation with temperature of capture rate in U^{238} and Pu^{239} and fission rate in Pu^{239} for neutrons of energy less than 30 kev. The error flags represent the probable error for about 1000 histories in the Monte Carlo calculations. A new set of 2030 Pu^{239} unresolved resonances were generated from the appropriate probability distribution curves at every 256 histories. Figure II.2.1-47 illustrates the relative change of fission rate in Pu^{239} with temperature for the conditions given above, all normalized to the average reactor fuel temperature (1325°F). These curves readily provide values for the relative change in fission rate from "cold" reactor condition to average full power condition; from average full power condition to fuel melting condition; and the isothermal temperature coefficient for the fission rate -- all for neutrons of energy less than 30 kev.

The reactivity coefficients listed in Table II.2.1-15 due to Doppler effect are obtained by interpolation from the quantities listed in Table II.2.1-16 and from the relative contribution to the neutron fission source from neutrons of energy less than 30 kev.

This latter quantity was obtained from Figure II.2.1-48, which gives the spectrum of the energy differential of the fission source term integrated over the core. Curves are plotted for two conditions of plutonium accretion in the blanket -- both representing "just critical" reactors. The contribution of neutrons with energy less than 30 kev to the fission source in the core is about 18%. Both conditions are within 5% of this value; in fact, it is of interest to note that this same value of 18% is obtained (within 5%) for a Pu^{239} - U^{238} carbide fueled core with the APDA library, as well as

with the WAPD-F1 library, even though the resulting spectra are quite different from the mixed plutonium fueled core for the Roundelay scheme (compare Figures II.2.1-48 and 49). The U^{238} contribution to the fission source is shown by the peak in the spectra above 1 Mev. Further details of spectra were discussed in Section II.2.1.2.1.

As mentioned previously, the compositions of the core and blanket, as well as the power distribution, change over the refueling cycle. Because of these, the Doppler coefficient also varies over the refueling cycle, and also over the reactor lifetime as equilibrium fuel conditions are approached. It is expected that the Doppler coefficients are larger near the end of the cycle since the amount of Pu^{239} in the core is at a minimum and the blanket contribution to the Doppler effect is at a maximum because of the increase in blanket power. In this way, the increase in Doppler effect over a fuel cycle tends to compensate for the sodium coefficients changing (in a positive sense) due to core power flattening and Pu^{239} buildup in the blanket. The more pessimistic values are reported in the summary of reactivity. In interpolating between the values given in Table II.2.1-16, the effective Pu^{239} to U^{238} atom ratio is obtained by increasing the Pu^{239} atom density to include Pu^{241} and increasing the U^{238} atom density to include Pu^{240} . Results obtained in this way are considered pessimistic. The inclusion of Pu^{240} as a separate isotope in the Monte Carlo calculations would allow additional interference with Pu^{239} resonances since the Pu^{240} resonances do not coincide in energy with the U^{238} resonances. It is believed that additional resonances would exert a stronger effect on the interference than the simple addition of slightly more (less than 10%) U^{238} , which is already interfering strongly with Pu^{239} resonances.

II.2.1.4.5 Uncertainties

The uncertainties involved in the various nuclear calculations are discussed under the specific topics. Experiments have not been performed with equilibrium plutonium fuel to measure the quantities calculated. It is therefore not possible to infer the degree of error from experiment. The uncertainties can only be estimated qualitatively by the amount of disagreement between various calculational procedures and libraries.

TABLE II.2.1-15

Reactivity Coefficients and Kinetic Characteristics(Average Blanket Age $\cong$ 2.5 yrs.)

<u>Isothermal Properties</u>	<u>$\delta k/k$</u>
CEX Expansion Coef., /°F	$-.75 \times 10^{-5}$
Structural Expansion Coef., /°F	-1.0×10^{-5}
Doppler Coef., at 1325°F, /°F	$-.69 \times 10^{-5}$
Doppler Coef., at 1325°F, /°F (Na Voided)	$-.37 \times 10^{-5}$
Sodium Coef. (entire Core), /°F	$+.045 \times 10^{-5}$
Sodium Coef. (entire reactor), /°F	$+.038 \times 10^{-5}$
Doppler Effect, 300 $\rightarrow$ 1325°F	$-.0077$
Doppler Effect, 1325 $\rightarrow$ 4000°F	$-.0084$
Doppler Effect, 1325 $\rightarrow$ 4000°F (Na Voided)	$-.0052$
CEX Effect, 600°F temp. rise	$-.0045$
Sodium Voiding Effect (entire core)	$-.0010$
Sodium Voiding Effect (entire reactor)	$-.0015$

Maximum Values (Roundelay)

Local Sodium Coef. /°F	$+.088 \times 10^{-5}$
Partial Voiding Effect	$+.0032$

Reactor Characteristics

Delayed Neutron Fraction, β	$.004$
Prompt Neutron Lifetime, Sec.	$.3 \times 10^{-6}$

TABLE II.2.1-16

Isothermal Doppler Effects

<u>Characteristic</u>	<u>Relative Changes in Fission Rate for Neutrons ≤ 30 kev</u>			
	<u>Na Present</u>		<u>Na Void</u>	
	<u>10%*</u>	<u>20%*</u>	<u>10%*</u>	<u>20%*</u>
Coefficient, /°F (change /°F at 1325°F)	-4.9×10^{-5}	-2.1×10^{-5}	-3.8×10^{-5}	-1.2×10^{-5}
Coefficient, /°C (change /°C at 991.6°K)	-8.9×10^{-5}	-3.8×10^{-5}	-6.8×10^{-5}	-2.1×10^{-5}
Change: 300°F → 1325°F	-0.055	-0.025	-0.045	-0.021
Change: 1325°F → 4000°F	-0.062	-0.023	-0.057	-0.013

NOTE:

- 1) Percentage represents Pu²³⁹ content in Pu²³⁹ + U²³⁸
- 2) Probable error of Monte Carlo calculations cause about $\pm 10\%$ error in the reported values.

Figure II.2.1-44 Doppler Effect on U^{238} Capture Rate

Figure II.2.1-45 Doppler Effect on Pu^{239} Capture Rate

Figure II.2.1-46 Doppler Effect on Pu^{239} Fission Rate

Figure II.2.1-47 Doppler Effect on Relative Pu^{239} Fission Rate

Figure II.2.1-48 Spectra of Energy Differential of Fission Source Term - Mixed Plutonium and Uranium Carbide Fuel

Figure II.2.1-49 Spectra of Energy Differential of Fission Source Term - Pu^{239} and U^{238} Carbide Fuel

REFERENCES

1. S. Yiftah, D. Okrent, P. A. Moldauer, Fast Reactor Cross Sections, Pergamon Press, 1960.
2. C. B. Mills, "Neutron Cross Sections for Fast and Intermediate Nuclear Reactors," LAMS-2255 (1959).
3. Gordon E. Hansen and William A. Roach, "Six and Sixteen Group Cross Sections for Fast and Intermediate Critical Assemblies," LAMS-2543 (1961).
4. H. Hummel and A. Rago, "An Accurate Treatment of Resonance Scattering in Light Elements in Fast Reactors," Physics of Fast and Intermediate Reactors 1 (1962) 231.
5. M. G. Bhide and H. Hummel, "Calculations of the Doppler Coefficient of Large Ceramic - Fueled Fast Reactors," ANL-6601 (1962).
6. W. G. Davey, "K Calculations for 22 ZPR-III Fast Reactor Assemblies Using ANL Cross-Section Set 635," ANL-6570 (1962).
7. T. P. Moorhead, "Errors in Cross Section Data," Physics of Fast and Intermediate Reactors, IEAE, 2 (1962) III.
8. A. B. Smith, "A Critical Summary of Microscopic Fast Neutron Interactions with Reactor Structural, Fissile and Fertile Materials." Physics of Fast and Intermediate Reactors, 1 (1962) 29.
9. D. Butler, S. Cox, J. Meadows, J. Roberts, A. Smith and J. Whalen, "The Number of Prompt Neutrons from U^{235} Fission over the Range 0.04 Mev to 3 Mev." Physics of Fast and Intermediate Reactors, 1 (1962) 125.
10. B. C. Diven and J. C. Hopkins, "Numbers of Prompt Neutrons per Fission for U^{233} , U^{235} , Pu^{239} and Cf^{252} , Physics of Fast and Intermediate Reactors, 1 (1962) 149.

11. C. Cook, W. O. Davey, "Some Calculations on Zeus Core 2 and 4A Using the 17-Group Fast Reactor Cross Sections of Yiftah, Okrent, Moldauer," AEEW-M158 (1961).
12. J. M. Stevenson and A. R. Baker, "A Comparison of Calculated and Experimental Cross-Section Ratios at the Center of Zephyr." AEEW-M184 (1961).
13. W. G. Davey, "A Comparison of Experimental and Calculated Prompt Neutron Lifetimes and Central Reactivity Coefficients in ZPR-III Assemblies to other Reactor Parameters," ANL-6682 (1963).
14. J. C. Hopkins and B. C. Diven, "Measurements of α for U^{233} , U^{235} and Pu^{239} for Monoenergetic Neutrons," Physics of Fast and Intermediate Reactors, 1 (1962) 110.
15. H. P. Flatt, D. C. Baller, "The AIM-6 Code, NAA Program Description" NAA internal memo (January 1961).
16. W. R. Cadwell, "PDQ-3, A Program for the Solution of the Neutron-Diffusion Equations in Two Dimensions," WAPD-TM-179 (1960).
17. F. J. Leitz, ed., "Sodium Cooled Reactors Program, Fast Ceramic Reactor Development Program, 4th Quarterly Report, July-September, 1962," GEAP-4080 (1962) Section 6.
18. J. K. Long, A. R. Baker, W. Gemmell, W. P. Keeney, R. L. McVean, F. W. Thalgott, "Experimental Results on Large Dilute Fast Critical Systems with Metallic and Ceramic Fuels," Physics of Fast and Intermediate Reactors, IEAF, 1 (1962) 271.
19. D. P. Satkus, H. P. Flatt, "SIZZLE, NAA Program Description," NAA Internal Memo, February 1961.

20. J. K. Long, W. B. Loewenstein, C. E. Brangan, G. S. Brunson, F. S. Kirn, D. Okrent, R. E. Rice, F. W. Thalgott, "Fast Neutron Power Reactor Studies with ZPR-III," Proc. 2nd UN Int. Conf. PUAE, 12 (1958) 119.
21. H. P. Flatt, "PERT, A Perturbation Theory Code, NAA Program Description," NAA Internal Memo, February 1961.
22. ANL, "Reactor Physics Constants," ANL-5800 2nd Edition (1963).
23. S. A. Cox, "Delayed Neutron Studies from Thermal-Neutron-Induced Fission of Pu^{241} ," Phys. Rev. 123 (1961) 1735.
24. D. Okrent, "Performance of Large Fast Power Reactors Including Effects of Higher Isotopes, Recycling and Fission Products," Physics of Fast and Intermediate Reactors, IEAE, 1 (1962) 271.
25. R. D. Richtmyer, R. Van Norton, A. Wolfe, "Monte Carlo Calculations of Resonance Capture in Reactor Lattices," Proc. 2nd UN Int. Conf. PUAE, 16 (1958) 180.
26. W. H. Arnold, Jr. and R. A. Dannels, "A Monte Carlo Study of the Doppler Effect in UO_2 Fuel," WCAP-1572 (1960).
27. K. W. Morton, "The Calculation of Resonance Escape Probability by Monte Carlo Methods," Proc. 2nd U.N. Int. Conf. PUAE, 16 (1958).
28. J. E. Olhoeft, "The Doppler Effect for a Non-Uniform Temperature Distribution in Reactor Fuel Elements," WCAP-2048 (1962).
29. J. Codd, P. J. Collins, Karlsruhe Symposium, April 1963; "The Influence of Resonance Overlapping on the Doppler Effect in a Dilute Fast Reactor," Conference on Breeding, Economics and Safety in Large Fast Power Reactors, ANL, October 1963.

30. L. Dresner, Resonance Absorption in Nuclear Reactors, Pergammon Press, 1960.
31. J. Chernick, R. Vernon, "Some Refinements in the Calculation of Resonance Integrals," Nucl. Sci. and Eng., 4: (1958) 649.
32. F. T. Adler, G. W. Hinman and L. W. Nordheim, "Effective Resonance Integrals; A Systematic Evaluation for all Temperature and Geometries of Practical Interest," Proc. 2nd U.N. Int. Conf. PUAE, 16 (1958).
33. D. J. Hughes, B. A. Magurno, M. K. Brussel, "Neutron Cross Sections" BNL-325, 2nd Ed., Supplement 1 (1960).
34. P. Greebler and E. Goldman, "Doppler Calculations for Large Fast Ceramic Reactors," "Effects of Improved Methods and Recent Cross-Section Information," GEAP-4092 (1962).
35. G. D. Collins, W. J. Ozeroff, "Supplemental Progress Letter #9, Fast Ceramic Reactor Development Program (Conference with Dr. J. Codd)," GEAP, August 1963.
36. W. J. Dollard, L. E. Strawbridge, "Fuel Management in Large Pressurized Water Reactors," Conf. on Nuclear Performance of Power Reactor Cores, San Francisco, September 1963.

II.2.2 THERMAL AND HYDRAULIC ANALYSIS

II.2.2.1 General

The thermal and hydraulic analysis for the 1000 MW Fast Breeder reactor will be discussed in the following order:

1. Overall Design Basis
2. Selection of Core Average Temperature Rise and Flow Rate
3. Core Analysis
4. Radial Blanket Analysis
5. Summary of Reactor Characteristics

In this discussion, the term "core" refers to the areas containing the mixed plutonium-uranium carbide. The term "reactor" refers to the core plus the blanket regions.

The surface heat transfer coefficient is calculated using the Dwyer-Tu(1,2) heat transfer correlation for triangular-pitched rod arrays. An uncertainty factor of 1.5 was applied to the correlation. The Dwyer-Tu correlation is:

$$Nu = 0.93 + 10.81 \left(\frac{P}{D}\right) - 2.01 \left(\frac{P}{D}\right)^{2.0} + 0.0252 \left(\frac{P}{D}\right)^{0.27} (\bar{\psi} P_e)^{0.8}$$

where:

P	Rod pitch
D	Rod diameter
P _e	Peclet number
N _u	Nusslet number
$\bar{\psi}$	Dwyer's eddy transport correction

The thermodynamic and transport properties of sodium used in the thermal and hydraulic analysis were taken from the liquid metals survey(1,3) conducted by the Southwest Research Institute.

II.2.2.2 Overall Design Basis

Thermal design of the 1000 MW Fast Breeder Reactor is based on a total heat output of 2500 megawatts. This is divided between the core and the radial blanket, with 2170 megawatts being released in the core, and 330 megawatts in the radial blanket at equilibrium fuel cycle.

A reactor mixed mean outlet temperature of 1200°F was given by the Atomic Energy Commission as one of the design objectives for this study.

The thermal and hydraulic limitations imposed on the reactor design are as follows:

1. A maximum steady state (100% power) hotspot surface temperature for the clad (type 316L stainless steel) of 1400°F in the core.
2. The core fuel pellets are thermally bonded to the clad with sodium. The equivalent heat transfer coefficient at the fuel-clad interface is $72,000 \text{ Btu/hr-ft}^2 -^{\circ}\text{F}$.
3. The fuel surface temperature at 100% power is limited to 1500°F .
4. The bypass flow (between fuel assemblies, control rod cooling, etc.) is 10% of the total flow.
5. The primary system pressure loss is limited to the available head from a single-stage impeller pump.

II.2.2.3 Core Average Temperature Rise and Flow Rate

The average temperature rise across the core and the core flow rate are functions of each other. The maximum flow rate is a function of the primary system pressure loss, which is limited to the available head from a single-stage impeller pump. The average temperature rise across the core is given by the total heat release and the flow rate, yet must satisfy the clad hotspot surface limitation of 1400°F in the core.

Three other factors which also affect the clad hotspot temperature and the primary system head loss are:

1. Overall hot channel factors
2. Maximum linear power output, Kw/ft, of the fuel rod
3. Core orificing requirements.

As the above functions are mutually dependent, the core average temperature rise and flow rate is defined from a performance analysis between the core and the primary loop. The limitations for the primary system head loss as a function of core sodium volume fraction and pump head are superimposed on the analysis to define the core operating range. The core average temperature rise and flow rate for a mixed mean outlet temperature of 1200°F are 221°F and 128×10^6 lbs/hr, respectively. The results of this analysis are given in Figure II.2.2-1.

II.2.2.4 Core Design

II.2.2.4.1 Design Basis

Thermal design of each core module is based on a total heat output of 310.0 megawatts. This is divided between the fuel rods and the cermet rods, with 306.5 megawatts being released from the fuel rods and 3.5 megawatts from the cermet rods. The coolant inlet and outlet (not including bypass flow) average temperatures are 979°F and 1225°F , respectively. The coolant flow (equilibrium core) to each core module is 15×10^6 lbs/hr.

The thermal and hydraulic limitations imposed on the core modules are the same as those imposed on the overall core design, Section II.2.2.2.

II.2.2.4.2 Hot Channel Factors

The nuclear hot channel factors are given in Nuclear Design Section of this report. The engineering hot channel factors are based upon attaining

Figure II.2.2-1 Primary Loop Performance Analysis

expected results outlined in the proposed research and development programs. For this study, the heat flux (F_q^E) and the enthalpy rise ($F_{\Delta H}^E$) engineering factors are 1.04 and 1.15, respectively.

II.2.2.4.3 Flow Orificing

The cooling requirements for the seven core modules change continuously with time due to the increasing contribution of the radial blanket to the total reactor thermal power. As the division of power changes, flow control orifices will adjust the flow to maintain a mixed mean outlet temperature of 1200°F. The flow control orifices are manually operated to give a mixed mean outlet temperature of 1200°F without exceeding the core temperature limits.

The coolant flow to each core module is orificed in favor of the hot fuel assembly by use of regional fixed orifices. The regions are defined by the radial power density profile and correspond to the three rings of fuel assemblies in each core module. As the inner-most ring has six fuel assemblies, compared to 18 fuel assemblies in the outer-most ring, a 21% flow increase in the inner region can be achieved by a 7% flow reduction in the outer region. Based upon the core radial power distribution as given in Nuclear Design Section of this report, this amount of orificing is considered realistic and achievable.

The average coolant velocity through the core module is 26.5 ft/second. The coolant axial temperature profiles in the average fuel assembly and the hot channel are shown in Figures II.2.2-2 and II.2.2-3, respectively.

II.2.2.4.4 Fuel Rod Linear Power Output

The average and maximum linear power outputs in the core modules are 12.0 and 29.1 kilowatts per foot, respectively. The maximum linear power output was selected to satisfy the maximum temperature limit of 1400°F for the clad surface. The sensitivity of the maximum clad temperature to the maximum linear power is small compared to the effect of the hot channel coolant temperature rise. This is the result of the high heat transfer coefficients.

Figure II.2.2-2 Core Module Axial Profile of Coolant, Clad and Fuel Temperatures in Average Channel; 100% Power and Equilibrium Core

Figure II.2.2-3 Core Module Axial Profile of Coolant, Clad and Fuel Temperature in Hot Channel; 100% Power and Equilibrium Core

II.2.2.4.5 Clad Temperature

The 1000 MW Fast Breeder core is clad-temperature limited. The maximum clad surface temperature, 1400°F , occurs at the approximately 90% position along the fuel rod measured from the inlet end. The hotspot position is higher than normally occurs in other types of reactors because the high heat transfer coefficient for sodium results in a coolant film temperature drop that is small compared to the coolant temperature rise per unit of core height.

The axial clad surface temperature profiles in the average fuel assembly and the hot channel are given in Figures II.2.2-2 and II.2.2-3, respectively.

II.2.2.4.6 Fuel Temperatures

The fuel is mixed plutonium-uranium carbide with a melting point of approximately 4200°F . A thermal conductivity of 11.0 Btu/hr-ft. $^{\circ}\text{F}$, was used to calculate the temperature gradient in the fuel pellet.

The axial temperature profiles for the fuel surface and center-line in the average fuel assembly, and the hot channel are shown in Figures II.2.2-2 and II.2.2-3, respectively. The maximum fuel center line and surface temperatures are 2184°F and 1476°F , respectively. The fuel's hotspot surface temperature is 140 degrees below the boiling point of sodium at atmospheric pressure.

II.2.2.4.7 Cermet Temperatures

The volumetric heat generation rate in the cermet rods is 20% of the fuel pellets' volumetric heat generation rate and is based on the results of the transient analysis, Section II.2.4. The cermet's axial center line temperature profiles in the average and hot fuel assemblies are shown in Figure II.2.2-4. The maximum center line temperature is 1450°F .

Figure II.2.2-4 Cermet Rod Axial Profile of Centerline Temperatures in Average and Hot Assemblies; $q_{\text{cermet}} = 0.2q_{\text{fuel}}$

II.2.2.4.8 Pressure Drop

The total pressure drop across the core modules, including the axial blankets, is 59.5 psi. The distribution of the pressure drop through the fuel assembly is as follows:

1. Inlet region	17.3 psi
2. Fuel Bundle	35.9 psi
3. Outlet Region	6.3 psi

II.2.2.5 Radial Blanket Design

II.2.2.5.1 Design Basis

Thermal design of the radial blanket is based on a total heat output of 330 megawatts. The coolant inlet and outlet average temperatures (including bypass flow) are 979°F and 1200°F, respectively. The coolant flow rate is 23×10^6 lbs/hr.

The thermal and hydraulic limitations imposed on the radial blanket region are:

1. Due to the power density increase with blanket resident time and the steep power density gradient near the core-blanket and blanket-graphite interfaces, the maximum hotspot surface temperature of the clad, at end of life, will not exceed 1400°F.
2. The maximum fuel center temperature is limited to the melting point, 4900°F, of the UO_2 .
3. The coefficient of heat transfer at the clad-fuel interface (gas bond) is 1000 Btu/hr-ft²-°F. A sodium bond is not specified as the fission gases are retained in the fuel rods for the radial blanket.

II.2.2.5.2 Hot Channel Factors

Due to the steep gradient of power density, the nuclear hot channel factors for the radial blanket are greater than for the core and are given in the Nuclear Design Section. The engineering hot channel factors are also greater because the effects of clad diameter tolerances, pitch, bowing, etc., are much greater in a tightly-packed fuel bundle. For the radial blanket, the heat flux, F_q^E , and enthalpy rise, $F_{\Delta H}^E$, engineering factors are 1.04 and 1.33, respectively. The enthalpy rise factor is higher in the radial blanket than in the core due to the more tightly packed rod bundle.

II.2.2.5.3 Flow Orificing

The cooling requirements for the radial blanket change continuously with blanket residence time due to increasing power density. To satisfy the changing cooling requirements, the flow control orifices will increase the blanket total coolant flow in keeping with the core-blanket power split. The mixed mean outlet temperature from each core region is employed to program manual operation of flow control orifices, and maintain the proper temperature balance between core and blanket regions.

The blanket coolant flow is then orificed regionally, according to power density distribution in the radial blanket. The blanket assemblies between the core modules have a higher power density buildup than the blanket assemblies on the outside of the reactor. Also, the blanket assemblies next to the core modules have an average power density buildup 30% greater than the next row of assemblies. Thus, to satisfy the wide variation of power density in the radial blanket, the coolant flow will be orificed regionally, according to whether or not the assembly is located: 1) between or on the outside of the core modules; or 2) is adjacent to the core module or the graphite barrier.

The average coolant velocity in the radial blanket is 6.2 ft/sec, based on mean blanket power density in an equilibrium core. The outlet temperature from the hot channel is 1396°F. The power density gradient

across the radial blanket produces a hot channel at the core-blanket and the blanket-graphite interfaces. The above hot channel outlet temperature occurs near the end of the blanket assembly resident time. Coolant axial temperature profiles in the average and hot channels are given in Figures II.2.2-5 and II.2.2-6, respectively.

II.2.2.5.4 Clad Temperature

The clad temperature in the radial blanket is only a few degrees higher than the coolant temperature because the heat flux is low and the heat transfer coefficient is high. The maximum clad temperature of 1400°F occurs at the outlet of the fuel rods in two locations: the core-blanket; and the blanket-graphite interfaces. The axial profiles of the clad surface temperature in the average and hot channels are shown in Figures II.2.2-5 and II.2.2-6, respectively.

II.2.2.5.5 Fuel Temperatures

The radial blanket fuel is depleted uranium-oxide in pressed and sintered pellets. A thermal conductivity of 1.6 Btu/hr-ft-°F is used in calculating the fuel pellet center temperature.

The fuel center temperature reaches a maximum of 2519°F in the hot channel. The axial profiles of the fuel center temperature are shown in Figures II.2.2-5 and II.2.2-6, respectively.

II.2.2.5.6 Pressure Drop

The total pressure drop across the radial blanket is 37.5 psi. Distribution of pressure drop through the blanket assembly is as follows:

1. Inlet region	3.1 psi
2. Fuel Bundle	32.9 psi
3. Outlet region	1.5 psi

Figure II.2.2-5 Radial Blanket Axial Profile of Coolant, Clad and Fuel Temperatures in Average Channel; Equilibrium Core

Figure II.2.2-6 Radial Blanket Axial Profile of Coolant, Clad and Fuel Temperatures in Hot Channel; Equilibrium Core

The head loss for the inlet flow orifices is not included in the inlet region pressure loss.

II.2.2.6 Fast Breeder Reactor Core Characteristics

The thermal and hydraulic characteristics of the 1000 MW Fast Breeder Reactor are summarized in the following table.

TABLE II.2.2-1

SUMMARY OF FBR CORE CHARACTERISTICS

A. REACTOR

1. Rating

Total thermal power, MW (t)	2500
a. Core modules	2170
b. Blanket	330
Total Flow, 10^6 lbs/hr	128
Bypass Flow, %	10

2. Temperatures, °F

Core inlet	979
Core average rise	221
Core mixed mean outlet	1200

B. CORE MODULES

1. Rating

Total thermal power, MW (t)	2170
Number of modules	7
Module total thermal power, MW (t)	310
a. Fuel rods	306.5
b. Cermet rods	3.5
Power density, Kw/liter (core)	308
Weight of Fuel, Kg (carbide)	25,200
Specific Power, Kw/Kg contained fuel (Pu,U)C	86.2

SUMMARY OF FBR CORE CHARACTERISTICS (Cont.)

Average linear power, Kw/ft	12
Maximum linear power (100% power), Kw/ft	29.1
Total flow rate (7 modules), 10^6 lbs/hr	105
Module Flow Rate 10^6 lb/hr	15.0
Composition, Volume %	
a. Sodium	55.1
b. Fuel	29.4
c. Steel	15.5
2. <u>Temperatures, °F</u>	
Module inlet	979
Module average rise	221
Module average channel rise (10% bypass)	246
Module mixed mean outlet	1200
Module average channel outlet	1225
Module hot channel outlet	1372
Maximum clad (outside)	1400
Maximum fuel center line (100% power)	2184
Maximum cermet surface (100% power)	1325
Maximum cermet center line (100% power)	1450
3. <u>Hot Channel Factors</u>	
Nuclear	
a. Radial, F_R^N	1.39
b. Peaking	1.14
c. Axial, F_A^N	1.45
Engineering	
a. Flux, F_q^E	1.04
b. Enthalpy, $F_{\Delta H}^E$	1.15
c. Surface Temperature, F_{θ}^E	2.0
F_q^T	2.42
4. <u>Fuel Assembly</u>	
Shape of fuel assembly	hexagonal
Number of fuel assemblies per module	36

SUMMARY OF FBR CORE CHARACTERISTICS (Cont.)

Rods per fuel assembly	
a. Fuel (upper and lower)	120
b. Cermet	7
Fuel rod array	triangle
Fuel material	(Pu-U)C
Clad material	316L SS
Active fuel length, inch	72
Fuel pellet diameter, inch	0.268
Type of fuel clad bond	sodium
Clad, inch	
a. I.D.	0.280
b. Thickness	0.010
c. O.D.	0.300
Fuel rod pitch, inch	0.426
Fuel rod pitch/diameter ratio	1.42
Cermet diameter, inch	0.300
Cermet active length, inch	74.95
Total heat transfer area per module	2030
Average heat flux, Btu/hr-ft ²	521,000
Maximum heat flux, Btu/hr-ft ²	1,261,000
Total flow area per module, ft ²	2.94
Average velocity, ft/sec	26.5
Nominal velocity in hot assembly, ft/sec	31.8
Average mass velocity, 10 ⁶ lbs/hr ft ²	4.88
Nominal mass velocity in hot assembly, 10 ⁶ lbs/hr ft ²	5.86
5. <u>Axial Blanket</u>	
Rods per assembly	120
Clad, inch	
a. I.D.	0.280
b. Thickness	0.010
c. O.D.	0.300
Fuel Material	UC (depleted)
Blanket thickness (upper and lower), inch	12

SUMMARY OF FBR CORE CHARACTERISTICS (Cont.)

C. RADIAL BLANKET

1. Rating

Total thermal power (equilibrium), MW (t)	330
Total flow rate, 10^6 lbs/hr	23
Composition, %	
a. Sodium	25.8
b. Fuel	54.5
c. Steel	19.6
d. Void Space	0.1

2. Temperatures, °F

Blanket inlet	979
Blanket average rise	221
Blanket average channel rise (10% bypass)	246
Blanket average channel outlet	1225
Blanket hot channel outlet (end of life)	1396
Maximum clad (outside)	1400
Maximum fuel center line	2519

3. Hot Channel Factors

Nuclear

a. Radial, F_R^N	1.9
b. Axial, F_A^N	1.50

Engineering

a. Flux, F_q^E	1.04
b. Enthalpy, $F_{\Delta H}^E$	1.33
c. Heat Transfer, F_{θ}^F	2.43
F_q^T	3.1

4. Fuel Assembly

Shape of fuel assembly	hexagonal
Number of fuel assemblies	357
Rods per fuel assembly	91
Fuel rod array	triangle
Fuel material	UO ₂ (depleted)

SUMMARY OF FBR CORE CHARACTERISTICS (Cont.)

Clad material	316L SS
Active blanket length, inch	84
Fuel pellet diameter, inch	0.420
Type of fuel-clad bond	gas
Clad, inch	
a. I.D.	0.424
b. Thickness	0.020
c. O.D.	0.464
Fuel rod pitch, inch	0.496
Fuel rod pitch/diameter ratio	1.07
Total heat transfer area, ft	27,650
Average heat flux, Btu/hr-ft ²	41,700
Average velocity, ft/sec	6.2
Average mass velocity, 10 ⁶ lbs/hr-ft ²	1.12

II.2.3 Mechanical Design

II.2.3.1 General Design Considerations

The design goal of the mechanical system is a core which incorporates physics, thermal, hydraulic, and material requirements into an optimum synthesis. The mechanical design should not introduce limitations of its own on such factors as power density, heat flux, and center temperature, and the mechanical arrangements should be inherently safe and reasonably accessible for service. Promising designs based mainly upon theoretical cycle and system components have proved to be mechanically unachievable. Consequently, a comparatively more detailed set of drawings of reactor mechanical internals was prepared than is usual for a conceptual design study. As the design concept was developed, certain developmental requirements were identified and are discussed subsequently. In general, all mechanical components and design requirements appear to be feasible or achievable within the proposed development program. No detail or requirement has been uncovered that would seem to contain a disabling difficulty.

REACTOR MECHANICAL PARAMETERS

Fuel Rod O.D.	0.300 in	
Fuel Rod Clad Thickness	0.010 in	
Fuel Pellet Diameter	0.268 in	
Fuel Rods per Assembly	120	
Cermet Rods per Assembly	7	
Area of Fuel Assembly (0.060" Spring Gap Included)	23.09 in ²	0.160 ft ²
Fuel Assembly Channel (Can) Wall Thickness	0.093 in	
Fuel Assemblies Per Module & Total	36	252
Module (Enriched Core) Area	5.935 ft ²	
Module Equivalent Diameter	2.748 ft	

S. S. in Clad, Channel, Ferrules and End Plugs (averaged)	3.12 in ²	13.5%
S. S. in Cermet @ 10% PuC	0.45 in ²	
S. S. Total of Above	3.57 in ²	15.5%
Effective Area PuC (inc. Cermet Rods)	6.72 in ²	29.4%
Na in Assembly		55.1%
Fuel Rods in 7 Enriched Cores Total	30,240	
Cermet Rods in 7 Enriched Cores Total	1764	
Total Rods in 7 Enriched Cores Total	32,004	
Wt. of Fuel Assy. (incl. Channel)	350	
Module Perimeter Reflectors (Canned Graphite)	174 cans	
Wt. of PuC Fuel Pellets (90% Dense)	1.8 lb/rod	216 lb/120 rods
PuC Inventory @ 90% dense (incl. Cermet Rods)	55,000 lb	25,000 kg
Blanket, 91-Rod Assemblies	357	
UO ₂ Pellet Diameter	0.420 in	
Rod Diameter	0.464 in	
Clad Thickness	0.020 in	
UO ₂ Area	0.1386 in ² /rod	12.6 in ² /assy
% UO ₂ Area	54.5%	
% Steel	19.6%	
Coolant Flow Area	5 in ²	21.7%
Total Sodium Area	25.8%	
Pellet radial Gap Area	0.1%	
Wt. of 87" UO ₂ Loading, 90% dense	388 lb/assy	
Wt. of UO ₂ in 357 Blanket Assemblies	139,000 lb	63,200 kg
Wt. of UC in 12" Axial Blanket	35,200 lb	
Control Rods, 61 tubes, 6' long B ₄ C (Natural)	54 lb/assy	

II.2.3.2 General Reactor Description

Figure II.2.3-1 is a schematic drawing of the reactor core. Identical-sized hexagonal cells are arranged in a hexagonal pattern of seven groups, each group called a "module". Each module consists of core assemblies, breeder blanket control rods, and perimeter moderator-reflector assemblies.

Figure II.2.3-1 Core Schematic

The fuel and blanket assemblies will be replaced on the usual refueling schedules. The perimeter moderator-reflector assemblies are permanent and not ordinarily replaced.

Figure II.2.3-2 shows the reactor general assembly. Sodium coolant enters the vessel through the outer pipes of the two concentric external pipe connections. The flow fills the annular space between the core barrel and vessel and is divided into 12 downcomers, provided by sectioning the annular space to guide the flow inlet to the core. There is one downcomer section for each outer module; two for the center module; and the remaining spaces provide flow to the breeder blanket sections of the module. A simple flow regulating gate is provided in each core downcomer to provide for flow balancing between enriched cores and to divert flow from the cores to the blanket as blanket breeding (and consequently heat generation) builds up.

The coolant flow turns upward at the vessel bottom and flows up through the core support structure (all of the core assemblies are shown standing vertically out of the core support structure). The flow proceeds upward through the core, thence out of the reactor through the central pipe of the two concentric pipe connections. Baffling installed below the core contains the flow from each downcomer to its designated core module or the general blanket assemblies.

Each assembly (fuel or blanket), or control rod guideway, is socketed into the core support structure. Above the core, there is an open grid on top of the permanent module perimeter reflector assemblies (Figure II.2.3-2, quarter view at elev. C). This grid, through which each module perimeter reflector assembly is pinned, fixes each module group position. Because this is an open grid, the interior core and breeder blanket assemblies are not held down or directly located by this grid, but are socketed into the lower core support and held down only by connection therein. The assemblies are vertical columns in simple tension or compression, depending upon flow (load conditions).

A vertical reactor with each fuel assembly inserted as a column into a bottom core support plate has certain inherent qualities. Free-standing parallel fuel assemblies eliminate interactions and bending due to differential thermal ambients and due to tolerance buildup between arrays of interlocking mechanical elements.

Control rod drive lines penetrate from above the reactor head through the core to their final location within the lower core plate. The drive line is fully protected by a full channel in and above the core. The drive connection rod is encased to below the reactor head closure, with sealed drive assemblies above the core. The half plan view shows the control rod drives above the top plug, four for each module.

II.2.3.3 Mechanical Design of Reference Plant Fuel Bundle

II.2.3.3.1 Description of Fuel Rod Assembly

The fuel rod assembly (Figure II.2.3-3) consists of three main sub-assemblies: the 7-rod central "cermet" sub-assembly (Part 1); identical upper (Part 2) and lower (Part 3) halves of 120-rod fuel sub-assemblies. The central 7-rod cermet sub-assembly extends through the entire length, from top to bottom rod support grids, and holds the two halves of the fuel sub-assemblies together. The fuel sub-assembly top and bottom halves separated in the center of the assembly to provide for "CEX" (a negative transient temperature feedback control feature described elsewhere).

Each half of the 120-rod fuel sub-assembly (Figures II.2.3-4 and II.2.3-5) consists of an array of fuel-filled rods spaced by round ferrules and brazed into a single stable structure. Forty-eight of the fuel rod end plugs, on the outer rods and under the three spokes, extend into the end grid and are fastened therein. This is most easily accomplished by plug-welding from the end after the basic cluster is completely assembled and tested. The fuel rod clusters are made in processes similar to those employed for Yankee Cores 1, 2 and 3. The cluster consists of an array of fuel rods and spacer ferrules brazed into a unit structure. When installed, the inner rods that are not directly supported from the end grids are supported by the brazed

Figure II.2.3-2 Reactor General Assembly

Figure II.2.3-3 Fuel Assembly

Figure II.2.3-4 Fuel Bundle Assembly

Figure II.2.3-5 Fuel Assembly Cross Section

ferrule system.

The fuel rod sub-assembly (Figure II.2.3-3) is suspended from the top grid (Part 4) which is attached to the side walls of the flow channel. The top of this assembly incorporates a heavy rigid fitting that provides coolant flow exit and a fuel handling grip. The fuel assembly hangs straight down in the flow channel (Part 5). At zero or partial flow, the weight of the fuel assembly hangs upon the cluster of seven central cermet rods. At full flow, the hydraulic drag forces are greater than the weight of the assembly.

The lower end (Part 6) of the fuel assembly channel is shown inserted into its orificed position in the core support structure. The core plate (Part 7) is shown about 13-1/3 inches thick with the fuel assembly inserted and latched into it. The fuel assembly is located by its position in the core plate and held securely by latching therein. No top-of-core hold-down or positioning is shown or provided.

The assembly is inserted into the round hole in the core plate by compressing the spring-loaded (Part 8) fuel can extension collar (Part 9) until the latch action at the $4\frac{1}{2}$ inch core penetration is accomplished.

II.2.3.3.2 Fuel Assembly Hold Down Detailed Operation

Three shouldered segments (Figure II.2.3-3, Part 10) hang from flat cantilever members (Part 11) at the end of the fuel can extension (Part 12). When the bottom tips of the cantilevered segments enter the 3.98 inch diameter hole in the core plate, they are bent radially towards the center to permit entry. The assembly proceeds downward until the latch position is reached, where clearance is provided so that the cantilevered segments can snap out to their neutral, non-stressed position. A ledge on each of the three segments enters a provided circumferential relief. This ledge-to-shoulder interference provides the hold-in latch. The Haynes-25 $\frac{1}{2}$ inch diameter wire spring (Part 8) on the fuel can extension (Part 12) is in a 500-lb loaded condition in the latched position. This exceeds the weight of the fuel assembly by approximately 150 lb. It is this spring force and

the upward hydraulic forces that keep the latch faces loaded at all times. The fuel can extension collar (Part 9) seats squarely on the core plate to produce a securely aligned base on which the fuel can column (Part 5) depends for position and rigidity.

The fuel assembly is removed as follows:

The fuel handling machine pushes straight down on the top of the fuel can fitting (Part 4). This will push the fuel assembly (at Part 12) further into its orifice against the upward force of the spring (Part 8). The unlatching requires about $1\frac{1}{2}$ inch travel and about a 300 lb downward force (about 150 lb spring pre-load, after allowing for the assembly weight, and 100 lb per inch for the travel) and 40 lb for latch friction. Note that a holding ring (Part 6) rests on three steps that extend from the wall of the core plate orifice. As the latch segments (Part 10) move down, the holding ring (Part 6), through whose center the three cantilevered segments slide freely, cannot move down. The three latch segments sliding through the holding ring are thus forced to deflect radially inward when the inclined faces of the thicker portion of the latch segments are forced through the holding ring. The spring load from the deflected cantilevers (Part 11) snugs the holding ring on the thick section of the latch segments. The latching ledge on the latch segment is now inside the 3.98-inch diameter of the hole in the core plate. Because the holding ring is also smaller than this diameter, the fuel assembly can now be lifted straight up and out of its core position.

The fuel assembly can now be transported to spent fuel storage or reinstalled in another core position. If the assembly is to be used again, the holding ring must be recocked by pushing it down off the thick section of the latch segment to its normal free clearance position, as shown in the core installed condition.

In its normal installed position, the cantilever has no bending stresses, and hangs straight down --- forced neither in or out. The load is straight tension from the net coil spring force (about 100 lb) and from the 300-lb upward hydraulic forces, causing a tensile stress of 1000 psi in each

cantilever or about 3000 psi if all the load is carried in one member. The bending stress, which occurs only during fuel assembly insertion and removal, is about 33,000 psi. Approximately four flexures are expected during a fuel assembly lifetime, when the ambient temperature is about 300°F.

There are a number of reasons for the selection of a basic triangular pitch array instead of a square pitch array for the fuel rods for this core. Several are noted below.

1. Larger pitch-over-diameter ratios are obtained with a triangular pattern than with a square pattern. This provides more space between rods for bowing and fabrication tolerances, and between rods and channel wall before causing coolant flow inhomogeneities with hot and cold spots. Because in both the fuel and breeder core sections the spacing is relatively close, this difference is significant.
2. Heat transfer and pressure drop for a given coolant area fraction are more favorable.
3. A stronger flow channel can be made using less structural steel in the hexagonal shape, enclosing triangular pitch rods, than a square channel around square pattern rod arrays. Each can is designed for full core pressure drop to minimize can wall deflection or collapse against the rods contained therein.
4. A triangular array, with round ferrule spacing between rods (as in the core) or spiral wire-wrapped (as in the breeder blanket), is mechanically the most stable of possible arrays if the perimeter of the assembly is constrained.
5. Many sodium cooled reactor studies and designs have used triangular rod patterns.

Some reasons for selecting a brazed ferrule fuel assembly rather than a spring contacting grid are noted below.

1. The core pressure drop is smaller with a low form-loss round ferrule than with the spring finger grid designs available. A larger core pressure drop has many disadvantages. Local coolant mixing due to high pressure drop grids is not important as sodium heat transfer is mainly by conduction.
2. At 1400°F surface temperature, the spring force from a friction contact grid is uncertain.
3. Leaf-type contacts in sodium may produce welds or fretting due to lack of oxide on the hot surfaces.

II.2.3.3.3 Fuel Rod Design

The fuel rods (Figure II.2.3-6) are half-core lengths of about 51 inches spaced to provide for the "CEX" (controlled expansion) operation. The rods are vented to the coolant. Consequently, there is a sodium bond between the fuel pellet and clad.

Two 18-inch columns of fuel (PuC) pellets occupy the center of core chambers of each half rod. At each end (top and bottom) of the half rods is a 12-inch column of depleted UC which acts as the axial breeder blanket. The chambers are separated by hollow tube discs, with $\frac{1}{2}$ -inch axial pellet expansion space provided in each chamber. Tube discs are locked to the tube wall as shown in the drawing and by brazing. The braze is made at the same time the fuel assembly spacer ferrules are brazed. This process is similar to that used for Yankee Reactor Cores 1, 2 and 3.

The tube wall shown is 0.01-inch thick 316L stainless steel. The mass transfer of nickel in the clad is discussed in detail in the Materials Evaluation Section. In this design, the clad provides for mechanical location of fuel pellet columns in a stable core array. Because of the vented rod design, there is no internal hoop stress on the clad due to fission gas release.

Figure II.2.3-6 Fuel Rod

Fission gas released from the fuel is expected to evolve upward through the sodium filled tube, through the hollow tube disc locks, thence out of the fuel rod through the vent hole in the top end plug. One vent is provided in each half rod through the top end plug in each half. There is, therefore, no coolant flow through the fuel rod due to either pressure or thermal gradients.

The fuel pellet is pressed and sintered to 90% of theoretical density to a nominal size of 0.268-inch diameter. The following allowances are provided in pellet-to-clad clearance. Pellet swelling at 2% $\Delta V/V$ per atom % burnup is equal to approximately 20% for 100,000 MWD/ton burnup. One-half of the 20% $\Delta V/V$, is absorbed into the pellet porosity; therefore, 10% $\Delta V/V$ clearance is provided for swelling. An additional allowance of ± 0.0025 -inch is made for diameter deviation of pressed and sintered unground pellets. There is also a ± 0.0005 -inch allowance for the inner diameter of the tube. No additional clearance is needed for differential pellet-to-clad thermal expansion since they are of equal magnitude.

Each fuel rod is comprised of three compartments separated by tube disc locks, installed progressively as each fuel rod is loaded. They are mechanically locked in position by rolling the tube wall into the circumferential groove in each disc lock. The disc lock is plated with a braze material, so that when the assembly is furnace brazed, the disc locks are brazed at the same time, thereby re-enforcing the mechanical crimp. No insulation disc is needed between these discs and pellet stack as there is internal sodium bonding to the 1400°F (peak) wall and the relatively cool (2000°F) pellet center temperature.

II.2.3.4 Blanket Assembly

The breeder blanket cross section, shown in Figure II.2.3-7 is installed in a flow channel of identical dimensions as the fuel assembly described in Section II.2.3. The upper and lower fittings are identical with those in the fuel assembly; therefore, the description of installation and hold-down is the same.

Figure II.2.3-7 Section - 91 Rod Breeder Blanket Assy.

Figure II.2.3-8 Control Rod Element - 61 Rod Element

The 91-rod blanket assembly in Figure II.2.3-7 consists of type 316L SS tubing containing depleted UO_2 pressed and sintered pellets. The blanket rods are full core length (102 inches). The tubing contains 87 inches of UO_2 pellets in the center and with empty chambers at each end. The tubing is dry-gas filled and hermetically sealed. The radial space between pellet and clad, and particularly the end chambers, are used for fission gas storage.

A 0.420-inch diameter pellet and 0.020-inch thick clad provides 54.5% area for breeder material, 19.6% SS, and 25.8% sodium coolant flow area. The balance (0.1%) is gas gap and stagnant sodium. This wall thickness is intended to contain all fission gas released in the void space provided. This void space has approximately 50% excess volume above the required amount. Assumptions made to achieve this condition are: 30% fission gas release from 0.022 Pu production fissioned (.017 average Pu production fissioned x 1.3 peaking factor).

The weight of 87 inches of UO_2 pellets per rod is 4.27 lb for 10.8 gm/cc 90% dense material. The total inventory in the 357 radial breeder blanket assemblies is about 139,000 lb.

While no specific rod bundle assembly technique is shown in Figure II.2.3-7, the 0.032-inch spacing of the rods is on a regular triangle pitch pattern. This indicates that a brazed wire-wrap assembly is practical, using welding or the same braze material as in the enriched core for assembly purposes.

The blanket rod design using hermetically sealed rods contrasts with the vented fuel rod design. This design provides satisfactory heat transfer, evolution of gas, and mechanical and manufacturing practicability. Therefore, there is little incentive in making the breeder rods thinner walled and vented, as are the rods in the core.

II.2.3.5 Control Rod Assembly

The control rod element shown in Figure II.2.3-8 consists of an array of

61 tubes occupying one full-sized flow channel in the core. The control channel consists of a heavy walled hexagonal tube 0.093 inches thick and approximately two core lengths long, resting on the lower core plate. These channels may be located within the core or in the blanket, as all channels are the same size.

The absorber bearing tubes (Figure II.2.3-8) are segmented, with a horizontal grid at about one foot intervals making a unit assembly of the tube array. The top grid from which all rods hang is provided with a vertical connecting member (not shown in Figure II.2.3-8) to the control rod drive coupling, shown at right side of Figure II.2.3-9. The radial spacer grids permit differential linear expansion of the individual rods to eliminate any bowing which might otherwise develop due to flux gradients around the control rod perimeter. They also provide running surfaces against the control rod gridway can.

The type 316L SS tubes are 0.570-inches in diameter with a 0.025-inch thick clad. Each segment terminates in an end plug drilled for gas passage to the adjacent tube segment. A porous sintered steel screen covering each through-hole is provided. Each tube segment (except the top and bottom segments) is filled with natural B₄C powder. A 61-rod array 6 feet long contains about 54 lb of absorber at 64% density. The connected tubing permits helium gas evolution from the B¹⁰ absorptions to proceed downward through the tube and out of the vent through the bottom end cap. The empty tube segment at the bottom of each rod element ensures that sodium fluid in the reactor can not rise within the tube to reach the B₄C contained in the adjacent tube segment. When fully inserted, the empty tube segment is in the axial blanket section of the core.

II.2.3.6 Control Rod Drive Line

The control rod drive line is about 43' - 10" long from core plate to top of the control rod drive housing. Figure II.2.3-9 shows all elevations. The control rod is shown within its guide channel, which extends from the core support plate up to twice the core height (79.2 inches) to permit full rod travel within it. The drive connecting rod continues up through

a guide tube (one of the group of four at Section A-A) until it enters the flared junction below the reactor top plug (Figure II.2.3-9). Here, it connects to the control rod drive shaft within the control rod drive housing. The control drive line elements are enclosed and are shielded from all coolant flow impingements.

The control rod guide channel is fixed relative to the core by insertion in the core support plate. The top end is mated to the round guide tube as shown in detail "B". This guide tube merely rests on the guide channel so that the spring-loaded fitting adjusts for vertical length differences. The top end of each guide tube is socketed within the flared junction below the top plug, thus forcing the guide tube snugly down on its spring seat on top of the guide channel. Four of the guide tubes are interconnected to make stable groups over each core module as shown in Section A-A and also in Figure II.2.3-2. In addition to providing stability for each module group, the interconnected guide tube groups can more readily be handled and removed during refueling operations.

When the reactor plug is off and the drive rod is removed as will be described in Section II.2.3.6.2, the guide tube cluster can be removed from the vessel by lifting it straight up. The guide clusters are replaced by setting it back down into the top of the control rod and guide channel (detail "B"). The drive rods are then inserted in each guideway and coupled to the control rod (2.3.6.2). The reactor top plug contains flared fittings which accept and fix the top end of the guide tubes. The top plug also presses each tube down against its spring loaded connection (Figure 9, Detail B).

II.2.3.6.1 Control Rod Drive

There are 28 control rods and drives in the reactor design, consisting of 7 groups of 4 rods each (Figure II.2.3-10). Because each module is virtually a separate core, each must be capable of individual startup, dynamic control, shim, and shutdown; thus drive is mechanically independent.

SCALE 1" = 1 FOOT

Figure II.2.3-9 Control Rod Drive Line (Elevations)

The drive, shown schematically in Figure II.2.3-10, is housed in a long, thin tube mounted in the top of the reactor core plug. This housing completely seals the drive unit and plug penetration. There is a connection above the drive mechanism for an inert purge gas inlet, which vents to the reactor top plenum thereby keeping the entire drive housing essentially free of sodium deposition.

The drive is essentially a pinion-and-rack drive. A coupling connects the drive rod below to the rack extension above. The rack is spur-gear driven by a drive motor through a reduction unit and electromagnetic clutch. The design shown for 1.2 in/min speed is typical of the requirements for about a 500 lb lift force. This will accommodate the drive line weight and will provide about 1%Δk per hour withdrawal rate of the safety rods.

This drive system is typical of the design philosophy used in the Westinghouse CVTR control rod drives which employed the type of stepping motor, reduction unit, electromagnetic clutch, and pinion and rack projected for this proposed application. The drive line in this reactor, however, is simpler and more direct than in the CVTR, thereby affording confidence in the feasibility of this general approach.

A 72 RPM, 30 in-oz synchronous stepping motor provides a countable withdrawal rate for position indication (A typical motor of this type, "Slo-Syn" is made by Superior Electric Company, Bristol, Connecticut.). The right angle 360:1 reduction unit drives the electromagnetic clutch which is energized to lift and support the drive rack. De-energizing the clutch either deliberately or due to power failure, permits the weight of the drive line to fall, spinning the small pinion shaft (Electro-magnetic

clutches of this type are made by Stearns Electric, Milwaukee. Type GSWA Size 504 is a typical "run dry", metallic lined device pictured for this application.). The rack is shown with a smooth idler opposite the drive pinion. All shaft ends and openings into the drive mechanism are sealed behind gasketed cover plates.

II.2.3.6.2 Control Rod Drive Line Couplings

Couplings between segments in the drive line are one of the necessary requirements in this reactor concept. The basic requirement is for secure connections that will not open accidentally, yet permit easy blind disassembly for refueling and other services by normal motions and provisions on the fuel handling machinery. There are two couplings in each drive line: one between the rod drive rack extension and the drive rod (Figure II.2.3-10); the other between the drive rod and control rod (Figure II.2.3-9).

The coupling between the drive rack and the rod drive (Figure II.2.3-10) is operated by the control rod drive system and is shown in the "zero" withdrawal or down position. A segmented collet gripper from the rack above engages a toothed fitting on the shaft below, similar to the fuel assembly hold-down system described in Section II.2.3.3.2. The collet gripper is backed up by the spring-loaded collet lock which, in the normal engaged position as shown, keeps the collet gripper in tight contact with the toothed drive rod end. The slit in the cantilevered gripper permits a tight no-back-lash grip.

Decoupling and recoupling are accomplished as follows. To decouple, the control rod drive is driven down below its "zero" position. At -3 inches travel, the collet lock stops against a ledge in the drive line guideway. An additional 1.5-inch downward travel to -4.5 inches slides the collet lock back against its spring loading. The three-segment collet grippers can now bend outward on their flat cantilever spring suspension. When this 4.5-inch decoupling motion is completed and verified on all 28 drives, the reactor top plug can be lifted and removed. As the top plug is lifted, it carries

Figure II.2.3-10 Control Rod Drive & Coupling Mechanism

the drive and drive line guideways with it. The drive rod slides out of the collet gripper, as the 300-lb, drive-line weight easily deflects the flat cantilever spring outward to release the drive rod.

While the reactor top plug is off, the relative positions of drive and collet gripper parts remain stationary in their $4\frac{1}{2}$ -inch "open" position. When the top plug is replaced on the reactor, all grippers are in the "open" position to accept recoupling as soon as the top plug is lowered sufficiently. The three-segment collet grippers over each drive rod are deflected radially outward by the entry of the drive rod toothed end. The control rod drive is now driven upward $4\frac{1}{2}$ inches to its "zero" position. After the first $1\frac{1}{2}$ inches of upward travel, the collet lock has been driven down by its spring and the collet gripper is again locked closed, securely, thus completing the coupling operation.

Drive Rod to Control Rod Coupling

As can be seen in Figure II.2.3-2, after the drive is decoupled from the lower drive line and the reactor top plug is raised, a guide tube and drive rod remains standing on top of the control rod guideways extending up, out of the core. To refuel or service the core, the drive-line components can be removed as follows. When the reactor top plug is off and the control rod drive is decoupled, as described above, the top end of each drive rod (Figure II.2.3-10) is visible above the sodium level. A control rod coupling operator is concentric with the drive rod. An accessible lift collar is pinned to the coupling operator shaft which is spring-loaded to a normal down position. Hoisting the lift collar straight up pulls the coupling operator up against the spring load and against the weight of the control rod.

Figure II.2.3-9, Enlarged View "B", shows the coupling between the drive rod and the control rod in its normal coupled condition. This coupling is similar in principle to the one above it connecting to the rod drive. The flexible flat cantilever-hung, inside-grip collet is shown mated to the control rod. The grip collet is blocked from opening by the coupling

operator. When the coupling operator, shown in Figure II.2.3-10, is pulled up about $2\frac{1}{2}$ inches, the collet releases its grip. The weight of the control rod causes it to slip out of the collet grip as its cantilever suspension is flexible when the coupling operator is removed from behind it. The drive rod continues to move up and out as it is carried by the lift collar shown in Figure II.2.3-10.

The drive rod is replaced simply by being carried by the lift collar and lowered through the guide tube. The collet deflects to enter the coupling because of its flexibility, and the lift collar is moved down, aided by its spring. This pushes the coupling operator down, locking the collet and thereby completing the coupling. The slit in the coupling operator provides for spring-loading the collet mating parts for a tight fit.

While couplings are necessary in any drive line, and those for a sodium fast reactor have special reliability and blind operation requirements, these appear straightforward enough to provide good assurance of being practical and trouble free in operation.

II.2.3.7 Module Perimeter Reflector

Module perimeter reflector blocks can be seen in Figures II.2.3-1, II.2.3-2, and II.2.3-11. Consistent with the geometrical patterns in the core, the reflector blocks are hexagonal steel cans containing graphite. Each can extends the full height of the core and penetrates both the lower grid and the top module perimeter locating grid. The top of each canned structure consists of a plate with a projection for penetrating the top locating grid for maintaining top spacing.

These reflector columns surround each core and blanket group, thereby substantially cutting flux coupling between them and making each of the seven groups virtually independent cores or modules. These graphite reflector cans are permanent core fixtures. In case of failure of the cans, as evidenced by carbon in the sodium, it would be necessary to shut down the reactor and replace the failed containers. To do this, the failed can must be located and the top locating grid above it must be lifted off.

Figure II.2.3-11 Module Perimeter

Figure II.2.3-12 Core Support Grid

II.2.3.8 Module Perimeter - Top Locating Grid

The module perimeter top locating grid is shown in Figures II.2.3-2 and II.2.3-11. Each graphite column is set into the bottom support grid and also into the top grid, thus providing a fixed geometrical perimeter for each module group. Because the fuel and breeder assemblies do not have top grid locating connections, the module perimeter provides a limit on the radial displacement these assemblies may take. There are eleven cans across each module. With a nominal 0.005 inch hot gap between the stellite contact slides at the top of each can corner, the most a can top may move is 0.055 inches. This amount may be further reduced by a resilient wedging provision between can tops.

The top locating grid is shown as a heavy beam web running across the flats of the permanent graphite reflector cans and mounted on lugs or pads on the thermal shield wall. Each can has a projection from its center axis which extends into the grid. The sides and top of this gridwork may be utilized to carry instrumentation lines over or into the cores.

II.2.3.9 Reactor Core Support Grid

This grid (Figure II.2.3-12) is a cored stainless steel casting which because of its size must be made in two or three segments. It is about 14 feet in diameter, 13 inches thick, and weighs 50,000 lb. There are 14 inches of sodium between the top of the support grid and the bottom of the axial blanket and an additional 12 inches to the core. The neutron flux (greater than 1 Mev) at the top of the grid of about $2 \times 10^{20}/\text{cm}^2$ per year which is low enough on stainless steel for safety against embrittlement during the design life of the plant (e.g., $10^{22}/\text{cm}^2$ integrated exposure would take approximately 50 years.).

The plate thickness was obtained using equations from R. J. Roark's "Formulas for Stress and Strain", 3rd ed., page 194.

$$S_{\max} = \frac{3 W}{8 \pi m t^2} (3m + 1)$$

$$D_{\max} = \frac{3W (m-1) (5m + 1) a^2}{16 \pi E m^2 t^3}$$

W = Total Load

s = Unit Stress

D = Deflection

m = $\frac{1}{\text{Poisson's Ratio}}$

t = Thickness

a = Radius

E = Young's Modulus

The stress and deflection was corrected for ligament efficiency using W. J. O'Donnell and B. F. Langer ASME paper, "Design of Perforated Plates", 61 - WA - 115, 1961.

The maximum values, for the down-loaded plate at no flow is 3090 psi stress, deflection 0.25 inch. The maximum for up-loading at full flow is 2030 psi stress and 0.165 in. deflection. These values are reasonable for sizing purposes and are probably higher than actual, as no credit was taken for stiffening and support due to inlet connection pedestals below the core plate.

II.2.4 Transient Analysis, Safety, and Reactor Control Considerations

II.2.4.1 Introduction

Although fast breeder reactors (EBR-I, EBR-II, Dounreay, BR-5, Enrico Fermi) have been constructed and operated during the past ten years, there remain several basic questions which have not been fully examined, but which are important to safe power reactor design and operation. The two major problems of reactor dynamics affecting reactor safety are:

1. Reactor stability
2. Reactor transient response.

Dynamic stability of a reactor at all power levels of normal operation ensures that the fuel temperature and system pressure are properly bounded during normal operation; that is, that no tendency exists toward the spontaneous and rapid buildup of power oscillations (and of associated fuel temperature and system pressure oscillation) to excessive peak values. Analysis of transient response during accidental situations involves:

- a. Determining to what extent, following a rapid insertion of reactivity, the reactor power, fuel temperature, and system pressure are self-limited as a result of internal changes in the physical configuration or state of reactor components.
- b. Determining the maximum allowable amount and rate of insertion for non-destructive self-limitation of power, temperature and pressure.
- c. Ensuring that the system is so designed that no credible accident could exceed these maximum conditions.

The inherent stability and safety characteristics of fast and thermal reactors differ because of variances in physical core characteristics and in the magnitude of the reactivity coefficients. Most thermal reactors have comparatively large temperature coefficients which are negative, hence tend to stabilize the kinetic behavior. Thermal reactors are also strongly affected by fission product poisoning, so that 10-15% excess reactivity at the beginning of life must be available to

overcome this effect. Fast reactors have smaller temperature coefficients and a much smaller poison effect. Some of the characteristics of a fast reactor which affect inherent stability and safety are:

1. The prompt neutron lifetime is extremely short, being several orders of magnitude smaller than for thermal reactors. Thus the period at prompt criticality for a fast reactor is in the millisecond range.
2. The ratio of the heat capacity to the thermal power of the core is low.
3. The sodium coefficient for large cores may become positive. This is caused by the variations with size of the two principal opposing components of the sodium coefficient, spectral hardening and leakage.
4. The Doppler coefficient may be small.

The mechanism relied upon for stability and safety in counteracting a reactivity insertion in a fast reactor varies with the type of fuel. Reactors using metallic or cermet fuel rods depend primarily upon fuel expansion which can be accurately predicted. Ceramic fueled reactors normally depend upon the Doppler effect as the primary shut-down mechanism in response to an uncontrolled reactivity insertion; the fuel expansion is unpredictable and does not exert a significant effect in some designs.

To investigate the inherent core stability, the transient response to reactivity changes, and safety with respect to reactivity accidents for the 1000 MWe Fast Breeder Reactor, an analog computer was used to simulate the core. The thermal and hydraulic characteristics of the reactor are given in Section II.2.2. The fuel considered is mixed plutonium-uranium carbide and the coolant is sodium. Sodium is used to provide a thermal bond between the fuel pellet and the stainless steel clad.

The controlled expansion (CEX) fuel assembly was selected for the 1000 MWe FBR study. In the CEX assembly, cermet rods are substituted for a few of the carbide fuel rods. The cermet rods provide a ceramic fueled core with

a strong negative mechanical expansion temperature coefficient in addition to the Doppler effect. The mechanical design of the fuel assembly is described in Section II.2.3. The study neglects the beneficial effects of the coupling between modules, and treats a module as behaving in the same manner as an isolated core.

II.2.4.2 Nuclear and Thermal Parameters

The range of values used to study the effect of each reactivity coefficient on the inherent stability is given in Table II.2.4-1.

TABLE II.2.4-1

Reactivity Coefficients

	<u>Max. Value</u>	<u>Nominal Value</u>	<u>Worst Value</u>
1. Doppler	$-4.0 \times 10^{-6}/^{\circ}\text{F}$	$-1.0 \times 10^{-6}/^{\circ}\text{F}$	$+2.0 \times 10^{-6}/^{\circ}\text{F}$
2. Sodium	$-5.0 \times 10^{-6}/^{\circ}\text{F}$	$+0.5 \times 10^{-6}/^{\circ}\text{F}$	$+5.0 \times 10^{-6}/^{\circ}\text{F}$
3. Cermet Expansion	$-10.5 \times 10^{-6}/^{\circ}\text{F}$	$-7.5 \times 10^{-6}/^{\circ}\text{F}$	$-3.0 \times 10^{-6}/^{\circ}\text{F}$
4. Clad Expansion	-----	$+2.99 \times 10^{-6}/^{\circ}\text{F}$	-----

The neutron lifetime, ℓ , is assumed to be 1×10^{-7} seconds and β_{eff} is 0.004.

The nominal cermet rod diameter is 0.300-inches, which is the same size as the fuel rods. To study the effect of cermet rod size, the number of cermet rods in each fuel assembly was decreased to three and the diameter was increased to 0.48-inches.

The nominal volumetric heat generation rate (q_c''') in the cermet is taken as 10% of the fuel pellet value. This heat generation rate was then increased in steps to 40%. From this investigation, a heat generation rate was selected for the cermet rod.

To study the effect of inlet temperature on stability, a step increase of 120°F in sodium temperature with several different time lags was used.

This temperature increase is equivalent to losing half of the secondary sodium loop flow.

Maximum temperatures reached in the fuel and cermet rods were investigated for a power scram, with the power scram controller set point taken at 125% of full power. Time delay for the control rods to be fully inserted in the core is 1-1/2 seconds, including control rod mechanism release time.

For the power scram analysis, the limiting temperatures for the hotspot locations in the fuel, clad and cermet were assumed to be the following:

1. Fuel center	4200°F (melting point)
2. Fuel surface	1600°F
3. Cermet center	1700°F
4. Clad	1500°F

The overall hot channel factors used to calculate the hotspot temperature in the power scram analysis are given as:

Heat generation, F_q	=	2.4
Enthalpy, $F_{\Delta H}$	=	2.0
Surface Temperature	=	4.0

The above values are conservative.

II.2.4.3 Analytical Procedures

The inherent stability and safety of the conceptual 1000 MWe FBR core were investigated by an analog study. For the analog computer, a mathematical model was derived to represent the nuclear and thermal characteristics of the core. The model was then transposed into a simulation for the analog computer. The simulation was scaled for two different ratios of computer time to real time. For the first time scale (which will be referred to as the "real time model"), the ratio was 1.0. This ratio allows the inherent stability and transient

response to be investigated for step insertions of reactivity of less than one dollar and for time periods of 12 seconds and longer in real time.

The second time scale (which will be referred to as "initial time model") ratio was 10^4 (i.e., one second on the analog represented 100 microseconds in real time). The purpose of the initial time model was to investigate the initial power burst and core behavior and safety for step insertions of reactivity greater than one dollar.

The mathematical model is divided into six major sections for the analog simulation. These sections are as follows:

1. Neutron Kinetics
2. Fuel pellet heat transfer
3. Clad heat transfer
4. Cermet rod heat transfer
5. Induced stresses in cermet rod
6. Reactivity feedback

The schematic of the analog simulation is shown in Figure II.2.4-1.

The neutron kinetic section for the real time model used a three-group approximation for the delayed neutrons. For the initial time model, a logarithmic simulation was employed as the initial power burst rose to several orders of magnitude above normal power. In the logarithmic simulation, only the fastest acting delayed neutron group was included as the influence of the other delayed neutron groups is effective only at relatively much later times.

The fuel pellet and the cermet rod heat transfer characteristics were described in space and time by the use of Hankel finite transforms. In this way, the temperature at any point can be developed in a series of exponential terms multiplying a forcing function. The series converges very rapidly, hence three terms are sufficient for the simulation. The

Figure II.2.4-1 Schematic of Analog Computer Model

forcing functions for the fuel pellet are the thermal power and the clad inner surface temperature derivative with time. For the cermet rod, the power and the coolant temperature derivative with time are the forcing functions. The average, maximum, and surface temperatures can be found separately by employing similar circuits in the simulation. Application of the finite transform method for analog simulation of the heat transfer characteristics of the fuel pellet and the cermet rod is given in reference 1.

The clad heat transfer characteristics for the real time model are described by finite transforms. In the initial time model, the clad heat transfer characteristics are represented by a cylinder divided into three equal thicknesses. Because of the high inside heat transfer coefficient resulting from the sodium bond between the fuel pellet and clad, this approach yielded a better model in terms of computing equipment requirements.

The stresses induced in the cermet rod during the initial temperature rise and before the top half of the fuel bundle is accelerated were described by the finite transform method. Once the fuel bundle begins to move by expansion, the induced stresses are relieved. The induced stress section is incorporated in only the initial time model because these stresses would be negligible (due to relaxation) in the real time model. The mathematical model and the analog simulation for the induced stresses are discussed in reference 2.

The reactivity simulation section utilized the following four feedbacks:

1. Doppler
2. Cermet Expansion
3. Clad Expansion
4. Sodium Average Temperature

In the CEX fuel assembly, clad expansion is a positive feedback in the reactivity. Expansion of the cermet rod moves the two halves of the fuel assembly apart. As the clad average temperature increases, the clad

Figure II.2.4-2 Effect of Doppler Coefficient with CEX Fuel Assembly: Step Reactivity Insertion, $\Delta k = 40$ cents

Figure II.2.4-2 Effect of Doppler Coefficient with CEX Fuel Assembly: Step Reactivity Insertion, $\Delta k = .40$ cents

Figure II.2.4-2 Effect of Doppler Coefficient with CEX Fuel Assembly: Step Reactivity Insertion, $\Delta k = 40$ cents

expansion moves the two fuel bundle halves toward the center of the core. The clad expansion therefore inserts reactivity, whereas the cermet expansion removes reactivity. No credit was taken for any fuel expansion counteracting the clad effect (i.e., the fuel was assumed to follow the clad).

Inherent stability and transient response is investigated on the real time model for a range of values for each reactivity coefficient with a step insertion for the reactivity addition. A step insertion is considered the worst case for reactivity addition. The steady state power level is 100% power. A step insertion is used in this study as the forcing function because it is considered to be the worst case for the addition of reactivity. Several combinations of the most adverse reactivity coefficient are investigated to obtain the most severe stability behavior.

II.2.4.4 Results

For clarity, the results of the analog computer study for the core inherent stability and transient response are presented according to the reactivity coefficients. Results for the initial time model which studied step insertion of reactivity greater than one dollar are discussed separately in the last section.

II.2.4.4.1 Doppler Coefficient Effects

The Doppler coefficient, $\left(\frac{\Delta k}{T}\right)_{\text{Dop}}$, is varied from 0 to $-4.0 \times 10^6 / ^\circ\text{F}$. The remaining reactivity coefficients are set at their nominal values. The volumetric heat generation rate in the cermet rod is 10% of the fuel rod. Coolant inlet temperature is held constant.

The analog results for the power rise, $\frac{q}{q_0}^*$, and the temperature rise, T^* , for the fuel, clad cermet and coolant, plotted versus time after the step reactivity insertion, are shown in Figures II.2.4-2a thru II.2.4-2h. Reactivity insertion is 40 cents. As the Doppler coefficient

Note: Throughout this discussion $\left(\frac{\Delta k}{T}\right)_{\text{Dop}} = \left.\frac{\partial k}{\partial T}\right|_T)_{\text{Dop}}$

becomes more negative, the magnitude of the power and the temperature rises decreases.

The axial expansion of the cermet rod removes reactivity from the core. The amount of reactivity so removed can be expressed as an equivalent increase in the Doppler coefficient. From the cermet average temperature rise and the expansion reactivity coefficient, the reactivity feedback, Δk_{cermet} , as a function of time can be determined. Similarly, the reactivity feedback Δk_{Dop} , for Doppler is given. The ratio of the cermet's reactivity feedback to the sum of the cermet plus Doppler reactivity feedbacks, $\frac{\Delta k_{\text{cermet}}}{\Delta k_{\text{cermet}} + \Delta k_{\text{Dop}}}$, is defined as the effectiveness ratio of the cermet rod and is shown in Figure II.2.4-3. As the Doppler coefficient becomes more negative, the cermet effectiveness ratio decreases due to the increased influence of the fuel temperature rise.

The rate at which the cermet rod reaches its final effectiveness is shown in Figure II.2.4-4 for -1.0 , -2.0 , and $-4.0 \times 10^6/^{\circ}\text{F}$ Doppler coefficients. In each case, the cermet rod obtains 95% of its full effectiveness in approximately 2.0 to 3.5 seconds after the step insertion. An analytical investigation at 10% heat generation rate and assuming that no heat conduction occurs from the cermet and fuel, gave a cermet rod's effectiveness ratio of 0.37. At one-half second after the insertion, Figure II.2.4-4 shows the cermet rods obtain, depending on Doppler, 67 to 85% of their full effectiveness. The increase in cermet rod effectiveness with time results from the lower thermal conductivity of the cermet with respect to the fuel. Although the fuel is enclosed in a clad, the overall heat transfer coefficient between the point of average fuel temperature and the coolant is greater than the cermet's overall heat transfer coefficient.

The combined reactivity feedback from Doppler and the cermet rod is expressed in terms of an equivalent Doppler coefficient,

$$\left(\frac{\Delta k}{T} \right)_{\text{eq. Dop.}} = \frac{\left[\left(\frac{\Delta k}{T} \right)_c T^*_c + \left(\frac{\Delta k}{T} \right)_{\text{Dop}} T^*_f \right]}{T^*_f}$$

Figure II.2.4-3 Effectiveness Ratio for Cermet Rods in CEX Fuel Assembly as a Function of Doppler Coefficient

Figure II.2.4-4 Cermet Rod Effectiveness Ratio for Various Doppler Coefficients as a Function of Time After Step Reactivity Insertion

based on fuel temperature rise, and is shown in Figure II.2.4-5. In the equilibrium maximum effectiveness, the cermet expansion reactivity feedback for a 10% heat generation rate is equivalent to a $-1.56 \times 10^{-6}/^{\circ}\text{F}$ Doppler coefficient. At the beginning of the transient, the cermet rod reactivity feedback is equivalent to $-0.57 \times 10^{-6}/^{\circ}\text{F}$ Doppler coefficient. These values increase linearly with the volumetric heat generation rate as will be seen in Section II.2.4.3.1

The hot spot temperatures reached at various locations in the fuel, cermet, and coolant outlet for the Doppler coefficients investigated during a power scram after a 40 cent step insertion of reactivity are shown in Figure II.2.4-6. All of the temperatures reached are below the limiting values given in Section II.2.4.3. The scram controller set point was taken at 125% of full power and the scram delay time for full insertion of the safety rods is 1-1/2 seconds.

II.2.4.4.2 Sodium Coefficient Effects

The effect of the sodium coefficient, $\frac{\Delta k}{T})_{\text{Na}}$, on the inherent stability and transient response was investigated for a range of values from $+5.0 \times 10^{-6}/^{\circ}\text{F}$ to $-5.0 \times 10^{-6}/^{\circ}\text{F}$. The nominal value was taken as $+0.5 \times 10^{-6}/^{\circ}\text{F}$. The other reactivity coefficients and the cermet's heat generation rate were set at their nominal values.

To study the worst combination of coefficients, the sodium coefficient was set at $+5.0 \times 10^{-6}/^{\circ}\text{F}$ and the Doppler coefficient was then increased to its worst value, $+2.0 \times 10^{-6}/^{\circ}\text{F}$, as seen in Table II.2.4-1. The volumetric heat generation rate in the cermet was increased from 10% to 20% as a parameter of the study.

The results for the various sodium coefficients, for a step insertion of 40 cents in reactivity, indicated only a small effect on the resultant response. The rise in power, $\frac{q}{q_0}^*$, versus time is shown in Figure II.2.4-7. The curves for the fuel and cermet temperatures showed the same trend given by the power rise curve. In Figure II.2.4-7, the

Figure II.2.4-5 Equivalent Doppler Coefficient Using CEX Fuel Assembly Based on Fuel Temperature as a Function of Doppler Coefficient

Figure II.2.4-6 Maximum Hotspot Temperatures During a Power Scram After a Step Reactivity Insertion, Δk , of 40 cents as a Function of Doppler Coefficient

Figure II.2.4-7 Effect of Sodium Coefficient with the CEX Fuel Assembly: Step Reactivity Insertion $\Delta k = 40$ cents

initial power rise is 60%, and the power rises afterwards at an average rate of 18% per second. The spread in the curves is less than 10%. The low sensitivity of the sodium coefficient on the inherent stability results from the cermet's expansion reactivity feedback. For a $\frac{\Delta k}{T})_{Na}$ of $+5.0 \times 10^{-6}/^{\circ}F$, the reactivity added for a 10 degree rise in the average coolant temperature can be offset by a 7 degree rise in the average cermet temperature. Without the cermet rod, a 50 degree rise in the average fuel temperature is required to counteract this reactivity insertion from the coolant.

The power scram analysis for the range of sodium coefficients gave maximum temperatures at the hotspot as follows:

Fuel center	2105°F
Cermet center	1476°F
Fuel surface	1482°F
Clad center	1432°F
Coolant outlet	1386°F

All of the above temperatures are below the limiting values given in Section II.2.4.3.

Increasing the Doppler coefficient to its worst value, $+2.0 \times 10^{-6}/^{\circ}F$ at a 10% cermet heat generation rate did not give any indication of encountering an instability. The reactivity inserted by a step is 20 cents. The insertion was limited by the computer scaling for this case. The cermet's expansion reactivity feedback is sufficient to overcome the reactivity insertion plus the reactivity added by the positive Doppler. The cermet center temperature rise is not considered excessive as shown in Figure II.2.4-8 which gives the hotspot temperatures in the fuel and in the cermets for the power scram analysis. None of the hotspot temperatures shown in Figure II.2.4-8 are above their limiting value.

Increasing the cermet's heat generation rate to 20% for the above case enhanced the cermet's reactivity feedback. The power scram results are

Figure II.2.4-8 Maximum Hotspot Temperatures During a Power Scram
 After a Step Reactivity Insertion, Δk , of 20
 cents as a Function of Sodium Coefficient

given in Figure II.2.4-9. As expected, the cermet's center temperature is higher, but is still below the limiting value of 1700°F.

II.2.4.4.3 Controlled Expansion (CEX) Member

Cermet expansion reactivity feedback is affected by three items: 1) volumetric heat generation rate; 2) the reactivity feedback worth per unit of temperature rise; and 3) the rod diameter. Each of these items were investigated for a range of values and the results are presented separately.

II.2.4.4.3.1 Volumetric Heat Generation Rate

The heat generation rate in the cermet rod is taken as a percentage of the fuel pellet volumetric heat generation rate. The nominal 10% value for the cermet is increased in steps to 40%. The 40% is considered to be an upper limit imposed by the amount of fuel which can be retained in the metal matrix and still have a cermet with the required strength and expansion coefficient.

For this study, the other coefficients were set at their nominal values and the inlet temperature held constant. The step insertion of reactivity was 50 cents.

The results of this study are given in Figures II.2.4-10a thru II.2.4-10h. As shown in Figure II.2.4-10a, the initial power rise, q^*/q_0 , for all heat releases is approximately 100%. For the 10, 20, and 30% heat generation rates, the power rises steadily, changing its rate of rise gradually during the first 3-1/2 seconds; attaining a constant rate thereafter. The rates of power rise for each heat generation rate are given in Table II.2.4-2.

Figure II.2.4-9 Maximum Hotspot Temperatures During a Power Scram After a Step Reactivity Insertion, Δk , of 32 cents as a Function of Sodium Coefficient

CERMET TEMPERATURE RISE (°F)

Figure II.2.4-10 Effect of Volumetric Heat Generation Rate in Cermets Rods of CEX Fuel Assembly: Step Reactivity Insertion, $\Delta k = 50$ cents

Figure II.2.4-10 Effect of Volumetric Heat Generation Rate in Cermet Rods of CEX Fuel Assembly: Step Reactivity Insertion $\Delta k = 50$ cents

Figure II.2.4-10 Effect of Volumetric Heat Generation Rate in Cermet Rods of CEX Fuel Assembly: Step Reactivity Insertion, $\Delta k = 50$ cents

TABLE II.2.4-2

Power Rise Rates for Step Insertion
of Fifty Cents of Reactivity

<u>Cermet's Heat Generation Rate</u>	<u>Initial Jump</u>	<u>First Three Seconds</u>	<u>After Three Seconds</u>
10%	~ 100%	30%/sec.	12-1/2%/sec.
20%	~ 100%	13%/sec.	8%/sec.
30%	~ 100%	8%/sec.	4%/sec.

Increasing the cermet's heat generation rate from 10% to 30% not only limited the total power rise as shown in Figure II.2.4-10a but also significantly reduced the rate of power rise after the initial jump. The largest reduction occurs between the 10 and 20% heat generation rates.

The power curve in Figure II.2.4-10a for the 40% heat generation rate shows an interesting characteristic. After the initial jump, the power decreases and then rises at a constant rate of 3% per second. The power decrease after the initial jump is due to the rapid expansion of the cermet. For this heat generation rate, the cermet expansion removes 5 times as much reactivity as does Doppler.

As would be expected, the cermet's average and center temperatures (Figures II.2.4-10b and II.2.4-10c) increase with increasing heat generation rate. The temperature rises very rapidly during the first two seconds, then levels off at a fairly constant rate of rise ($\sim 6^\circ\text{F}/\text{sec}$). The temperature rise per unit of heat generation decreases with increasing relative heat generation rate, with the largest decrease occurring between the relative heat generation rates of 10 and 20%. The fuel temperatures, however, (Figures II.2.4-10d, II.2.4-10e and II.2.4-10h) show the opposite trend to that for the cermet.

The cermet's reactivity feedback effectiveness as a function of the volumetric heat generation ratio, q'' cermet/ q'' fuel, is shown in

Figure II.2.4-11. The effectiveness ratio is a measure of the contribution to the total reactivity feedback resulting from cermet rod expansion. The cermet's effectiveness increases with heat generation rate and appears to approach a constant value of approximately 87%. At a heat generation rate of 10% in the cermet, feedback effectiveness is 61% increasing to 76% at 20% heat generation rate.

The cermet plus Doppler reactivity feedback expressed in terms of an equivalent Doppler coefficient, based on fuel temperature, is given in Figure II.2.4-12. At a 10% heat generation rate, the cermet reactivity feedback is equivalent to a $-1.56 \times 10^{-6}/^{\circ}\text{F}$ Doppler coefficient. From Figure II.2.4-12, the portion of the equivalent Doppler, $\left[\frac{\Delta k}{T} \right)_{\text{Eq. Dop}} - \frac{\Delta k}{T} \Big)_{\text{Dop}}$, contributed by the cermet is proportional to the heat generation rate.

The results of the power scram analysis for this volumetric heat generation rate study are shown in Figure II.2.4-13. The hotspot center temperature of the cermet is essentially linear with heat generation rate and the limiting value of 1700°F is reached at a heat generation rate of 21.3%. The nominal Doppler coefficient, $-1.0 \times 10^{-6}/^{\circ}\text{F}$, was used for this investigation. As the Doppler coefficient becomes more negative, the heat generation rate which gives the limiting cermet center temperatures will increase as the effectiveness ratio for the cermet decreases with increasing Doppler (see Figure II.2.4-3). The fuel and clad temperatures reached during a power scram are below the limiting values.

II.2.4.4.3.2 Cermet Rod Diameter Effects

The range of cermet rod diameters is limited by the rod size which would occupy the same space as the seven 0.300-inch diameter rods in the fuel assembly design. The other rod diameter studied was 0.48-inches. The reactivity coefficients and the volumetric heat generation rate in the cermet rod were set at their nominal value, and the step insertion of reactivity introduced into the simulation was 40 cents.

Figure II.2.4-11 Effectiveness Ratio for Cermet Rods in CEX Fuel Assembly as a Function of Volumetric Heat Generation Rate in Cermet Rods

Figure II.2.4-12 Equivalent Doppler Coefficient Using CEX Fuel Assembly as a Function of Volumetric Heat Generation Rate in Cermet Rods

Figure II.2.4-13 Maximum Hotspot Temperatures During a Power Scram as a Function of Volumetric Heat Generation Rate in Cermet Rods After a Step Reactivity Insertion, Δk , of 50 cents

Results for the power and the cermet average temperature rise are shown in Figures II.2.4-14a and II.2.4-14b. The initial power jump for both diameters is shown as 60%. For the first 1.8 seconds, the power rise for the larger diameter is seen as slightly greater than for the smaller diameter. After that time, the situation reverses. The rates of increase in power after 2 seconds are 9%/sec. and 5%/sec. for the 0.3 and 0.48-inch diameters, respectively.

The average temperature rise in the 0.48-inch diameter rod is higher than for the 0.3-inch diameter rod by approximately the ratio of the diameters. At a constant volumetric heat generation rate, the surface heat flux is proportional to the rod diameter and the temperature gradient in the rod is proportional to the surface heat flux. Thus, the cermet's average temperature rise is as expected.

The average fuel temperature, for both cermet rod diameters, rises at a rate of 160°F/second for the first second. After the first second, the fuel temperature for the larger cermet rod diameter is approximately 20% lower than for the nominal diameter.

The reactivity feedback effectiveness ratio for the 0.48-inch diameter rod is 0.75 compared to 0.61 for the nominal rod diameter as shown in Figure II.2.4-15. Also shown in Figure II.2.4-15 is the equivalent Doppler coefficient, based on fuel temperature, for the sum of the cermet and the Doppler reactivity feedbacks. The portion of the equivalent Doppler coefficient, due to the cermet is $-3.1 \times 10^{-6}/^{\circ}\text{F}$

$\left[\left(\frac{\Delta k}{T} \right)_{\text{Eq. Dop.}} - \left(\frac{\Delta k}{T} \right)_{\text{Dop.}} \right]$ for the 0.48-inch diameter rod which is twice the value, $-1.56 \times 10^{-6}/^{\circ}\text{F}$ for the nominal diameter.

The rate at which the larger diameter cermet rod obtains its final effectiveness ratio is compared to the nominal diameter in Figure II.2.4-16. The larger rod requires twice the amount of time as does the nominal diameter to obtain 95% of its final effectiveness. This is due to the larger time constant for the larger diameter which is approximately twice that of the nominal rod diameter.

Figure II.2.4-14 Effect of Cermet Rod Diameter in CEX Fuel Assembly: Step Reactivity Insertion, $\Delta k = 40$ cents

Figure II.2.4-15 Equivalent Doppler Coefficient and Cermet Rod Effectiveness Ratio as a Function of Cermet Rod Diameter

'CERMETS' REACTIVITY FEEDBACK RATIO TO
FINAL REACTIVITY FEEDBACK RATIO

Figure II.2.4-16 Cermet Rod Effectiveness Ratio for Various Rod Diameters as a Function of Time After Step Reactivity Insertion

The results of the power scram analysis for the two cermet rod diameters are shown in Figure II.2.4-17. The only significant difference between these two cases is the cermet's center temperature which, for the larger rod, reached a value of 1650°F (50°F below the limiting temperature).

II.2.4.4.3.3 Cermet Expansion Coefficient Effects

The worth of the cermet's expansion in terms of the reactivity coefficient is varied from -3.0 to $-10.5 \times 10^{-6}/^{\circ}\text{F}$. The other coefficients are set at their nominal values as given in Table II.2.4-1. The cermet volumetric heat generation rate is 10% and the coolant inlet temperature is held constant. The reactivity inserted into the simulation is 40 cents and in the form of a step.

The results of this study for the power rise and the cermet temperatures are shown in Figures II.2.4-18a and II.2.4-18b. The initial power rise, for all expansion reactivity values, is approximately 65%. Thereafter, the power rise, as well as the cermet temperatures, decreases as the expansion coefficient is made more negative.

The effectiveness ratio for the cermet rod as a function of the expansion reactivity coefficient is shown in Figure II.2.4-19. Also shown is the equivalent Doppler coefficient, based on fuel temperature, for the combined reactivity feedbacks from the cermet expansion and Doppler. The cermet's contribution to the equivalent Doppler coefficient is proportional to the cermet's expansion coefficient.

The hotspot temperatures for the power scram analysis are well below the limiting values given in Section II.2.4.3.

II.2.4.4.4 Coolant Inlet Temperatures Effects

In this study, the coolant inlet temperature is increased by 120°F thru a range of time lags; 2.5, 5.0, and 10 seconds. The reactivity coefficients are set to the nominal value given in Table II.2.4-1.

Figure II.2.4-17 Maximum Hotspot Temperatures During a Power Scram After a Step Reactivity Insertion, Δk , of 40 cents as a Function of Cermet Rod Diameter

Figure II.2.4-18 Effect of Cermet Expansion Coefficient for CEX Fuel Assembly: Step Reactivity Insertion, $\Delta k = 40$ cents

Figure II.2.4-19 Equivalent Doppler Coefficient and Cermet Rod Effectiveness Ratio as a Function of Cermet Expansion Coefficient

The volumetric heat generation rate in the cermet is 10%. The 120°F increase in inlet temperature is approximately equivalent to losing one-half of the secondary loop heat removal capacity. The average temperature rise across the core is 221°F.

The results for this phase of the study are shown in Figures II.2.4-20a thru II.2.4-20h. For each time lag, the coolant heats the cermet at such a rate that the cermet expansion reactivity feedback overrides the reactivity additions from the coolant, and the clad expansion and thus the thermal power and the fuel temperatures decrease with time. The increase in the cermet's average temperature is slightly greater than the increase in the coolant. With the reduction in power, the fuel temperatures are lowered and thus the fuel acts as a heat sink to hold down the clad temperature rise.

II.2.4.4.5 Reactivity Insertion Above Prompt Critical

The extremely short neutron lifetime, in the order of 10^{-7} seconds in a fast reactor, gives a period of prompt criticality in the millisecond range. The transient flux or power response of a fast reactor during the initial burst of power is of great importance in the safety aspects of the core. An insight as to the effect of important parameters, such as Doppler, on the kinetic behavior of the core would be desirable. An initial time model analysis of reactor power with reactivity feedback is presented for a step insertion of 1-1/2 dollars of reactivity. A step insertion is considered the worst case for the addition of reactivity.

The time rate equations for reactor power, fuel average temperature, and cermet average temperature are considered for short periods. During this time, the assumption is made that no heat conduction has taken place between the fuel or cermet and coolant. Also, for the time involved, the effect of the delayed neutron precursors may be neglected. The reactivity is considered as an insertion term and the negative reactivity feedback is an equivalent Doppler based on the fuel temperature. Cermet rod expansion is also a negative reactivity feedback, once the fuel bundle

Figure II.2.4-20 Effect of Coolant Inlet Temperature with CEX
Fuel Assembly: Temperature Increase of 120°F

Figure II.2.4-20 Effect of Coolant Inlet Temperatures with CEX Fuel Assembly: Temperature Increase of 120°F

Figure II.2.4-20 Effect of Coolant Inlet Temperature with CEX Fuel Assembly: Temperature Increase of 120°F

begins to move. The worth of the cermet's expansion, for a volumetric heat generation rate of 10%, after movement is attained but before heat conduction to the coolant occurs, is equivalent to an addition to the Doppler coefficient of minus $0.57 \times 10^{-6}/^{\circ}\text{F}$.

The resulting set of non-linear equations was solved for a step reactivity insertion with the reactor at 100% power and temperature. The transient behavior for such an insertion is a very rapid increase in neutron flux or power with subsequent heating of the fuel and cermet. The maximum flux level is reached at the point where reactivity feedback, from the equivalent Doppler, compensates for the initial insertion. As the fuel temperature continues to increase, the net reactivity becomes negative with a corresponding decrease in neutron flux until it finally reaches the initial level. At this point, the fuel temperature reaches its maximum value.

The flux profile determined for a reactivity step insertion of $1-1/2$ dollars with two different reactivity coefficients are shown in Figures II.2.4-21 and II.2.4-22. For a given initial thermal power and total mass of fuel, the parameters of interest are the neutron lifetime, and the equivalent reactivity feedback coefficient, $\left(\frac{\Delta k}{T} \right)_{\text{Eq. Dop}}$. As seen in both of the previous Figures, the larger the absolute value of $\left(\frac{\Delta k}{T} \right)_{\text{Eq. Dop}}$, the smaller the magnitude and duration of the power excursion for the given insertion. The longer neutron lifetime slowed down the response of the system (magnitude) but broadened the power peak, as can be seen by comparing Figures II.2.4-21 and II.2.4-22. The peak neutron flux level encountered during an excursion for the reactivity insertion being considered versus Doppler is shown in Figure II.2.4-23. However, the total energy released for a given $\left(\frac{\Delta k}{T} \right)_{\text{Eq. Dop}}$ is the same, i.e., is independent of the neutron lifetime.

The changes in fuel and cermet temperatures for the power excursions investigated are shown in Figures II.2.4-21 and II.2.4-22. Because the volumetric heat generation in the cermet is only 10% that of the fuel in this analysis, the cermet temperature rise is proportionally lower than that of the fuel. As the equivalent negative reactivity feedback

Figure II.2.4-21 Neutron Flux and Average Temperature, Fuel and Cermet, Rises Versus Time After Step Reactivity Insertion of 1-1/2 Dollars: Neutron Lifetime of 1×10^{-7} Seconds

Figure II.2.4-22 Neutron Flux and Average Temperature, Fuel and Cermet, Rises Versus Time After Step Reactivity Insertion of 1-1/2 Dollars: Neutron Lifetime of 3×10^{-7} Seconds

Figure II.2.4-23 Peak Neutron Flux and Average Fuel Temperature Rise Occurring During a Step Reactivity Insertion of 1-1/2 Dollars as a Function of Doppler Coefficient

coefficient, $\frac{\Delta k}{T}$)_{Eq. Dop.}, is increased, the fuel and cermet temperatures are reduced, substantiating the postulation that less energy is generated when the feedback is large.

For the conditions of this analysis, the neutron flux returns to essentially the initial level within 1 to 3 milliseconds, depending on neutron lifetime. The power follows a normal distribution with a maximum value as given in Table II.2.4-3.

TABLE II.2.4-3

Maximum Neutron Flux Temperatures for Insertion
of 1-1/2 Dollars of Reactivity

Equivalent Reactivity Coefficient, $\frac{\Delta k}{T}$) Eq. Dop.	Neutron Lifetime Seconds	Maximum Neutron Flux q^*/q_0	Fuel Average Temperature Rise °F	Cermet Average Temperature Rise °F
$-1.57 \times 10^{-6}/^{\circ}\text{F}$	1×10^{-7}	21,800	2450	190
$-1.57 \times 10^{-6}/^{\circ}\text{F}$	3×10^{-7}	7,160	2450	190
$-4.57 \times 10^{-6}/^{\circ}\text{F}$	1×10^{-7}	7,450	870	60
$-4.57 \times 10^{-6}/^{\circ}\text{F}$	3×10^{-7}	2,250	870	60

As seen in Table II.2.4-3 for a Doppler of $-1.57 \times 10^{-6}/^{\circ}\text{F}$, the fuel average temperature rise may be as high as 2450°F on the absence of heat conduction to the coolant. This value may be considered reasonable since a step increase in reactivity is an extreme case and the time for the power excursion is less than 3 milliseconds. The average fuel temperature rise during the excursion for various Doppler coefficients is shown in Figure II.2.4-23. For a Doppler of approximately $-4.4 \times 10^{-6}/^{\circ}\text{F}$ at a step reactivity insertion of 1-1/2 dollars, the center temperature of the fuel at the hotspot reaches the melting point for the mixed carbide. The time constant of the fuel rod is 0.48 seconds.

The cermet's contribution to the negative reactivity feedback for terminating a prompt excursion of this duration is of interest. The period

of the natural frequency of the fuel bundle and cermet rod is approximately 10 to 11 milliseconds. Consequently, the cermet rod would require approximately 4 milliseconds to expand to a new equilibrium value after a step insertion of reactivity.

As the power reverts to its initial value in 3 milliseconds or less, the movement of the cermet is extremely small and thus is considered negligible for this analysis. If the reactivity insertion was by a 2 millisecond or longer ramp function rather than by a step, the duration of the power excursion would be sufficiently long for the cermet rod to contribute to the negative reactivity feedback. The peak thermal stress in the cermet rod for a step insertion of 1-1/2 dollars in reactivity at $-1.57 \times 10^{-6}/^{\circ}\text{F}$ and $-4.57 \times 10^{-6}/^{\circ}\text{F}$ Doppler coefficient are 65,000 psi and 20,600 psi. This stress reduces by relaxation once the bundle begins to expand. For ramp insertion of reactivity, the peak stress will be considerably lower than for a step, as the increase in the cermet temperature occurs over a longer period of time. This gives the cermet rod sufficient time to move the fuel bundle and thus relieve the thermal stress.

The results of this analysis are considered to be quantitatively within reason and qualitatively correct.

Module Kinetic Behavior

A preliminary analysis has been performed of the characteristics to be expected of a module in an array, relative to an isolated core.

The kinetics equations for a single isolated core may be written as:

$$\frac{dn(t)}{dt} = \frac{\partial k - \beta}{l^*} n(t) + \sum_j \lambda_j C_j \quad (1.a)$$

$$\frac{dC_j}{dt} = \frac{\beta_j}{l^*} n(t) - \lambda_j C_j \quad (1.b)$$

where $n(t)$ = neutron population at time t
 ∂k = excess reactivity above steady state requirements
 β = total delayed neutron fraction
 l^* = effective prompt neutron lifetime
 C_j = delayed neutron precursor population at time t
 λ_j = delayed neutron decay constant
 β_j = partial delayed neutron fraction

If this same core is placed in an array of identical modules, neutrons will enter the core from the other modules in the array; consequently it will be necessary to remove a certain amount of reactivity from the core to maintain a steady state condition. The rate at which neutrons entering the core from an adjacent module alter the neutron population can be expressed as:

$$\frac{\tau_{ji} n_{ji}(t - l_{ji})}{l^*}, \quad \tau_{ii} = 0$$

where the subscript i refers to the module of interest and the subscript j refers to another module in the array. The quantity l_{ji} is the neutron transport time from module j to module i , and $n_{ji}(t - l_{ji})$ is the neutron population in module j at the time when the neutrons now arriving in module i left module j . The τ_{ji} is a transfer coefficient for neutrons from j to i . The magnitude of τ_{ji} is determined by the geometric and material properties of the modules. The change in reactivity required to maintain the core in steady state can be written as ∂k^0 .

Thus the kinetic equations for any module i can be expressed as:

$$\begin{aligned}
 \frac{dn_i(t)}{dt} = & \left(\frac{\partial k_i + \partial k_i^0 - \beta}{l^*} \right) n_i(t) + \sum_j \lambda_j C_{ji} + \\
 & + \frac{1}{l^*} \sum_j \tau_{ji} n_j(t - l_{ji})
 \end{aligned} \tag{2.a}$$

Note that: $\partial k_i^0 < 0$.

$$\frac{dC_{ji}}{dt} = \frac{\beta_j}{l^*} n_i(t) - \lambda_j C_{ji} \quad (2.b)$$

In the steady state condition of the array, the contribution to the neutron population growth in module i, occasioned by the neutrons flowing in from the other modules, is exactly balanced by the reduction in reactivity ∂k_i^0 . That is:

$$\partial k_i^0 n_i = - \sum_j \tau_{ji} n_j \quad \text{in steady state.}$$

A portion of the reactivity ∂k_i^0 is attributable to each of the other modules, and in fact

$$\partial k_{ji}^0 n_i = - \tau_{ji} n_j \quad \text{in steady state.}$$

Then the magnitude of the transfer coefficients can be determined from:

$$\tau_{ji} = - \partial k_{ji}^0 \left(\frac{n_i}{n_j} \right) \quad (3)$$

either experimentally or theoretically in steady state. For identical modules in steady state with $n_i = n_j$, then

$$\tau_{ji} = - \partial k_{ji}^0.$$

The kinetic equations for identical modules can then be written as:

$$\begin{aligned} \frac{dn_i(t)}{dt} = & \frac{1}{l^*} (\partial k_i^0 - \beta + \sum_j \partial k_{ji}^0 (1 - \frac{n_j(t-l_{ji})}{n_i(t)})) n_i(t) + \\ & + \sum_j \lambda_j C_{ji} \end{aligned} \quad (4.a)$$

$$\frac{dC_{ji}}{dt} = \frac{\beta_j}{l^*} n_i(t) - \lambda_j C_{ji} \quad (4.b)$$

The only difference between equations (1.) for a single core and (3.) for one module in an array is the term

$$\left(\sum_j \partial k_{ji}^o \left(1 - \frac{n_j(t-l_{ji})}{n_i(t)} \right) \right) n_i(t) / l^*$$

In the steady state, this term is zero because $n_j(t-l_{ji}) = n_i(t)$. If the normal steady state, neutron populations do not satisfy this requirement, then the definition of the τ_{ji} from equation (4.) must be used and the term above becomes:

$$\left(\sum_j \partial k_{ji}^o \left(1 - \frac{n_i(s)n_j(t-l_{ji})}{n_j(s)n_i(t)} \right) \right) n_i(t) / l^*$$

where $n_i(s)/n_j(s)$ is the ratio of the neutron populations in steady state, and the whole term remains zero in steady state.

For the cases of interest here, the normal steady state is $n_i = n_j$.

Now consider the case in which excess reactivity is inserted into module 1 only. The kinetics equations are:

$$\begin{aligned} \frac{dn_1(t)}{dt} = & \frac{1}{l^*} (\partial k_1 - \beta + \sum_j \partial k_{j1}^o \left(1 - \frac{n_j(t-l_{j1})}{n_1(t)} \right)) n_1(t) + \\ & + \sum_j \lambda_j C_{j1} \end{aligned} \quad (5.a)$$

$$\begin{aligned} \frac{dn_i(t)}{dt} = & \frac{1}{l^*} (-\beta + \sum_j \partial k_{ji}^o \left(1 - \frac{n_j(t-l_{ji})}{n_i(t)} \right)) n_i(t) + \\ & + \sum_j \lambda_j C_{ji} \quad , \quad i \neq 1 \end{aligned} \quad (5.b)$$

$$\frac{dC_{j1}}{dt} = \frac{j}{1^*} n_1(t) - \lambda_j C_{j1} \quad (5.c)$$

The net excess reactivity available in module 1 is:

$$\partial k_1 = \sum_j | \partial k_{j1}^o | \left(1 - \frac{n_j(t-l_{j1})}{n_1(t)} \right)$$

where the absolute value of ∂k_{j1}^o is employed to emphasize the fact that ∂k_{j1}^o is a negative quantity.

The ratio $n_j(t-l_{j1})/n_1(t)$ must always be less than unity after excess reactivity has been inserted into module 1 (except instantaneously at the instant of insertion). Therefore, the quantities $(1 - n_j(t-l_{j1})/n_1(t))$ must always be positive after the insertion. This, then, makes the net effective reactivity insertion always less than the actual insertion, ∂k_1 .

For example, consider a two core case; and assume ∂k_1 is sufficiently large to cause $n_1(t)$ to exceed $n_2(t-l_{j1})$ by 11%, with ∂k_1^o equal to 0.02. The net excess reactivity is then reduced by the amount $0.02(1-0.9) = 0.002$, or about a 50 cent reduction. This would result from an insertion of the order of one dollar.

The physical interpretation of this effect is simple. In the steady state, the in-leakage from adjacent modules just balances the fraction by which each module would be subcritical out of the array. When one module "takes off", it cannot pull the other modules along with it completely. Therefore, the ratio of in-leakage to total neutron population decreases, and the in-leakage no longer compensates for the full difference in reactivity between the module in the array and the module in isolation.

The effective reactivity which disappears from the module experiencing an insertion of reactivity does not actually disappear. As can be seen from the equations for the other modules, it is transferred to the other modules. This transfer, however, can never become sufficient to bring the other modules up to power to the level of the module experiencing the actual physical

insertion of reactivity because the effective reactivity transfer occurs solely as a result of the difference in power. Thus it is logical to describe the phenomenon as one of reactivity sharing.

In the reference modular core design, each module is physically isolated from the others by the heavy-walled graphite cans attached to the vessel at both top and bottom. Each module has its own control system, so that each module is also isolated in the control sense. Thus the modules are nearly statistically independent from the point of accident considerations. Consequently, for most accident situations, the kinetic behavior of the reactor is controlled by the reactivity sharing effect, and the response to an accidental insertion of reactivity is much slower than that of one module in isolation. The modular reactor tends to be much more sluggish in response to most accidents than a single core.

The reactivity sharing effect also provides a restoring force which tends to maintain the modules in power balance. This restoring force is non-linear, and increases rapidly as the degree of unbalance increases.

In order to ensure that the results would be highly conservative, neither the reactivity sharing effect nor the increase in λ^* occasioned by the modular design were considered in the transient analyses quoted in Section II.2.4.4.

II.2.4.5 Conclusions: Stability and Transient Analysis

The analog studies on the 1000 MWe Fast Breeder Reactor show the core to be inherently stable and to have a good response to reactivity changes over the range of reactivity coefficients investigated (Table II.2.4-1). All reactivity insertions introduced into the analog simulation are by step functions. The range of reactivity coefficients is based on a set of nominal values which were considered to be reasonable at the beginning of the analog study. However, since completing this stability analysis, the latest calculated Doppler coefficient is $-6.6 \times 10^{-6}/^{\circ}\text{F}$, which is more negative than the best value of Doppler given in Table II.2.4-1. Also, the latest calculated neutron lifetime is 3×10^{-7} seconds; thus the core stability with a negative Doppler coefficient of this value will be improved over the results for the $-4.0 \times 10^{-6}/^{\circ}\text{F}$ Doppler coefficient.

Use of cermet rods, as in the CEX fuel assembly, to provide a strong negative mechanical (expansion) temperature coefficient in a ceramic fueled core, enhances the core stability and response to reactivity changes. The importance of the cermet rod on the transient power response is shown in Figure II.2.4-24, which compares the power rises with and without the cermet rod for two Doppler coefficients, $-1.0 \times 10^{-6}/^{\circ}\text{F}$ and $-2.0 \times 10^{-6}/^{\circ}\text{F}$. In this comparison, the volumetric heat generation rate in the cermet rod is 10% and the rod diameter is 0.300-inch. The reactivity feedback from the cermet rod reduces the power rise (Figure II.2.4-24) by approximately 45%. Increasing the cermet's volumetric heat generation rate improves the transient power response to reactivity insertions.

A figure of merit for the cermet's contribution to the total negative reactivity feedback after a reactivity insertion is given by the effectiveness ratio. The effectiveness ratio is defined as the ratio of the cermet's reactivity feedback to the sum of the cermet plus Doppler reactivity feedbacks. The effectiveness ratio for the cermet increases with time after the reactivity insertion, reaching some final value. The rate of increase of the effectiveness ratio is a function of the time constant of the cermet rod. For a cermet rod diameter of 0.300-inches and 10% volumetric heat generation rate, the effectiveness ratio reaches 95% of its final value in 2.0 to 3.5 seconds, depending on the Doppler coefficient (Figure II.2.4-4) after the reactivity insertion. Increasing the volumetric heat generation rate in the cermet rod improves the effectiveness ratio.

With the CEX assembly, the sodium coefficient showed only a small effect on the core power response when varied from $+5.0 \times 10^{-6}/^{\circ}\text{F}$ to $-5.0 \times 10^{-6}/^{\circ}\text{F}$. Reactivity added by a 10°F rise in coolant temperature was offset by a 7° rise in the cermet's average temperature. The results from the increased inlet temperature study illustrated the cermet's overriding negative feedback.

The maximum temperatures expected in the fuel and cermet rod during a power scram after a step insertion of 40 cents are below the limiting values.

Figure II.2.4-24 Comparison of Core Power Rise With and Without the CEX Fuel Assembly: Step Reactivity Insertion, $\Delta k = 40$ cents

From the power scram analysis, a volumetric heat generation rate of 20% is recommended for the cermet rod. The cermet's maximum center line temperature at this heat rate, and for a Doppler coefficient of $-1.0 \times 10^{-6}/^{\circ}\text{F}$ after a step reactivity insertion of 50 cents was 1650°F . As the Doppler coefficient became more negative, the cermet's hotspot temperature decreases.

The neutron flux during the initial burst after a step insertion of $1\frac{1}{2}$ dollars of reactivity is several thousand times the initial neutron flux level, even for the latest calculated neutron lifetime of 3×10^{-7} seconds. The power returns to its initial level in 3 or less milliseconds. Assuming the Doppler coefficient varies inversely with temperatures, the Doppler coefficient of $-4.57 \times 10^{-6}/^{\circ}\text{F}$ represents, approximately, the average Doppler coefficient during an excursion for the latest calculated value of Doppler, $-6.0 \times 10^{-6}/^{\circ}\text{F}$. Integrating the energy under the neutron flux curve, the average fuel temperature rises 870°F , and the cermet average temperature rises 60°F . The cermet's reactivity feedback is negligible since the movement achieved by the cermet during the power burst is extremely small. The hotspot centerline temperature in the cermet rod is approximately 1800°F , and the fuel hotspot center temperature is near the melting point ($\sim 4150^{\circ}\text{F}$). Due to the low fuel temperature (maximum hotspot centerline of 2184°F) at 100% power, the fuel can absorb an integrated energy equal to approximately 7.8 full-power-seconds before fuel melting occurs at the hotspot. The amount of reactivity which can be inserted in a step as a function of Doppler to produce fuel centerline melting at the hotspot is shown in Figure II.2.4-25.

II.2.4.6 Operational Nuclear Instrumentation System

The function of the operational nuclear instrumentation system is to monitor the FBR power from source level through the intermediate level and up to 125% of full power. This is accomplished by means of fast neutron flux detectors located in instrument thimbles in the graphite assemblies of the core adjacent to each of the seven active regions. The system provides indication, control and alarm signals for reactor operation and

Figure II.2.4-25 Step Reactivity Insertion for Fuel Centerline Melting at the Hotspot as a Function of Doppler Coefficient

protection. (The term "operational" is used to distinguish this system from any neutron flux mapping system that may be provided.

The anticipated location of 15 thimbles for the installation of fast neutron detectors is shown in Figure II.2.4-26. Methods by which these thimbles would be installed in the graphite areas have not been developed at this time because the physical characteristics of the detectors are not presently known. The performance capabilities of the proposed fast neutron detectors; their physical dimensions, mechanical, thermal and nuclear limitations; must be determined by a research and development program. Means for providing instrument thimbles can then be developed.

For the purpose of this reference design, it is assumed that detectors can be developed which will respond satisfactorily to the fast neutron spectrum of the flux as seen in the graphite areas, throughout the range from startup to full power levels. It is considered that no single detector can be developed to cover the entire range, and this reference design therefore presupposes the use of two detectors in each thimble: the first a highly sensitive neutron counter for use in the source range; and the second a compensated ionization chamber with reduced sensitivity for the intermediate and power ranges.

The detector locations are chosen to provide two channels for each of the outer six active regions of the core which are as independent as possible from the other regions. These pairs have approximately 120-degree spacing with respect to the center of each region and are located in the outer rows of graphite assemblies (see Figure II.2.4-26). For the center region, an attempt is made to monitor the flux by three channels at 120-degree spacing in the inner graphite barriers around the central region, although considerable contributions to the flux at these locations will arrive from the other six regions.

It is assumed that fission counters will be used in the source range. Pre-amplifiers near the reactor vessel are required with fission counters due to the smaller pulses produced compared with those from proportional counters (Figure II.2.4-27).

Figure II.2.4-26 1000 MW Fast Breeder Reactor.
Location of Neutron Detector
Thimbles

Figure II.2.4-27 1000 MW Fast Breeder Reactor. Block Diagram, Typical Nuclear Instrumentation for One Region of FBR Core

The fast neutron flux, as measured in the graphite areas, produces current pulses in the fission counters. After pre-amplification, these pulses are transmitted to the control room to transistor amplifiers and discriminators to separate the pulses due to fast neutron reactions from those due to gamma-ray or other reactions. The pulses are converted to direct currents proportional to the count rates, which in turn are amplified in logarithmic micro-microammeters. Differentiating circuits compute the startup rates. These channels indicate the source range levels and startup rates and provide startup rate rod stop signals to the control rods of the particular region covered, and alarm signals to the control board annunciators. At shutdown, they provide alarms of any inadvertent increases in reactivity in that region.

The intermediate range channels utilize compensated ionization chambers with direct current outputs to transistor logarithmic amplifiers and rate circuits in the control room. These channels indicate the intermediate levels and startup rates, and provide startup rate rod stop and scram signals for the particular region covered, and alarms to the control board annunciators.

The same currents from the ion chambers are also used for direct meter indications in the power range, in order to reduce errors due to amplification. These currents are also supplied to averaging and differential circuits (see Figure II.2.4-28). They supply low level scram signals during startup and high level rod stop and scram signals during power operation by means of bistable trip circuits. Calibration for the power range currents is provided by adjustable precision shunts which can set the percentage readings of the ion chamber indicators in proportion to the percentage of rated power in that region, as determined from process instrumentation and calorimetric computations. The ion chambers are approximately the same length as the active core height (72 inches) in order to provide integrated or average axial flux measurements, rather than point measurements. This reduces the errors due to axial flux peak shifts due to control rod movements or other causes.

REFERENCES

1. Batenburg, A. and Godts, J. E., "On the Simulation of Fuel Element Temperatures Based on Finite Transforms", WCAP-2261.
2. Godts, J. E., "Analog Simulation of the Stress Load in a Fast Reactor Cermet", WCAP-2522.

Figure II.2.4-28 1000 MW Fast Breeder Reactor. Nuclear Instrumentation, Power Range Averaging and Differential Circuits

The calibrated currents from the two chambers for each region are compared with one another in differential circuits. If there should be an aximuthal flux shift within a region, it should be detectable because the two detectors are symmetrically spaced with respect to the region fuel and control rod geometries.

Each region also has an averaging circuit (see Figure II.2.4-28) for the two channels, and the seven averages are then supplied to an overall average circuit. Differential circuits then measure any shifts of flux from one region to another with respect to the entire core. The detection of flux shifts is shown by alarms only, with correction to be done by manual manipulation of control rods as required. The average of the seven regions is available for use in the overall plant control system.

Scrams of the safety rods for each region occur upon scram initiation in the individual channels of each region. As shown in the scram logic circuit of Figure II.2.4-29, a full reactor scram is initiated if there are coincident scram signals from any two separate regions.

Built-in test facilities for manual checks and calibrations of all channels are part of the operational nuclear instrumentation system. Recorders are provided for logging selected source and intermediate channel levels of all seven regions during startup, and for logging the seven average power range levels during power operation.

II.2.4.7 Primary Plant Control System

The overall plant control philosophy is illustrated in the control block diagram, Figure II.2.4-30. This block diagram illustrates nuclear as well as secondary plant controls. Nuclear plant control is based on control and shim rod motion initiated by changes in primary coolant temperature. Load control is accomplished by programming the mixed mean reactor outlet temperature control set point according to desired power output up to 50% plant capacity. At 50% plant capacity, the temperature program will achieve the 1200°F maximum operating value of

Figure II.2.4-29 1000 MW Fast Breeder Reactor. Scram Logic Circuitry

Figure II.2.4-30 1000 MW Fast Breeder Reactor. Block Diagram Plant Control System

the mixed mean reactor outlet temperature. From 50% to 100% plant load capacity, the power removed from the reactor core is governed by speed control of the primary coolant loop circulating pumps. Above 50% capacity, mixed mean reactor outlet temperature is controlled to a constant 1200°F. The pump speed control governs primary coolant mass flow rate according to a predetermined pump speed versus load control program. The speed versus load control receives a power demand signal which is compared with plant output power to derive a very slow reset control mode. The reset control modifies the speed versus load set point to eliminate offset between desired and actual plant output.

The primary coolant loop flow rate is not directly controlled on the basis of closed loop feedback control. Pump speed control with plant load deviation for error correction governs the primary loop heat transfer rates.

The steam plant consists of once-through steam generators with separate reheaters. Steam temperature and pressure are programmed below 50% plant load. Above 50% plant load, steam temperature and pressure are held constant.

II.2.4.7.1 Reactor Control

Reactor control is accomplished by automatic positioning of two groups of control rods. Primary control is accomplished by the "control" rod group. The control rod group consists of a control rod in each core region. The control rods are directly actuated by the mixed mean outlet temperature controller, sequentially in small increments; and are synchronized to maintain equal rod position for the seven core regions. By providing overall reactor control through uniform action of a control rod in each core region, the mixed mean outlet temperature is directly controlled.

Secondary reactor control is accomplished by the "trim" rod group. Each core region coolant outlet temperature is measured and compared to the

mixed mean outlet temperature. The trim rod comparator and control initiator (Figure II.2.4-30) derives control signals based on individual core region temperature deviation from the reactor mixed mean outlet temperature. The trim rods are actuated very slowly to drive the individual core region temperatures back to a narrow dead-band temperature range. The dead-band temperature controller is designed to provide floating set point control for each of the individual core regions, based on the measured value of the mixed mean outlet temperature. The automatic trim rod control action is slow in terms of rate of change of reactivity as compared to the rate of change of reactivity of the control rod group.

II.2.4.7.1.1 Control Rods

The reactor control rods consist of one automatic control rod in each core region (total of seven). The control rods are programmed to move one at a time and in small, fixed increments to limit the maximum rate of change of reactivity. In addition, the control rods are sequenced to maintain equal (to one increment) control positions over the entire reactor core. The control rods function is to maintain the desired mixed mean reactor outlet temperature as determined by the load versus temperature control programmer.

II.2.4.7.1.2 Trim Rods

The reactor trim rods consist of one automatic control rod in each core region for a total of seven. These trim rods control the individual core region outlet temperatures within an adjustable dead-band temperature range which is adjustable with respect to the variable mixed mean reactor outlet temperature. This permits adjustment of the control band to minimize interaction of the trim rods with the control rods. Thus, the trim rods are controlled directly by a floating set point derived from the measured value of the reactor mixed mean outlet temperature. The floating set point maneuvers, as a result of programmed mixed mean reactor outlet temperature change, are in the range of less than 50% power. Above 50%

power, the floating set point will be stabilized as a result of mixed mean reactor outlet temperature reaching a steady value of 1200°F.

II.2.4.7.1.3 Safety Rods

The reactor safety rods consist of two rods in each core region for a total of fourteen. These rods are manually actuated for rod withdrawal but are automatically inserted by scram functions.

II.2.4.7.1.4 Alternate Control Configuration

For the reference design control system the central rod in each module serves as the "trim" rod and provides the reactivity compensation for temperature changes and burnup. The duty required of this rod permits its rate of motion to be severely restricted mechanically, so that its large worth does not adversely affect reactor safety.

The "control" rods need only a very small amount of available reactivity. This can be accomplished in any of three ways within the reference design without changing the basic control concept. The method previously outlined utilizes one of the three peripheral rods in the almost fully withdrawn position. The two alternate approaches utilize all three outer rods solely as safety rods. The "control" rod can be a small, lightweight rod operating either off center or in the center of the "trim" rod. This would require a channel within the central "trim" rod to accommodate the "control" rod. Alternatively, the "control" rod function can be performed by superimposing a short-stroke mechanism on the "trim" rod drive. Thus the "trim" rod could operate rapidly over a very short range to perform the "control" rod duty. This arrangement would not alter the basic mechanical limit on speed of insertion in the "trim" rod mode.

II.2.4.7.1.5 Coolant Flow Distribution

The reactor coolant inlet plenum is compartmented to provide seven controllable flow paths. Each path is provided with manually operated valves to regulate coolant flow to the seven reactor core regions. The remainder of the reactor coolant passes through the radial blanket regions. Control of coolant distribution facilitates balance of reactor power between core regions and blanket areas as the core versus blanket power varies with time.

Temperature recorders are provided with sensing elements located above the radial blanket regions. These temperature records show the trend of temperature change in the blanket region as blanket power output varies over core life. The rate of change of blanket coolant outlet temperature will be very slow with respect to the mixed mean reactor outlet temperature. As the blanket areas increase in power output, the manual throttle valves in the flow paths to the core regions will be adjusted to maintain desired temperature gradients between the blanket areas and the core regions. The proposed R & D program will determine what core versus blanket temperature gradients would be required during various stages of core and blanket life.

II.2.4.7.1.6 Mixed Mean Outlet Temperature

The reactor mixed mean outlet temperature is measured by groups of temperature sensors in the reactor outlet coolant lines. These temperature sensors are arranged radially in each outlet line and connected electrically to detect average temperature. The average temperature signal from each reactor outlet line is then inter-connected electrically to measure the average of the average temperatures. As a

result of the high coolant flow velocities and multiple points of measurement, a direct measurement of reactor mixed mean outlet temperature is assured.

II.2.4.7.1.7 Core Region Outlet Temperature

Core region outlet temperature is measured by a group of three temperature sensors placed above each core region and arranged radially in the coolant flow path above the enriched fuel modules. The three sensors are interconnected electrically to measure the average outlet temperature of the respective core region.

II.2.4.7.1.8 Radial Blanket Outlet Temperature

Radial blanket outlet temperature is measured by twenty-one individual temperature sensors located in the outlet coolant flow path of the radial blanket assemblies. Three temperature sensors are provided for the radial blanket area of each core region. The blanket outlet temperatures are recorded to provide the reactor operator with blanket outlet temperature history and a continuing plot of the trend of temperature change. The reactor operator will manipulate the individual core region inlet coolant flow control valves based on the recorded blanket outlet temperatures.

II.2.4.7.1.9 Primary Coolant Flow Control

Primary coolant flow is controlled by speed control of the circulating pump motors. Below 50% reactor power the pumps operate at minimum speed. From 50% to 100% reactor power, the pumps are programmed to follow prescribed speed versus % reactor load program.

Above 50% power, the mixed mean reactor outlet temperature is constant; therefore reactor power is directly dependent on primary coolant flow.

Primary coolant flow, by virtue of pump speed control, is the dominant control factor of reactor power output. Closed loop flow control has been avoided in this design due to its attendant problems and inaccuracies.

Because pump speed versus head and flow characteristics vary with pump wear and with changes in core configuration and certain other factors, a reset control mode has been provided. The reset control function slowly adjusts pump speed to bring plant power output to the plant load set point.

II.2.4.7.1.10 Safety and Interlocks

Safety functions are primarily accomplished from nuclear signals. Process variables, such as loss of coolant temperature error, are interlocked to cause rod stop, fast run-down and scram as required. The final safety and interlock design criteria will be established following a computer analysis of the reactor and power plant systems.

II.2.4.8 Accident Considerations

Four ground rules were established as a guide to safety for the core design in the 1000 MWe Fast Breeder Reactor study.

1. The core should have a negative sodium temperature coefficient at full power and exhibit a negative reactivity insertion upon complete removal of sodium from the core. In event of a positive reactivity insertion associated with partial sodium voiding of the core, the available negative reactivity should be sufficiently great to terminate the power excursion without gross damage to the core.
2. The Doppler coefficient of reactivity should be negative.
3. The CEX fuel assembly should be used to provide a strong negative mechanical (expansion) temperature coefficient in the core. This provides additional assurance, in addition to the Doppler effect, of good transient response to temperature increases.

4. The controlled reactivity should be the minimum consistent with the breeding ratio and the fuel management program.

To satisfy the criteria with regard to the sodium temperature coefficient, a segmented core, wherein the core regions are separated by blanket material, was selected. This arrangement increases the neutron leakage, which is the major negative contributor to the sodium coefficient. The leakage can be controlled by adjusting the size of the core regions.

The sodium coefficient varies as a function of position in the core. The negative reactivity contribution due to scattering leakage takes place primarily near the boundary of the core. At the center of the core, a change in the neutron direction has no effect on the leakage probability. The effect of moderation, by the sodium, on reactivity is most powerful near the core center. Thus, partial sodium voiding in the center of the core may result in a positive reactivity insertion.

The ability of the CEX fuel assembly to provide the core with a strong negative mechanical (expansion) temperature coefficient was shown by the Transient Analysis study as reported in Section II.2.4.1. This feature will minimize the possibility of a positive reactivity insertion occurring from partial sodium voiding because the cermet rods will be expanding during the time required to heat the sodium to its boiling point. At mid-core height, the sodium temperature in the hot channel is 420°F below the boiling point at atmospheric pressure. The Transient Analysis study of increasing inlet temperature on the core power response, Section II.2.4.4.4, showed the cermet rod expansion reactivity feedback to be greater than the positive reactivity addition from the positive sodium coefficient.

The occurrence of a positive reactivity insertion from fuel slumping is considered very remote because of the low fuel temperature. The fuel maximum center temperature is 2184°F which is approximately 2000°F below

the melting point of the mixed plutonium-uranium carbide. The average fuel temperature in the core is 1326⁰F. The fuel slumping effect is further minimized by compartmenting the stack of fuel pellets.

II.3 REACTOR PLANT DEVELOPMENT

II.3.1 Systems and Components Design

II.3.1.1 Primary Coolant System

II.3.1.1.1 General Description

The function of the primary coolant system is to transfer heat from the reactor core to the intermediate sodium system. The process flow diagram of this system is shown in Figure II.3.1-1. The system consists of two identical loops connected in parallel to the reactor vessel. Each loop contains two circulating pumps, an intermediate heat exchanger, the connecting piping and instrumentation necessary for operational control. All system equipment that contains, or can contain, sodium is electrically or gas heat-traced. All primary system equipment is located inside the reactor containment.

During operation, free surface type pumps circulate 128×10^6 lb/hr of sodium through the intermediate heat exchanger and the reactor vessel. The sodium coolant is heated from 979°F to 1200°F as it passes through the core. It next flows to the pumps and then to the intermediate heat exchangers, where the heat is transferred to the intermediate sodium system, and then back to the reactor vessel to repeat the cycle.

Cold traps are provided to remove gross amounts of sodium oxide from the sodium coolant. Primary sodium coolant flows from the intermediate heat exchanger effluent to a regenerative heat exchanger and then to the cold trap. The cold trap effluent flows to the pump suction via the regenerative heat exchanger. To prevent the solidification of sodium, the cold traps are not employed to remove sodium oxides at temperatures less than 250°F . The cold traps can be removed and replaced while the plant is in operation.

Figure II.3.1-1 Primary Sodium System Flow Diagram

When the plugging temperature is approximately 250°F, a hot trap will be placed in service and the cold trap flow may be reduced. Because the hot leg temperature is approximately 1200°F, the hot trap requires little heating. The hot trap normally employs the head developed by a main coolant pump for establishing the necessary flow.

A plugging indicator is installed in parallel with the cold trap system. The indicator consists of a regenerative heat exchanger, a cooled orificed chamber and necessary instrumentation and controls. The plugging indicator is designed to continuously determine the sodium oxide content of the coolant.

Each intermediate heat exchanger and pump is located in a separate shielded cubicle to reduce radiation dose rates during maintenance. Each cubicle is continuously purged with nitrogen to prevent sodium fires should sodium leaks develop.

II.3.1.1.2 Description of Primary System Equipment

Reactor Vessel

The reactor vessel is a cylindrical vessel of austenitic stainless steel with a hemispherical bottom head. The top of the vessel is sealed to the shield floor of the refueling hot cell. Access to the fuel during refueling is by removal of a steel and graphite shield plug, which in normal operation is mechanically held in place. The vessel contains the seven cores, fertile blanket, support structure, control rods, blanket and core flow divider plates, and flow distribution gates which are remotely operated to adjust sodium flow between core and blanket regions.

Two pairs of concentric nozzles are located in a plane above the core. Coolant enters the vessel through the annular space of each nozzle pair and flows downward along the vessel wall, to the flow divider plates, through

the core flow distribution gates, then turns at the bottom and flows upward through the core and blanket assemblies to the outlet nozzles.

Concentric inlet and outlet nozzles are employed to reduce shield penetrations and to simplify the layout of the primary sodium system. There is normally 11 feet of sodium above the center line of the vessel nozzles to assure that the core will not become uncovered if the vessel ruptures. It also provides adequate pump NPSH (net positive suction head) during step load changes. The sodium pool surface and the shield plug are separated by an argon cover gas space which provides expansion volume, minimizes heat losses to the shield plug, and prevents oxidation of the sodium. A sodium over-flow line is located in the argon space to limit sodium expansion.

All vessel connections are above the core so that if a break occurs in the primary sodium system the fuel can never become uncovered. A more detailed description of the reactor vessel is given in Section II.3.1.4.

Intermediate Heat Exchangers

The two intermediate (Na to Na) heat exchangers are of the vertical shell and tube type. Two heat exchangers were chosen to achieve the minimum plant cost consistent with reliability and heat exchanger manufacturing capabilities. A single heat exchanger presents significant manufacturing and shipping problems, while three or four heat exchangers are deemed too costly and not required for plant reliability.

The primary sodium flows through the shell side of the heat exchanger, entering through two diametrically opposed horizontal nozzles and leaving through a single nozzle. Intermediate sodium flows through the tubes.

The shell side contains a free sodium surface covered by argon gas to prevent oxidation and minimize tube sheet thermal stresses. The argon space contains a sodium overflow line to accommodate volume surges.

The thermal center of the heat exchanger is located sufficiently above the center of the core to provide adequate coolant natural circulation after a complete loss of flow accident. The tube sheets and tube bundle may be removed from the shell for inspection or maintenance.

Pumps

The four primary sodium variable speed (two-to-one) circulating pumps are of the free surface type consisting of:

1. A pump drive motor mounted on a shield deck.
2. A pump volute located in the primary sodium system piping below the shield deck.
3. A pump impeller located in the volute and connected to the motor by a shaft.
4. A vertical column between the shield deck and the volute to contain the shaft.

The vertical column contains a sodium pool, argon cover gas chamber, gas shaft seal and other necessary pump components.

Westinghouse studies of liquid sodium pumps indicate that three or four pumps are desirable due to the questionable reliability record of units which have been demonstrated to date.* Four pumps, each having 80,000 GPM capacity, are therefore specified to pump sodium at the required rate to the two intermediate heat exchangers. The use of four pumps permits a large

*Refer to AEC Contract AT(30-1)-3123

partial load rating in the event of the loss of a single pump. With four pumps and two heat exchangers, the hot leg piping in each loop is divided to deliver sodium to the suction nozzles of the two pumps contained in a loop. Each pump delivers flow to the intermediate heat exchanger.

The pump volute contains one suction and one discharge nozzle. Once inside the volute the sodium flow is divided to provide double suction flow to the impeller. Although a double suction design requires a heavier volute than a single suction pump, this type was selected due to its lower NPSH requirements and smaller diameter guide vane as compared to a single suction pump.

All parts of the pumps in contact with sodium are stainless steel except for the bearings and other special parts. Each pump is equipped with a mechanical device to prevent reverse rotation in the event of loss of power.

Valves

No loop isolation or pump check valves are provided. Loop isolation valves are omitted as power operation is not contemplated if both pumps in one loop become inoperative. In this event, the plant would be shut down for the necessary repairs. By eliminating shut-off valves, system complexity is reduced and plant economics are improved.

Reduced power operation with one defective pump is possible. With proper piping layout and a locked pump rotor, approximately 70% power operation is possible without pump check valves. Since pressure surge problems associated with check valve slamming are eliminated, reduced power operation without shutdown is possible. Plant economics and reliability are both improved by check valve elimination.

Insulation and Heat Tracing

All equipment that contains, or can contain, sodium is either electrically heat-traced or gas-heated to prevent sodium solidification. Selection of ultimate heating system will be based on detailed engineering and economic analysis. The sodium piping and equipment are covered with insulation to minimize heat losses.

Instrumentation

The primary sodium system instrumentation provides the required signals for reliable, safe, and efficient operation and control of the system. The following is a functional description of the system instrumentation and control equipment. Refer to Section II.2.4.6 for details of the reactor control system.

Temperatures

Loop Temperatures

Temperature detectors in the hot and cold leg of each loop provide signals for primary sodium system control during startup, shutdown, and normal operation. The temperature signals are used by the reactor control and protection system for control of system temperature and are recorded in the main control room.

Sodium Level

The reactor vessel is equipped with a sodium level detector. A low or high sodium level alarm is sent to the reactor control and protection system to scram the reactor. Sodium level is recorded and alarm indicators are located in the main control room.

Primary Sodium Flow

Flow rates are measured in each primary sodium loop cold leg. A low flow signal in any loop actuates an alarm in the main control room. The low flow alarm is sent to the reactor control and protection system, which initiates a low flow scram if flow falls below a predetermined value.

II.3.1.1.3 Design Basis

Primary Sodium Loops

The primary sodium loops include the reactor vessel, pumps, intermediate heat exchangers and connecting piping. They are designed to transfer a total of 8.53×10^9 Btu/hr (2500 MW) from the reactor core to the intermediate heat exchanger.

The equipment is capable of withstanding the transient thermal and pressure stresses due to the transient conditions defined in this section. These transient conditions are conservative estimates for equipment design purposes only, and are not to be construed as representations of actual transients or as reflections on actual operating procedures.

<u>Transient Condition</u>	<u>Occurrences</u>
a. Plant heatup at $*100^{\circ}\text{F}$ per hour	240
b. Plant cooldown at $*100^{\circ}\text{F}$ per hour	240
c. Plant loading at 5 per cent of full power per minute	3000
d. Plant unloading at 5 per cent of full power per minute	3000
e. Step load increase of 10 per cent of full power but not to exceed full power	240

* Subject to confirmation by equipment manufacturers.

<u>Transient Condition</u>	<u>Occurrences</u>
f. Step load decrease of 10 per cent of full power	240
g. Reactor scrams from full power	160
h. Loss of coolant flow followed by natural circulation	160

Intermediate Heat Exchangers

Each intermediate heat exchanger is designed to transfer one-half of the heat produced during power operation. Design data are:

Heat Duty	4.265×10^9 Btu/hr
Primary Sodium Flow/Unit	6.4×10^7 lb/hr
Primary Sodium Temperatures	
In	1200°F
Out	980°F
Pressure Drop (shell side)	10 psi (maximum)
Design Pressure	150 psig
Design Temperature	1250°F
Material	Austenitic stainless steel
Code Requirements	ASME Section VIII and Nuclear Code Cases

Primary Sodium Circulating Pumps

Each pump is designed to circulate one-fourth of the required total sodium flowrate. Four pumps are required for reliability, to satisfy net positive suction head requirements, and to provide for continued part load operation in the event of a single pump outage.

Design data are:

Pump Type	Vertical centrifugal free surface
Design Flow	80,000 gpm
Drive	Variable speed motor (2/1 from 50 to 100% power)
Fluid Pumped	1200°F sodium
Volute	
Material	Austenitic stainless steel
Nozzles	Single suction and discharge
Impeller	
Material	Austenitic stainless steel
Suction	Double
Discharge	Single
NPSH Available	45 feet
RPM	~ 700
Total Dynamic Head	~ 328 feet
Brake Horsepower	~ 5500

The system is designed to keep the total pressure drop within the maximum practical pump TDH. At the present time, single stage sodium pumps appear limited to a TDH of approximately 350 feet. The system is therefore designed for the following pressure drops:

System Piping	20 psi
Intermediate Heat Exchanger	10 psi
Reactor Vessel	5 psi flow control valves 18 psi internals
Orifice and End Effects	24 psi core, average channel
Inlet to Outlet	<u>36</u> psi core, average channel
Total	113 psi ~ 328 feet

Hot Trap

Hot traps are provided to reduce and maintain sodium oxide concentrations at 10 ppm or less and carbon to low levels. They are not utilized until the cold traps have reduced the sodium oxide concentration to a plugging temperature of approximately 250°F.

Type	Zirconium getter
Fluid	1200°F sodium
Volume	50 ft ³
Design Pressure	150 psig
Design Temperature	1300°F
Material	Austenitic stainless steel
Code	ASME Section VIII and Nuclear Code Cases

Cold Trap

Cold traps are provided to reduce the primary sodium oxide concentration to a plugging temperature of approximately 250°F within a few days of plant startup. Design data are:

Oxide capacity (each)	1000 pounds
Design Temperature	1250°F
Design Pressure	150 psi
Operating Temperatures of the Regenerative Heat Exchanger:	
Tube Side	
In	980°F
Out	400°F
Shell Side	
In	250°F
Out	830°F

Cold Trap

In

400°F

Out

250°F (minimum)

II.3.1.2 Cover Gas System

II.3.1.2.1 General Description

The cover gas system serves the general function of providing a protective inert atmosphere for the primary sodium coolant. In addition, it serves to control primary system pressure, to provide a collection volume for fission product gases (released from the vented fuel), to cool selected reactor shields, to continuously purge the control rod drives, and to provide ventilation and cooling services for the hot cell fuel handling system.

The cover gas system is shown in Figure II.3.1-2. The system consists of: a) a makeup pure argon supply system; b) a regulated supply to the reactor vessel, Na pumps and IHX's; c) a pressure relief system from the primary components; d) a controlled sweep gas subsystem for the control rod drives; e) a recirculating shield cooling and fuel handling cell loop; and f) a "hold-up" storage system for radioactive gas.

The reactor vessel argon cover gas is essentially static. Gas pressure is controlled at approximately 1 psig by introducing purified argon through a regulator system, or by releasing excess gas through relief valves to the gas holder for contaminated gas storage. A small side-loop circulates argon from the reactor vessel through a cooled sodium vapor trap to the control rod drive housings. The argon stream flows down through the housings and thus prevents sodium precipitation on the rack-and-pinion drive assemblies.

During normal plant operation, the primary reactor shield and reactor vessel shield plug are cooled by circulation of argon. Heat exchangers and compressors are provided for this service. Since the reactor shield contains graphite which releases CO, CO₂, H₂, N₂, and CH_x gases, the shield cooling is isolated from free sodium surfaces of the primary system. It is, however, deemed desirable to use this same compressor and heat exchanger system for hot cell and fuel handling machine cooling during refueling operations. This is safely accomplished by valving-off the shield cooling circuits which are not required during plant shut-down and by purging the loop equipment of contaminated gas before tie-in to the hot cell and fuel handling manipulator. In this manner the equipment is used more efficiently and primary sodium purity is assured.

A gas holder is used to store gas purged from the cover gas system. Gas is held in this tank to permit decay of argon 41 and fission gases. Argon 41 is produced from an $A^{40}(n, \gamma)A^{41}$ reaction in the reactor vessel cover gas (argon) and in the graphite shield cooling argon. By far the bulk of the argon 41 is produced in the reactor vessel cover gas. Large amounts of fission gases are expected to be present in the reactor vessel gas due to the use of vented fuel elements. This type of fuel element design potentially releases all gaseous fission products to the coolant. Based on existing data and SRE experience, all halogen fission gases released from the fuel are assumed to react chemically with the coolant (e.g., $I_2 \uparrow + 2Na \rightarrow 2NaI \downarrow$) and therefore are not released to the cover gas. All xenon and krypton isotopes are assumed to be released to the cover gas.

Access to the hot cell is required for maintenance and inspection prior to and during refueling. Therefore, at the end of each core cycle the reactor vessel cover gas is purged to the gas holder prior to removal of the shield plug. To minimize the quantity of argon required for purging, the reactor vessel sodium level is raised until only 10% of

Figure II.3.1-2 Cover Gas System

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the vessel cover gas remains. The remaining cover gas volume is purged with clean argon approximately 13 times in order to reduce the gaseous radioactivity to an acceptable level. Preliminary calculations indicate the following dose rates due to gaseous radioactivity in the hot cell 12 hours after plant shut down and after the shield plug has been removed:

<u>Isotope</u>	<u>Dose Rate (mr/hr)</u>
Xe-133	24.7
Xe-135	~0.3
Kr-85	3.5
A-41	<u>~0.02</u>
Total	28.5

These dose rates will permit reasonable periods of access to the hot cell.

After refueling, Xe-133 and Kr-85 will remain in the hot cell in small quantities. Approximately 0.5 curies of Xe-133 and 1.1 curies of Kr-85 are present in the hot cell during refueling. This amount of radioactivity can be dispersed safely by purging the hot cell with clean argon or nitrogen.

After decay, the contents of the gas holder (predominantly Kr-85) are gradually released to the ventilation system stack. The amount of decay time required is dependent on the meteorological conditions existing at the plant site. At most sites, sixty to ninety days decay will provide adequate reduction in the Xe-133 activity for safe discharge to the environment. The required height of the ventilation system stack will be determined by the quantity of Kr-85 ($\sim 1.4 \times 10^6$ curies/yr) to be released and by site conditions.

II.3.1.2.2 Description of Equipment

Components

Control Rod Drive Purge Compressors

Two compressors are supplied. One is a spare and is on standby service.

These compressors are the single stage, low pressure diaphragm type and consist of a flexible metal diaphragm clamped between two circular plates. The lower plate has a concave surface and is perforated by a number of holes which allow a fluid to circulate in the space between the lower plate and the diaphragm. A piston reciprocates in the fluid to flex the diaphragm, causing gas to be alternately drawn into and compressed and discharged from the space between the diaphragm and the upper plate. A small receiver is installed in the discharge of each compressor to contain the hydraulic fluid if a diaphragm ruptures. Each compressor is constructed of carbon steel and is operated by a belt and electric motor.

Cover Gas Compressors

Two compressors are supplied. One is a spare and is on standby service.

These compressors are of the intermeshing screw rotor type. They consist of dual screw type rotors rotating in opposite directions. As they rotate within a housing, argon becomes trapped between them and is compressed and discharged from the housing. No lubrication system is required for impeller shaft sleeve or roller bearings. Each shaft is sealed to the housing with a labyrinth vented to the suction by a lantern ring to reduce seal pressure requirements and minimize seal leakage. Each compressor is constructed of carbon steel and cast iron and is operated by an individual electric motor.

Supply Tank

The supply tank is designed to receive, contain, and release purified argon gas to the cover gas system. It supplies argon to this system for purging and leakage makeup.

The cylindrical all-welded tank is constructed of carbon steel, and is designed to contain 20% of the normal cover gas volume at 50 psi to assure an adequate controllable argon supply.

Gas Holder

The gas holder is a carbon steel sphere containing a collapsible fabric reinforced synthetic rubber diaphragm. The diaphragm is sized to contain three reactor vessel cover gas volumes and is totally enclosed in the sphere. The space between the diaphragm and the sphere is vented to the stack through monitors and automatic valves.

Cover Gas Heat Exchanger

Two heat exchangers are supplied. Because there is a plant cooling requirement during power operation and during refueling, a spare heat exchanger is supplied to permit heat exchanger maintenance without requiring plant shut down.

Each heat exchanger is of the straight radially-finned tube type with a baffled carbon steel shell. The tubes are welded to the tube sheets. Channels are flanged and bolted to permit tube cleaning. Coolant flows on the tube side and argon on the shell side. A receiver is installed downstream of the heat exchangers to collect coolant in the event of leakage.

Sodium Traps

Two sodium traps are supplied to permit sodium drainage from one trap while maintaining continuous purging or recirculation of reactor vessel cover gas.

Each trap consists of multiple, replaceable, short concentric-pipe heat exchangers. The inner pipe has radial fins of alternating heights and contains nitrogen gas coolant. The coolant is obtained from, and returned to, one of the primary sodium equipment cubicles. The heat exchanger annulus contains reactor vessel blanket argon. Each heat exchanger is contained in an electrically heated mantle to periodically melt solidified sodium trapped between and on the fins. The exchanger's longitudinal axis is inclined 3° to ensure drainage of molten sodium.

Instrumentation

The cover gas system instrumentation provides the required signals for reliable, safe, and efficient operation and control of the system. The following is a functional description of the system instrumentation and control.

Temperatures

A temperature detector is located on the discharge of the cover gas heat exchanger. Temperature is indicated in the main control room and is used by the operator to evaluate and control operation of the heat exchanger.

Level

A level detector is located in each receiver. Individual level alarms in the main control room warn of fluid collection in the receivers.

Flow

Argon flow detectors are located in each of the following locations:

- a) Downstream of the shield plug, primary shield, hot cell tunnel and atmosphere, reactor vessel gas blanket, and the cover gas compressors.
- b) Upstream of the gas holder.

Individual flow indicators are located in the main control room for operational control.

Pressure

A pressure detector is located on the discharge side of each compressor. A low pressure signal from an operating compressor automatically starts the standby compressor and actuates an alarm in the main control room. Additional pressure detectors, indicators and controllers are included in the system to aid operational control.

Conductivity

A conductivity detector is located in the argon discharge line from each cover gas heat exchanger to detect leakage of heat exchanger coolant. The conductivity is indicated and alarms are located in the main control room.

II.3.1.2.3 Design Basis

Control Rod Drive Purge Loop

A small separate control rod drive argon purge loop is provided to prevent significant radioactive contamination of the large cover gas loop. This small loop circulates argon at 28 SCFM from the reactor vessel blanket gas to the control rod drive housings and returns it to the blanket gas. The blanket gas is normally isolated from the cover gas loop and contains large amounts of radioactivity. Preliminary calculations indicate the following radioactive gaseous production rates in the blanket gas:

Xenon 133	17.6 curies/sec
Xenon 135	2.4 curies/sec
Krypton 85	0.046 curies/sec
Argon 41	10.0 curies/sec

Since the control rod drive purge must exhaust to the reactor vessel gas blanket, a small closed loop was chosen to prevent the spread of gaseous radioactivity.

Cover Gas Loop

A large cover gas loop with multiple parallel flow paths was chosen to circulate argon to the desired areas for maintaining an inert atmosphere and to provide cooling for various components. The loop contains compressors, heat exchangers, vapor traps, and argon makeup equipment. They are designed to remove approximately 2.75×10^5 Btu/hr at 3250 SCFM argon. The argon flowrate is based on a minimum argon temperature of 200°F and a maximum concrete temperature of 300°F in the region of the primary shield. Nitrogen is a possible alternate gas. Heat removal rate is sufficient to:

- a) Maintain the primary shield concrete temperature at or below 300°F during power operation.

- b) Prevent fuel element clad damage while transferring a spent fuel element from the reactor vessel to the spent fuel pit during refueling.

Cover Gas Compressors

Each compressor is designed to circulate and compress all of the required argon flowrate. Design data are:

Type	screw rotors compressing axially
Design flow	3250 SCFM
Fluid	argon gas
Design temperature	350°F
Design pressure	15 psig
Drive	gear drive directly connected to electric motor
RPM	1765
TDH	8 psi at 300°F
Suction pressure	essentially atmospheric at 300°F
Brake horsepower	250
Materials	rotors and casing: cast iron
Lubrication	gases are pressure lubricated with a self-contained oil pump and filter
Shaft seals	labyrinth with leakoff back to suction for argon, and a mechanical seal to keep oil out of compressor chamber.

The compressor TDH is based on the following system pressure drops:

Cover gas heat exchangers	2 psi
System piping	1 psi
Graphite primary shield	5 psi

Control Rod Drive Purge Compressors

Each compressor is designed to circulate and compress all of the required

argon flow rate. Design data are:

Type	Diaphragm
Design flow	28 SCFM (1 SCFM per rod drive assembly)
Fluid	Argon gas
Design temperature	300°F
Design pressure	10 psig
Drive	Belt and electric motor
TDH	3 psi at 200°F
Suction pressure	1 to 5 psig at 200°F
Materials	Stainless steel diaphragm Carbon steel and cast iron compressor head
Lubrication	Self-contained oil bath

The compressor TDH is based on an estimated pressure drop through system piping, rod drive assembly, and sodium trap.

Cover Gas Heat Exchangers

Each heat exchanger is designed to be individually capable of removing all of the heat load. Design data are:

Type	Counter flow, horizontal, straight finned, tubed, baffled shell
Fluid	
tube side	Tetrelin
shell side	Argon gas
Design temperature	350°F
Design pressure	
tube side	50 psig
shell side	10 psig
Pressure drop	
tube side	5 psi
shell side	2 psi maximum
Heat duty	2.75×10^5 Btu/hr
Design flow	
tube side	100 GPM
shell side	3250 SCFM

Temperature drops	
tube side	
in	80°F
out	130°F
shell side	
in	300°F
out	200°F
Material	Carbon steel

Supply Tank

Internal volume	20% of cover gas system
Design pressure	75 psig
Design temperature	150°F
Normal operating pressure	50 psig
Material	Carbon steel
Construction	All welded

Gas Holder

Internal volume	
sphere	1100 ft ³
diaphragm	1000 ft ³
Design pressure	
sphere & diaphragm	Atmospheric
Design temperature	
sphere & diaphragm	100°F
Materials	
sphere	Carbon steel
diaphragm	Fabric reinforced Synthetic rubber

Sodium Traps

Type	Parallel, radially-finned pipe-in-pipe
Installation slope	2 to 1 horizontal

Design pressure	10 psig
Design temperature	350°F
Heat duty	1.8 x 10 ⁴ Btu/hr
Design flowrate	
inner pipes	112 SCFM nitrogen
outer pipes	30 SCFM argon
Temperature drops	
inner pipe	
in	100°F
out	250°F
outer pipe	
in	800°F
out	200°F
Heater	Electric mantle

II. 3.1.3 Radiation Shielding

The radiation shields provided in the FBR are designed to attenuate radiation originating in the reactor core, structure, and coolant to levels low enough to prevent damage to structures, equipment or instrumentation and to limit the radiation exposure of operating personnel to permissible levels. There are three distinct shields in the FBR: the reactor shield; the refueling shield; and the secondary shield.

II. 3.1.3.1 Reactor Shield

Function

The reactor shield, surrounding the reactor core, attenuates the neutron flux leaking from the core to low levels outside the shield. The design level is set at a total flux of no more than 10^4 n/cm²-sec to prevent significant activation of the secondary coolant or of the stainless steel heat exchange equipment. With such a neutron field around the primary heat exchangers, the activity of the secondary coolant is estimated to be less than 2×10^{-4} μ c/cc.

This attenuation of neutron flux is unavoidably attended by the emission of gamma radiation. The reactor shield also attenuates this gamma radiation and disposes of the heat which is released at temperature levels that will not damage the reactor, structures, or shields.

Description

There are two distinctly separate portions of the reactor shield: the radial shield; and the axial shield plug.

Radial Shield

The radial shield is illustrated as shown later in Figure II.3.1-5. The primary constituent of the shield is graphite containing 3% natural boron by weight. This boronated graphite shield is 36 inches thick and is centered

on the reactor core, extending 48 inches above and below the core. The remainder of the radial graphite shield is not boronated.

Since the reactor core and blanket contain very little moderating material, neutron leakage from the blanket is primarily in the form of fast neutrons with energies of 5 kev to 1 Mev. As can be seen in the plot of neutron flux versus shield depth (Figure II.3.1-3) there is some neutron slowing down in the reflector and sodium downcomer, but mostly at the energies of the fastest neutrons, those with energies above 1 Mev. Therefore, the leakage from the reactor is primarily that of neutrons with energies in the kev energy range. The graphite shield quickly thermalizes this leakage flux, bringing it into equilibrium with the fastest group at a depth of 2 feet within the shield.

The addition of boron in the graphite increases the thermal capture cross section of the shield from $.00022 \text{ cm}^{-1}$ to 1.44 cm^{-1} with the result that the thermal neutrons are absorbed as quickly as they are born in the graphite. Since the absorption reaction in boron is (n, α) , the attendant energy release is small and takes place within the graphite. The resulting heat is carried away by a flow of argon gas through the graphite.

The $2\frac{1}{2}$ inch thick support cylinder outside the vessel functions as a gamma shield, absorbing capture gamma radiation originating within the blanket and the two reactor tank walls. Heat released within this shield is dumped to the flow of inert gas in the graphite.

The final layer of the reactor shield is comprised of 48 inches of ordinary concrete. Thermal flux peaks within the concrete because the fast flux is moderated faster than thermal neutrons are absorbed. However, the peak is only one decade higher than the flux at the inner surface of the concrete, or $10^5 \text{ n/cm}^2\text{-sec}$. Use of 48 inches of concrete results in a smaller leakage flux than the design value, but this conservatism is desirable because of the uncertainties introduced by diffusion theory at large penetrations of absorbers. Heat generation in the concrete due to the absorption of radiation is calculated to be less than 5 Btu/hr-ft^2 of inside surface area.

Figure II.3.1-3 Neutron Flux vs. Shield Depth (Radial)

This small amount of heat can be carried off without an undue temperature rise within the concrete.

Axial Shield Plug

The axial shield plug is illustrated in Figure II.3.1-4. As in the radial shield, the neutron shielding is provided by 3% boronated graphite in 2 layers for a total thickness of 24 inches. The flux incident on this shield contains a good many neutrons of thermal energies, as the thick layer of sodium above the core moderates many of the fast neutrons leaking from the core. The first layer of the shield plug is a structural member, a 2-inch thick stainless steel plate. This is followed by the first layer of boronated graphite, 2 inches of stainless steel, 4 inches of carbon steel, another 12 inches of boronated graphite, and, finally a 4-inch thick carbon steel plate.

Both stainless plates are structural members that support and align the control drive shafts. Because these plates must be in alignment with the hot core structure, no direct cooling is provided for this portion of the shield. This ensures it will function at a higher temperature than the argon sweep gas below it.

The axial shield plug not only attenuates neutron flux during power operation, but also serves to shield any personnel in the refueling hot cell from gamma radiation originating in the core and coolant after shutdown. Use of the plug illustrated will reduce the dose rate under these conditions (8 hours after shutdown) to less than 15 mr/hr. The shield plug does not fully shield the reactor at power, since the gamma flux on the upper surface of the shield is 10 R/hr at full power. However, the walls and ceiling of the hot cell (see "Refueling Shield" below) provide the required attenuation.

Method of Calculation

The neutron fluxes were calculated by the PLMG multi-group transport approximation code. The fluxes plotted in Figures II.3.1-3 and II.3.1-4 are the

Figure II.3.1-4 Neutron Flux vs. Shield Depth (Axial)

absolute fluxes as obtained by multiplying the normalized flux output by the normalizing factor to account for a critical reactor producing the proper thermal power. The core input parameters were taken from the physics data and the cross sections were taken from the physics MABI calculations.

Core gamma and capture gamma sources were hand calculated from the PLMG fluxes. The gamma currents were calculated by hand.

II.3.1.3.2 Refueling Shield

Function

The refueling shield protects the refueling crew from radiation from three sources:

- 1) Fission products in the irradiated fuel assemblies.
- 2) The pool of activated coolant in the reactor vessel.
- 3) The gaseous fission products in the atmosphere of the hot cell.

Because of its location, this shield also serves to attenuate gamma fluxes from the shield plug during full power operation.

Description

The shield consists of the walls and ceiling of the refueling hot cell. It is comprised of 72 inches of ordinary concrete in the walls, and at least 24 inches of concrete in the ceiling of the hot cell to limit the dose rate on the outside surface of the shield to 7.5 mr/hr during fuel transfer operations.

II.3.1.3.3 Secondary Shield

Function

The secondary shield is designed to attenuate the radiation from the Na-24 activation product in the reactor coolant loop components to levels low enough to permit continuous access immediately outside the shield during full power operation. This shield also attenuates the neutron flux leaking from the reactor to levels which will allow continuous access.

Description

The secondary shield consists of 57 inches of ordinary concrete surrounding the reactor coolant loop components (pumps, primary heat exchangers, cold traps, etc.). The gamma energy current incident upon this shield from the activated reactor coolant is calculated to be less than 5 Btu/hr-ft². The concrete can dissipate this energy to ambient condition without provision for special cooling.

II. 3.1.4 Reactor Vessel and Shield

II. 3.1.4.1 Reactor Vessel

Function

The reactor vessel is designed for the following basic functions:

1. To provide structural support for the reactor cores and blanket fuel assemblies.
2. To provide primary containment for the liquid sodium coolant pumped through the fuel assemblies.
3. To provide distribution of the sodium flow to the active core and blanket assembly sections of the fuel matrix.

Description

The reactor vessel assembly with shield plug, supports and shielding is shown in Figure II.3.1-5.

The reactor vessel is basically a vertically oriented cylindrical tank of 18 ft diameter closed at the lower end by an elliptical head. The height of this section of the tank is $26\frac{1}{2}$ ft. Within this tank there is another concentric cylinder of approximate 15 ft diameter extending $21\frac{1}{2}$ ft in length down into the outer tank. The upper end of the outer vessel is formed by a vertical extension of the inner cylinder to a height $6\frac{1}{2}$ ft above the outer tank. This upper end of the vessel is open and flanged for receiving and supporting the shield plug closure.

Two sets of horizontally oriented concentric inlet and outlet nozzles are located 180 degrees opposed at a level 13 ft below the top flange of the vessel. The outer nozzle of 5 ft 8 in. inside diameter is attached to the outer tank cylinder, and the inner nozzle of 4 ft 5 in. outside diameter is attached to the inner cylinder. Together they form an annular inlet channel to the vessel. The vessel's outer and inner cylinder correspondingly form an annular inlet plenum chamber. Continuation of this double cylinder below the plenum forms the annular downcomer section of the vessel. In the downcomer, radial ribs between the inner and outer cylinders divide the annular space into separate flow channels. There are twelve such channels, eight of which convey the sodium flow to the 7 active fuel cores. The remaining four channels are united in the lower head to form a common chamber that conveys the sodium to the blanket fuel assemblies. In addition to providing an inlet plenum for flow distribution control, the geometric arrangement of the double concentric wall vessel offers the advantage of vessel design having an outer containment that will operate at the lowest primary coolant inlet temperature (980°F), rather than the high outlet temperature of 1200°F .

The active fuel cores and blanket fuel elements are supported in a circular disk type support structure. The core plate is held in place by bolting to a support ring ledge that forms an integral part of the vessel inner wall at its extreme lower end.

Figure II.3.1-5 Reactor Vessel Assembly

All the sodium flow rising from the core and blanket assemblies is mixed in the common chamber above the assemblies as formed by the inner cylinder of the vessel. This outlet chamber is concentric to, and is inside of, the inlet plenum described earlier. To promote thorough mixing of the sodium before it leaves the vessel, the chamber is enlarged to extend to a height of 16 ft. above the fuel assemblies. Outlet nozzles from this chamber are located on a center line 4 ft. above the fuel assemblies. An inert argon gas blanket at pressures in the range of 1 to 2 psig is maintained above the sodium level in this chamber. Normal operating sodium level is 14 ft. above the fuel assemblies.

The vessel support structure is an integral box girder design formed by the top closure plate of the 18-ft. outer tank and another circular ring plate between the two cylinders approximately 2 ft. below. The load of the fuel assemblies is transmitted to this box girder section of the tank by the inner and outer vessel walls. The inner cylinder also transmits the load of the shield plug to the girder.

The entire vessel and shield plug load is transmitted by fourteen support lugs attached to the outside diameter of the girder section of the vessel. The level of the support feet is 54 inches above the nozzle center line.

The entire inner wall cylinder and outlet nozzles are provided with two thermal barriers that maintain the inner vessel cylinder temperature in close proximity with the temperature of the remainder of the vessel. The vessel operating metal temperature is essentially that of the sodium inlet temperature of 979°F. The first thermal barrier is a stagnant argon gas-filled annular space between the inner vessel cylinder and a gas tight liner. The second thermal barrier is formed by several concentric closely spaced thin cylinders internal to the argon gas liner cylinder.

Each of the four downcomer channels that conduct the sodium flow to the blanket assemblies is provided with a flow control device to permit control of the flow distribution between core and blanket over the entire fuel lifetime. The device is a simple flat plate gate guided by side plates. Each downcomer has two bypass sections on either side of the gate so that flow is always available to the blanket assemblies even if the gates are fully closed. The seals for the drive stems are incorporated in a cylindrical housing welded to the top of the support grider section of the vessel. Freeze or bellows seals, with gas seal and packing gland backup will be employed to contain any possible leakage. The drive unit is completely canned (enclosed) and accessible for removal, replacement and/or repair from the refueling floor level. Flow control gates can be provided for each of the seven active cores should they be required to achieve good distribution between cores.

Basis of Design

The vessel will be designed, fabricated, inspected and tested in accordance with ASME Boiler and Pressure Vessel Code, Section VIII Unfired Pressure Vessels. The vessel will also meet the requirements of Special Nuclear Code Case rulings No. 1270 N and 1273 N. Under special ruling 1270 N the Reactor Vessel is classified as a primary vessel.

In addition, the reactor vessel will be capable of withstanding throughout its design life all the steady state and transient operating conditions. The calculation of nozzle stress, thermal stress and fatigue will be performed in accordance with methods and criteria equivalent to those contained in the "Tentative Structural Design Basis for Reactor Pressure Vessels and Directly Associated Components" document PB-151987 (Published by the U.S. Department of Commerce, Office of Technical Services).

II.3.1.4.2 Shield Plug

Function

The shield plug is designed to perform the following basic functions:

1. Provide a structural closure for the top of the reactor vessel that is removal as a single unit for refueling operations.
2. Provide a structural member for support and alignment of the control rod drive mechanisms and shafts.
3. Provide shielding to attenuate neutron flux during power operation and to serve as a gamma shield after shutdown for personnel in the hot cell above the vessel.

Description

The shield plug is a cylindrical, single-stepped plug of 16-1/2 ft. maximum diameter and 14-1/2 ft. stepped diameter. Total shield thickness is 3 ft. and is made up of the following material from bottom to top: two inches stainless steel; 12 inches of stacked 3% boronated graphite block; 2 inches of stainless steel; 4 inches of carbon steel; 12 inches of boronated graphite block; and 4 inches of carbon steel.

Two stainless plates form the main structural members that support and align the control rod drives and shafts. The section between the two stainless plates is ribbed and stiffened to perform its load carrying and alignment functions. The upper carbon steel contained section of the shield will be self-supporting and cooled internally by argon gas circulation.

The shield plug is supported at its stepped diameter on the reactor vessel flange. This flange is provided with double concentric "O" ring seals, with leakage indication taps between the seal rings. The plug is bolted to the flange for positive sealing and alignment with the vessel. A back up freeze seal employing a low melting point alloy (Cerrobend) can be provided should the results of the development program show that it is preferable.

Suspended from the lower side of the shield plug is a vortex breaker assembly which operates partially submerged in the liquid sodium coolant. This vortex breaker segregates the free surface tending to dampen hydraulic action that would entrain cover gas in the molten sodium. The assembly also contains guides for the control rods.

II.3.1.4.3 Support Cylinder and Radial Shield

Function

The support cylinder performs the following two basic functions:

1. Provides support for the reactor vessel and shield plug.
2. Provides gamma shielding for the surrounding concrete.

Description

The support cylinder takes the load of the reactor vessel from the 14 support lugs and transmits it vertically downward to the lower support concrete. The cylinder carbon steel has a maximum outside diameter of 20 ft. and is 20 ft. in height. Its thickness of 2-1/2 inches is based on gamma shield requirements and is more than sufficient to accommodate the entire reactor weight loading.

Continuity of the radial shielding function requires that another cylinder be placed directly above this support cylinder and extending to the shield plug level. This radial gamma shield will be of the same thickness as the support cylinder and approximately 7-1/2 feet in height.

The radial shield is completed by a 3 ft. thickness of 3% boronated graphite blocks stacked outside the support cylinder. There are only two exceptions:

1. At the level between center line of the reactor vessel nozzles and the shield plug area, plain carbon block is used.
2. At the level above the reactor vessel support girder, the location of the carbon block is shifted inward to a position inside the 2-1/2" thick steel gamma shield.

The entire radial shield and support is provided with a flow of inert gas that prevents entry of oxygen and removes heat generated in the shielding materials. To accommodate this function, all the stacked carbon block is grooved and/or parted to create adequate gas circulation paths.

II.3.1.4.4 Shield Tank

Function

The shield tank acts as secondary containment of the sodium coolant in the unlikely event of leakage from the reactor vessel.

Description

The reactor vessel and all the shielding previously mentioned is contained within a carbon steel lined concrete cavity. The amount of

void space within the shield tank enclosure, external to the reactor vessel thermal insulation can, is sufficiently small that the total coolant level will not drop below the level of the center line of the reactor vessel nozzles should a leak develop anywhere in the reactor vessel.

This requirement ensures continued circulation of sodium with pump operation or by natural convection; thus providing cooling for the core in the unlikely event of such an accident.

II.3.1.4.5 General Data

Reactor Vessel

Height	33-1/2 ft.
Diameter, Outer	18 ft.
Diameter, Inner	15 ft.
Design Pressure	100 psig
Design Temperature	1100°F
Basic Wall Thickness	1-1/2 inches
Material	Type 304 stainless steel

Shield Plug

Maximum Diameter	16-1/2 ft.
Minimum Diameter	14-1/2 ft.
Thickness	3 ft.
Design Temperature	1100°F
Materials	Borated graphite, carbon and stainless steels

Support Cylinder

Diameter	20 ft.
Height	30 ft.
Thickness	2-1/2 inches
Material	Carbon steel

Radial Shield and Shield Tank

Outside Diameter	26 ft.
Nominal Height	35 ft.
Materials, Thickness	Borated graphite, 3-ft. carbon steel, 1/4 inch.

II.3.1.5 Fuel Handling System

II.3.1.5.1 Concept

The reactor is refueled by remotely controlled machines operating in a shielded, inert atmosphere cell. Two machines -- the shield plug crane and the refueling machine -- accomplish all the fuel handling operations, from the introduction of new fuel assemblies into the cell to the final loading of the spent fuel into the transfer cask (Figures II.3.1-6 and II.3.1-7). In addition to handling fuel, the refueling machine also removes and replaces reflector elements, control rods, and control rod drive shaft assemblies. All core components have lifting fixtures identical to the fuel.

A remote, gas shielded, "hot cell" refueling system was chosen in preference to the following two alternatives: 1) a lead cask containing an inert gas; or 2) a system employing a remote mechanism submerged in sodium. The "hot cell" system offers the following advantages:

1. Mechanisms operating under sodium are kept to a minimum, improving reliability and service life. Only the gripper mechanism is operated intermittantly in sodium. No part of the machine is exposed to sodium over 300°F.
2. Position indexing motions are rectilinear and have two separate systems to ensure reliability. The rotational indexing arms required by submerged systems are avoided; thus increasing reliability and precision.
3. No gas seals are necessary between the refueling machine and reactor, as is the case with a cask type machine. With both the reactor and the spent fuel pit open at all times during refueling, transfer time between the two is substantially reduced.

Figure II.3.1-6 Hot Cell Elevation

Figure II.3.1-7 Hot Cell (Plan & Elevation)

4. The reactor top plug design is simplified since it is not used as part of the refueling system. Rotational indexing and refueling ports are not required.
5. The fuel is handled in the open over pools of sodium. The fuel can be quickly immersed in the sodium should it become overheated. Thus the major hazard of cask type machines -- an accident with the fuel trapped in the cask -- is eliminated.
6. Locating the spent fuel storage pit within the range of the refueling machine eliminates the need for auxiliary handling equipment, such as transfer rotors and cask cars. It also permits return of fuel to the reactor after temporary storage.

II.3.1.5.2 Equipment Description

Cell Structures

The hot cell is a gas-tight structure enclosed by 6 ft. thick concrete shield walls lined with stainless steel. The floor and ceiling are also lined with stainless steel. The interior is 9⁴ ft. long and 33-1/2 ft. wide. Periscopes and lead glass windows are provided in the walls and ceiling for viewing the refueling operation.

One end of the hot cell, over the entry and discharge tubes, can be converted into a maintenance area for the refueling machine. Two rolling steel doors close off the end of the cell; with air admitted to the closed-off area. The space between the doors is filled with inert gas at a pressure slightly above the cell pressure. Normally, this maintenance area will be used only when the reactor and spent fuel pit plugs are in place to protect the core sodium from possible air leakage.

Maintenance inside the hot cell will be performed by personnel wearing breathing apparatus and protective clothing. The cell will not be supplied with air except for major maintenance when the sodium in the reactor vessel and storage pool must be covered or removed. In any case, a waiting period of at least a week must ensue before the cell is entered. This period allows for cleanup of the cell atmosphere and radioactive decay of sodium dripped on the floor during refueling.

The ventilation supply of inert gas into the hot cell during refueling is through a tunnel along one side of the cell. This tunnel is formed by the wall supporting the refueling machine and plug crane, and is covered except for a slot wide enough to pass the end of the refueling machine bridge. Incoming inert gas at approximately 200°F fills the tunnel and spills over the top of the wall into the cell. The bridge-drive machinery of the refueling machine and the hoists of the plug crane are located in the tunnel, as well as the trailing cables and feed rails providing power to the machine. This limits the ambient temperature of this equipment and the radiation exposure, thereby increasing the service life.

Spent Fuel Storage

Spent fuel assemblies and control rods are stored in sodium-filled pits in the cell floor. The fuel and control rods are suspended in rows in six long pits, separated by 2-foot thick concrete walls lined with stainless steel. A top plate covers the pit, with a guide sleeve located above each storage position. The concrete contains boron to ensure a subcritical condition.

Each pit will hold a row of 45 components for a total storage capacity of 270, or about 40% of the core. This provides flexibility in scheduling of refueling and shipping of fuel. Because sodium is used as the coolant, the storage pit can also be utilized for storing partially-spent fuel during maintenance operations.

The storage pits will be filled with sodium only to the extent required for storage. Normally, two pits will store the fuel from a refueling cycle. Decay heat is removed by circulating the sodium through an intermediate sodium-NaK heat exchanger to maintain a temperature of 300°F.

The pit walls are sufficiently high to provide 11.5 ft. of sodium above the fuel for gamma shielding in case the hot cell must be entered for maintenance. If air must be admitted to the hot cell for maintenance performance, the storage pit sodium is protected by inserting seal plugs placed by the refueling machine in the top plate guide sleeves of each storage position. An argon cover gas blanket is maintained under the top plate.

Fuel Shipping and Handling

At the end of the hot cell and beyond the storage pits is the fuel discharge tube. Spent fuel is removed from the storage pits by the refueling machine and lowered through the fuel discharge tube into a transfer cask positioned below the floor. A plug at the upper end of the tube and a bellows flange on the bottom that can be sealed to the cask flange allow the cask to be purged with argon before opening to the hot cell atmosphere. The discharge tube is sufficiently large that all positions in the cask can be filled without moving the cask. Additional shielding, in the form of a lead sleeve surrounding the tube, reduces the radiation level in the lower floor.

The transfer cask contains a molten lead-antimony alloy that serves as a heat transfer medium and also provides shielding above the fuel assemblies. As a result, the cask lid can be installed without radiation hazard. After sealing the cask lid, the cask is shipped to a reprocessing facility.

New fuel is brought into the hot cell through entry tubes located in the floor adjacent to the discharge tube. The entry tubes provide access to

heating chambers on the lower floor. Fuel assemblies are placed in the chambers through side doors, the doors are closed, and the chambers are purged with heated argon. When the fuel assembly has reached the temperature of the reactor sodium, the valve in the top of the chamber is opened and the fuel is extracted by the refueling machine.

Reactor Plug Crane

The reactor plug crane is a remotely controlled bridge crane designed to lift the top plug from the reactor, move it aside for refueling, and replace it after refueling. Four separate hoists are provided to lift the plug; and a bridge drive is provided to move the crane along the rails. No trolley is necessary as the crane is used only for plug insertion or removal.

The control rod drives are lifted integral with the plug after remote disconnection of the drive shafts. Electric cables connected to the drives have enough slack to allow the plug to be lifted and moved without disconnecting the cables.

The crane hoists are permanently connected to the plug top. During reactor operation, the crane is parked directly over the reactor. The lifting cables are rigged through sheaves over to the hoist mechanisms located on the end of the bridge in the equipment tunnel. The four separate hoists are driven by variable speed DC motors, with speed adjustment circuits in the control system that allow the motors to be driven individually or balanced to function together. Each hoist drum has two separate drive units connected to the drum by electric clutches. Power is carried to the hoist and bridge drive through a feed rail in the ventilation corridor. Circuits are provided for each motor so that a complete dual-drive system is available for the hoists, bridge drive and clutches.

When replacing the plug, the reactor plug crane is positioned over the reactor, by driving against the refueling machine which has been parked in a predetermined position through its position indicating system. Level indicating instruments are located directly on the plug top and readings can be checked through a wall periscope. Corrections to level the plug are made by driving the hoists individually.

Refueling Machine

The refueling machine is a remotely controlled rectilinear crane that carries a vertical telescoping mast for reaching down into the reactor. The machine has been specifically designed to operate in a high temperature atmosphere. Fuel assemblies, reflector assemblies, control rods, and control rod guide tubes are removed and replaced by the refueling machine (Figure II.3.1-8).

The bridge and trolley are driven by variable-speed drive units controlled through trailing cables hung in festoons in the equipment tunnel. The bridge drive is also located in the tunnel on the end of the bridge; and the trolley drive is contained within the trolley. Bridge and trolley maximum speeds are about 80 fpm and 20 fpm, respectively. Both bridge and trolley normal drive units are backed up by single speed emergency drives controlled through a feedrail system in the equipment tunnel. Electric clutches engage the main drive when power is on and disengage the emergency drive. Failure of power reverses the engagement for driving through the emergency circuit.

An electrical position repeat-back system on both bridge and trolley indicates the position of the vertical mast in relation to a fixed grid pattern. The outer rows of the storage pit are under the trolley limits of travel at either end of the bridge. These two rows will normally be used when removing spent fuel from the reactor. To conserve time in positioning the trolley, it can be driven directly against shock absorbers

on the bridge ends. The electric position-indicating system is backed up by position markings on the rails. These can be aligned with targets on the crane through the wall periscopes.

The vertical mast of the manipulator crane is made in three telescoping sections, with the outer tube mounted in the center of the trolley. The two inner sections are raised into the fixed section by a hoist connected to the innermost lower section. Guide rollers between the mast sections maintain rigidity and prevent rotation of the mast. A housing at the top of the mast contains the hoist sheave and gripper base reel. The housing also seals the mast to contain the cooling gas supply required in the handling of spent fuel.

The mast hoist has a variable speed drive as well as an emergency drive, and a position repeat-back system similar to the bridge and trolley drives.

A gripper for engaging and lifting the core components is attached to the bottom end of the mast. The mechanism for the gripper is a pneumatic (argon) cylinder, located at the top end of the section to keep it out of the sodium. An operating rod runs the length of the tube to connect the cylinder to the gripper. The gripper is a fail-safe device that remains closed unless the operating cylinder is pressurized; and even then it cannot be opened if a weight is hanging on the fingers. High pressure argon is supplied to the cylinder through a hose trailed behind the machine with the electric cables. A high pressure storage cylinder on the trolley provides an emergency supply of argon that can be cut in by solenoid valves operated through the feed-rail circuits.

Decay heat of the hottest fuel assembly amounts to approximately 54 kw after a 24-hour decay period. At this rate, the fuel can be suspended in the cell atmosphere up to five minutes before it reaches the boiling point of sodium (1620°F). Although this time interval is sufficient to complete a normal

Figure II.3.1-8 Refueling Machine

transfer of the fuel from the reactor sodium to the storage pit, a forced cooling system is provided to extend the time indefinitely. A trailing hose, 3 in. in diameter, supplies 3000 lbs/hr of argon at 200°F and 5 psig to the top of the vertical mast. The gas flows down through the mast, through the fuel assembly and then out to the cell atmosphere. With this system, the fuel equilibrium surface temperature is a maximum of 875°F. The forced cooling system operates continuously. When a fuel assembly is lifted from the reactor pool, it is suspended over the reactor sodium until the cooling system is checked. The fuel lift is then completed and followed by a horizontal transfer to the storage pool.

The machinery inside the trolley cab is cooled by diverting the flow of the cooling gas from the mast into the cab whenever there is no fuel in the gripper. The diverting valve is designed to fail closed so the gas flow will be directed into the mast.

The entire trolley cab and stationary part of the mast are insulated to cut down the heat load within the trolley. Interior temperature and pressure, plus any other non-vital information concerning machine operation are displayed on instruments on the side of the trolley and can be read through a periscope.

The refueling machine and plug crane are operated from a control room outside the cell. A viewing window and closed circuit television is provided for the operator although the information display on the control console will normally be sufficient to operate the equipment without continuous direct viewing. Interlocking circuits are provided to prevent hazardous operation, and alarm circuits warn of abnormal conditions.

The machine can be equipped with automatic drive control circuits and a programmed operating sequence if desired. The added cost would have to be justified by the time thus saved during refueling.

The sequence of fuel handling operations and the estimated time to perform each step are given in Table II.3.1-1. The minimum time required for one-third refueling is about four days from shutdown to startup providing no maintenance operations delay the procedure.

TABLE II.3.1-1

Refueling Sequence and Time Cycle Estimate

	<u>Time</u>
1. Shutdown Reactor	0
2. Cool at 100°F/hr to 400°F	8 hr
3. Purge Reactor Cover Gas	6 hr
4. Remove Shield Plug Fastenings and Disconnect Control Rods	8 hr
5. Remove and Shift Shield Plug	2 hr
6. Disconnect, Remove and Store Control Rod Shaft Guides	3 hr
7. Prepare Manipulator and Storage Pool	<u>3</u> hr
	30 hr subtotal
8. Fuel Handling Operations	
a) Remove Spent Fuel Assembly to Storage Pool	4 min
b) Unlatch and Transfer to New Fuel Position	4 min
c) Pickup New Fuel Assembly and Place in Core	6 min
d) Locate and Grip Next Assembly	<u>5</u> min
Time/Core Position	19 min
For 6 Region Roundelay Core Cycle,	
8 Region Blanket Cycle, ~88 Core Positions	= 28 hr
9. Replace and Connect Control Rod Shaft Guides	4 hr
10. Shift and Replace Shield Plug	3 hr
11. Fasten, Seal and Test Shield Plug	12 hr
12. Connect and Test Control Rods	8 hr
13. Heat Primary Circuit at 100°F/hr to 600°F	3 hr
14. Start Reactor and Bring up to 30% Power	<u>5</u> hr
TOTAL	93 hr

II.3.1.6 Plant Instrumentation and Control

Plant instrumentation consists of intermediate loop pump control, steam generator, and reheater controls. The steam generator feedwater control system will be largely dependent on final steam generator physical design. The system, as illustrated previously in Figure II.2.4-30, serves a once-through superheat steam generator and a separate reheater section. In addition, a side-stream differential temperature limiting flow circuit has been provided. The maximum allowable differential temperature between steam generator outlet sodium and feedwater inlet temperatures will be maintained by controlled sodium bypass flow around the steam generator.

Secondary Loop Pump Speed Control

The secondary loop pump speed is controlled on the basis of secondary loop cold leg temperature. The cold leg temperature controller receives its set point from the steam plant load versus steam temperature and pressure programmer. The programmer provides reduced temperature set points for plant loads below 50% capacity.

Steam Generator Outlet Temperature Control

The steam generator superheated steam outlet temperature is indirectly controlled by steam pressure and feedwater flow. The load versus steam temperature and pressure programmer senses steam temperature and plant load and calculates appropriate control set points for feedwater flow, steam pressure, and reheater temperature. By controlling feedwater flow and steam pressure with respect to plant load, superheated steam temperature is indirectly controlled.

Steam Pressure Control

Steam pressure to the turbine is controlled to follow a plant load program below 50% plant capacity. Above 50% plant capacity, steam pressure is controlled at a constant value. The steam pressure controller receives a calculated set point from the load versus temperature and pressure programmer.

Reheater Temperature Control

The steam power plant utilizes reheat cycle turbine with a sodium heated reheater. Reheat steam temperature is controlled by throttling intermediate coolant sodium flow to the reheater unit. The reheat temperature controller receives a programmed temperature set point from the load versus temperature and pressure programmer.

Boiler Feedwater Control

Feedwater control for the once-through superheat boiler is accomplished by flow control based on a calculated set point determined by the load versus temperature and pressure programmer.

II.3.2 Materials Analysis

Increased interest in the development of fast breeder reactors has resulted in a rapidly progressing technology based on bulk carbide fuels, which possess several desirable attributes for fast breeder applications.

The high fissile and fertile atom density of monocarbide fuels permits high in-core breeding, a relatively low fuel inventory and a low rate of fuel reactivity loss. The low parasitic absorption cross section and low carbon atom density result in good neutron economy and little degradation of the fast neutron spectrum; both of these contribute toward efficient breeding.

Uranium carbide and uranium-plutonium carbide exhibit high thermal conductivities and have demonstrated reasonable irradiation stability. Irradiation studies conducted by Oak Ridge (1), Argonne (2), Atomics International (3), Battelle (4) and more recently United Nuclear Development (5), United Nuclear Research (6), and Harwell (7) have demonstrated the ability of the carbide fuels to sustain high burnups without deleterious results. Very few differences have been found between the irradiation performance of the mixed carbide and uranium carbide. These results indicate the potential superiority of carbide fuels in fast breeder applications requiring long life performance at high power levels and temperatures.

Continued testing of carbide fuels is required to better establish performance limitations and the effect of higher burnup and temperature on fuel stability. The Hallam uranium carbide core will provide additional information on the performance of hypostoichiometric fuel although at a significantly lower temperature than proposed for the present fast breeder reactor design.

This study had, as one of its objectives, the design of a reactor capable of generating 1000°F steam. This requirement, and the concurrent specification of stainless steel as the cladding material, gives rise to several problems. Efforts have been directed toward the definition and resolution of these problems. In addition, material recommendations and information on fuel and cladding properties have been provided to various functional groups.

II.3.2.1 Cladding Material

II.3.2.1.1 Mechanical Properties of Stainless Steel

The proposal to use a carbide fueled, stainless steel clad fuel element in the design of a 1000 MWe Fast Breeder Reactor imposes limitations on the type of steel which can be used. A survey of the properties, where available, of stainless steels resulted in a detailed examination of three austenitic stainless steels, viz., types 304, 347 and 316L, for use as cladding materials over the temperature range 100 to 1500°F in a flowing liquid sodium environment. The compatibility of stainless steel with liquid sodium is discussed in Section II.3.2.1.2.

Short Term Tensile Properties

The short term tensile properties of types 304, 347 and 316L stainless steel as a function of temperature are given in Figure II.3.2-1. There is wide scatter in the reported data, but specific curves have been drawn, as opposed to scatter bands, to enable the properties of the stainless steels to be compared.

TABLE II.3.2-1

Nominal Composition of the Stainless Steels

AISI Type	Nominal Composition (%)					
	C	Mn	Si	Cr	Ni	Other
304	0.08 max	2.0 max	1.0 max	18.0-20.0	8.0-12.0	---
347	0.08 max	2.0 max	1.0 max	17.0-19.0	9.0-13.0	10xC min. Nb
316L	0.03 max	2.0 max	1.0 max	16.0-18.0	10.0-14.0	2.0-3.0 Mo

Although the short term tensile properties of a material can provide a useful guide to its application, long term effects are of greater significance in the selection of clad material. These effects are discussed in the following sections.

Figure II.3.2-1 Variation in Mechanical Properties with Temperature

Creep Rupture Properties

There appears to be no marked difference in the creep-rupture strength of types 316 and 316L stainless steels and reported values for both steels have been included in the creep-rupture curve as type 316L. The creep-rupture behavior of types 304, 347 and 316L at 1000, 1200 and 1500°F is given in Figure II.3.2-2. Although the difference in creep rupture properties is marginal at 1200°F, there is a distinct advantage in using 316L stainless steel at temperatures above 1200°F.

Secondary Creep Rate

The secondary creep rates of types 304, 347 and 316L stainless steel at 1000, 1200 and 1500°F are given in Figure II.3.2-3. The limited information available on the creep rate of type 316L would indicate that it is slightly less than that of type 316, but as the difference is small, the results for types 316 and 316L have been combined and listed as type 316L.

Elongation at Rupture

Measured values of the total elongation at fracture, obtained from creep rupture tests, are widely scattered. A graph showing the general trend of total elongation with stress has been constructed for types 304, 347 and 316L stainless steel (Fig. II.3.2-4). From these results, the maximum allowable stress, for a minimum specified time to rupture and ductility, can be determined and allowed for in the design of the clad.

Effect of Environment on Mechanical Properties

The mechanical properties of type 316 stainless steel are presently being studied in liquid sodium (8,9). As shown in Figures II.3.2-5 and II.3.2-6, the sodium does not appear to have an adverse effect on fatigue or stress rupture at 1200°F. There is an effect on the creep rate as shown in Figure II.3.2-7. In 1200°F sodium at a stress of 13,500 psi for 3,000 hours, the creep rate was 0.0007%/hour. In air, the creep rate for the same conditions was 0.00025%/hour. It should be noted that Mine Safety

Figure II.3.2-2 Creep Rupture Behavior of Selected Austenitic Stainless Steel

Appliance (8) postulated that this discrepancy was due to instrumentation difficulties in the sodium. The dashpot was located out of the sodium several feet from the sample. Later work (9) ignored this postulate and stated flatly that creep rates were greater in the liquid sodium.

Microstructural Stability

Changes in microstructure on prolonged heating are generally correlated with the change in impact strength. Sticha (10), on the basis of tests extending over a period of eight years, concludes that austenitic stainless steels with low chromium equivalent are less subject to loss of impact resistance at service temperatures up to 1450°F.

The initial loss of impact strength is attributed to carbide precipitation. At increased exposure times, precipitation of the sigma phase occurs and the impact resistance is further reduced. The relation between carbide precipitation and sigma formation is complex and the loss in impact resistance is greater than that due to either carbide precipitation or sigma formation alone.

Although type 316L is virtually free of grain boundary carbide precipitate, small amounts of sigma phase are formed in the grain boundaries at 1450°F. Types 304 and 347 are more susceptible to sigma formation than type 316L.

The effect of sigma is reflected in changes in room temperature impact strength. However, at reactor operating temperatures, the change in impact strength due to sigma formation will be relatively small.

Effect of Cold Work

Stainless steel, cold worked approximately 10% to give additional strength, has been used for cladding at temperatures below 1000°F. Examination of the recrystallization temperatures for types 304, 347 and 316L indicates that recrystallization will occur at temperatures above 1300°F, and at the anticipated operating temperatures in the FBR no strength advantage will be gained by using cold worked stainless steel for the clad.

Figure II.3.2-3 Creep Rate of Selected Austenitic Stainless Steels

Figure II.3.2-4 Stress-Rupture Ductility

Figure II.3.2-5 Stress to Rupture of 316 Stainless Steel Specimen

Figure II.3.2-6 Fatigue Curves of Averaged Data for 316 Stainless Steel
Fatigue Tests

Figure II.3.2-7 Creep Rates, 316 Stainless Steel Specimens

Fast Neutron Irradiation

The effects of fast neutron irradiation (at temperatures above 1000°F) on the properties of stainless steel are not known. Results on the behavior of stainless steels irradiated at temperatures below 1000°F indicate that fast neutron irradiation increases the hardness and yield strength and reduces the ductility and the impact values. Increasing the irradiation temperature, in general, tends to reduce the changes induced by fast neutron irradiation, but the degree of "annealing" is sensitive to the initial properties of the steel, fast neutron flux and total fast neutron dose.

It is anticipated that fast neutron irradiations at temperatures above 1000°F will result in enhanced diffusion rates leading to the earlier precipitation of carbides and sigma phase formation. The effects of enhanced carbide and sigma phase precipitation on mechanical properties at reactor operating temperatures are not known with certainty, but will be less than the changes in room temperature properties.

The uncertainty of the effects of high temperatures and large fast-neutron doses on the properties of stainless steel indicates that the out-of-pile data should be used only as a guide in design work. On the basis of microstructural stability, creep strength and creep-rupture properties, it is recommended that type 316L stainless steel cladding be used for the FBR.

II.3.2.1.2 Corrosion Resistance of Stainless Steel

The selection of reactor materials must include consideration of the corrosion resistance of the material to the corrodant involved, in this case, liquid sodium. Corrosion in liquid sodium may occur by mechanisms different from those found in high temperature water systems (11, 12, 13, 14, 15). Most important of these mechanisms is mass transfer. Mass transfer includes the dissolution of material in one portion of the circuit or loop, its transport to another portion of the circuit and finally the precipitation of this material on the walls of the circuit. This mass

transfer may be caused by a thermal gradient or a chemical gradient.

Thermal Gradient Transfer

Thermal gradient transfer arises from a temperature dependent solubility. If a mono-metallic, isothermal system is considered, the corrosion or dissolution of the base metal will cease when the liquid metal is saturated. Iron, for example, has an equilibrium solubility of about 19 ppm at 1300°F in liquid sodium (16). A power system, however, is not isothermal and there will be areas of heat removal and temperature drop. In the colder areas, the liquid metal will have a decreased solubility for the base metal and a non-equilibrium condition will exist. To satisfy the requirements for equilibrium, the liquid metal or solvent will precipitate out solute or base metal to relieve an otherwise super-saturated condition. Such mass transfer, if large quantities of material are involved, may result in either substantial thinning of hot zone walls, such as fuel cladding, or in plugging of pipes and heat exchanger tubes through the precipitation of metallic crystals in the cold zone. Depending on hot zone temperature and temperature gradient, both thinning of hot zone walls and plugging of pipes may occur.

Studies have been performed on alloy systems in liquid sodium at Argonne National Laboratories, Brookhaven National Laboratory, Knolls Atomic Power Laboratory, Mine Safety Appliances, and Oak Ridge National Laboratory. Although considerable data have been amassed, liquid sodium technology has not advanced to the point where rate equations have been accurately developed for mass transfer phenomena. This is due to limitations of previous tests in which the materials of loop construction generally were the subject of more extensive evaluation than the corrosion coupons being tested. This type of investigation, although yielding much in the way of qualitative and semi-quantitative information, could not yield definitive information about corrosion rates.

Mass transfer has been found to be more severe as the oxygen content and temperature of the sodium were increased. When austenitic stainless steels were exposed to temperatures above 1000°F in sodium containing

about 200 ppm oxygen, severe intergranular attack occurred (16). The resulting structure was extremely porous because of the removal of oxidation products.

Besides intergranular attack, the presence of oxide also increases the corrosion rate in the manner of weight loss. Above 1000°F, chromium will reduce sodium oxide (17). When oxygen is low, weight gains are noted on stainless steel from the formation of chromium oxide (16). When the oxygen level is high, weight losses are noted, probably from spalling of heavy oxide films. Iron also corrodes rapidly. Brush and Koenig (16) postulated that the iron oxide formed was reduced to furnish additional Na₂O which acted as a catalyst for accelerated corrosion of iron. As a result, tests in liquid sodium had to be limited to low oxygen concentrations to provide realistic corrosion data.

Additional experiments were performed to determine corrosion rates above 1000°F. McKee (18) developed a gas lock to fit on a thermal convection loop (harp). By the use of this gas lock, he was able to periodically remove a sample from the loop, weigh it, and obtain corrosion rates on the sample at 1575°F. His parameters included a sodium flow velocity of 1/6 feet per second and an oxygen content that ranged from 60 to 120 ppm. McKee reported a corrosion rate of 50 milligrams per square decimeter per month or 0.3 mils per year after 4,000 to 5,000 hours of test. McKee also presented a graph of his raw data. From this data, a corrosion equation of the form $W = k t^n$ could be calculated where W is total weight loss in milligrams per square decimeter, k and n are constants with values of 125 and 0.67 respectively, and t is time in months. Metallographic examination of the specimens revealed a ferritic layer on the surface. McKee attributed the observed decrease in corrosion rate at longer exposures to the formation of this ferritic layer. No additional tests were performed at other temperatures.

Tests were performed at American Standard in thermal convection loops (19,20). Trains of coupons were inserted into the hot legs of the loop to evaluate the corrosion with respect to the position in the hot leg. Temperatures varied from 1350°F to 1600°F. Oxygen content was approximately

50 ppm. In all cases, the specimens gained weight in the colder end of the hot leg and lost weight at the higher temperature. The weight changes varied uniformly from positive values (deposition) near the bottom through zero near the center of the hot leg to increasingly negative values (attack) in the hotter region near the top. Although no measurements were made, it is reasonable to assume that the tube walls of the loop experienced the same type of deposition and dissolution.

The zero point, or area where the attack and deposition balanced each other, can be postulated as that area where the liquid sodium was at equilibrium with the loop and coupons. In the cooler areas, the sodium was supersaturated and relieved this situation by precipitation of solute. Farther up the loop, where the temperature was higher, the solution was unsaturated. Thus, the sodium was again corrosive and could attack the stainless steel. Weight losses at 1600°F were in the order of 70 mg per square decimeter per month or approximately 0.4 mils per year. Velocity of the sodium was given as 3 feet per minute.

To establish quantitative data, General Electric, under the auspices of the Atomic Energy Commission, built, and is operating a series of six pumped liquid sodium loops (21). One loop was built entirely of type 316 stainless steel and two loops were built with type 316 stainless steel in the hot legs and $2\frac{1}{2}$ Cr-1% Mo in the cold legs. Of the three loops remaining, one had 316 stainless steel in the hot leg and 5% Cr- $\frac{1}{2}$ Mo- $\frac{1}{2}$ Ti in the cold leg; another loop was built completely of the $2\frac{1}{2}$ Cr-1% Mo steel and the final loop was built completely from the 5% Cr- $\frac{1}{2}$ Mo - $\frac{1}{2}$ Ti steel. Each loop had seven sample holders and each sample holder had two sample inserts. One sample insert holds three samples and allows sodium to flow by at rates of approximately six feet per second; the second sample insert holds only one sample and allows sodium to flow by at rates of approximately 21 feet per second. Test runs were made at 1200°F and 1100°F with temperature drops of about 250°F or 500°F. GE has issued reports concerning metallographic examination of the test specimens (22) and tabulation of the raw data (23).

Their metallographic examination revealed an austenitic to ferritic transformation at the surface of the type 316 stainless steel at temperatures of 1100 to 1200°F. This ferrite was noted after 700 hours of exposure, and transformation was enhanced in areas of deformed structure. Chromium depletion was also noted in these areas. A surface anomaly was found on coupons exposed at 1000°F for 700 hours. Hetzler and Young (22) postulated that this was the inception of the austenite to ferrite transformation. Samples from the hot legs had evidence of heavy carbide build-up. McKee (18) postulated that the formation of a ferritic surface layer increased the corrosion resistance of the stainless steel to liquid sodium. Analyses of the raw data reported by Pohl (23) showed a time dependence on the corrosion rate of the stainless steel that was identical to that reported for McKee in that $W = k t^n$ where W is the weight loss in milligrams per square decimeter, t is time in months, and k and n are constants. As before, the value of n is approximately 2/3. Since k is a function of temperature, velocity and geometry of the loop, it is expected that k values should be different. This value was calculated for type 316 stainless steel in stainless steel loops. Lockhart (24) claimed that the effect of velocity was also that of an exponential function of the type $W = k' V^m$ where W is weight loss in milligrams per square decimeter, V is velocity in feet per second, k' and m are constants. Calculations at Westinghouse Atomic Power Division using Pohl's data (23) gave a value of 0.4 for m .

Further analyses of raw data revealed an effect due to geometry, hence a closer look at the General Electric loops is warranted. The sample holders and heaters are arranged in the following order: heater, first sample holder (H1), heater, second sample holder (H2), heater, third sample holder (H3), fourth sample holder (H3R) and three successive cold sample holders. The first heater raised the temperature of the liquid sodium to 200°F below the highest temperature in the loop. The second and third heaters each raised the sodium temperature an additional 100°F. The three successive cold sample holders were used to determine deposit rates on a colder area.

Although sample holders H3 and H3R were at the same temperature, they did not exhibit the same weight losses. The weight losses in H3R were signifi-

cantly less than those in H3. Pohl (23) and Lockhart (24) called this the "downstream factor." This was probably caused by the sodium being partially saturated with iron, nickel and chromium. In these downstream areas, the thermodynamic driving force is not as great as in the region of maximum attack because equilibrium conditions are being approached. This is consistent with Epstein's (25) predictions which indicate that one area in a loop is the point of maximum attack and succeeding areas, though at the same temperature, will not suffer as much corrosion. The variations in corrosion rate with time and sodium flow velocity have been defined previously. The remaining variations in corrosion rate are attributed to effects of sodium temperature and sample location. An Arrhenius plot of these data results in a separate curve for each sample position. The curves are approximately parallel and the observed displacement is thought to be an indication of the downstream factor, e.g., the corrosion rate at sample position H2 is approximately 1.7 times that at H3 and 6.3 times that at H3R.

Extrapolation of the data for position H2 (highest corrosion rate) to 1400°F, 31.8 feet per second sodium velocity and 25,000 hours indicates that a clad thinning of 4.3 mils would occur at the hot spot. In any case, it will be necessary to run corrosion tests to verify the corrosion rate under the contemplated condition of exposure.

It should be emphasized that several assumptions were made in this calculation. Among the most important is that oxygen content at full power be restricted to about 10 ppm through the use of cold and hot traps. Carbon should also be restricted to a very low level by the use of carbon traps. In addition, a monometallic system must be used. Pohl (23) reported that rates of corrosion or weight losses in dissimilar metal loops were significantly higher (at least $1\frac{1}{2}$ to 2 times as compared to the all 316 stainless steel loops). The type 316 stainless steel coupons with high weight losses were, in most cases, heavily carburized. In those cases where the coupons had high weight losses but were not heavily carburized, chemical analyses of the liquid sodium showed carbide contamination of greater than 120 ppm. Because of insufficient information, no theory can be postulated as to

why bimetallic loops exhibit higher corrosion rates. Based on the available information, however, carbon trapping and a monometallic system such as 316L are recommended. Type 304 may be compatible in some applications.

Chemical Gradient Transfer

As with thermal gradient transfer, chemical gradient transfer entails the dissolution of material in one part of a circuit or loop, its transfer to another part of the circuit and finally, its deposition on the materials in this second part of the circuit. In liquid sodium systems, chemical gradient transfer of carbon is of primary interest. In this case, ferritic steels which would otherwise be used for piping are decarburized with the carbon going into solution in the liquid sodium. The carbon is transported to the austenitic stainless steel and then carburizes the stainless. Another potential carbon source is hyperstoichiometric uranium carbide fuel. The UC_2 phase is readily decarburized releasing carbon to the sodium. Carburization of the stainless steel will result in reduced mechanical properties particularly fatigue strength and tensile elongation. Such carburization will be obviated by the use of hypostoichiometric fuel in the present design.

The solubility of carbon in liquid sodium, as determined by Gratton (26), follows the conventional $\ln S = A - B/T$ formula with variations in the constants according to the oxygen content. The solubility of carbon in ppm, from $400^\circ F$ to $1300^\circ F$ may be expressed as follows:

$$\ln S = 4.96 - 6.21 \times 10^2/T \text{ with 30 ppm of oxygen content}$$

$$\ln S = 5.61 - 3.76 \times 10^2/T \text{ with 260 ppm oxygen content}$$

where S is the solubility of carbon in sodium (ppm) and T is in degrees Kelvin. It should be noted that increased amounts of oxygen also permit a greater solubility of carbon in the liquid sodium.

Experimenters at Atomics International (27) reported decarburization of low chromium steels, but essentially no decarburization of steels containing 5% chromium stabilized with Ti or steels containing greater than 5% chromium. These steels were exposed in a high velocity isothermal loop at $1025^\circ F$ and in a low velocity thermal convection loop (harp) at $1050^\circ F$ with a $500^\circ F$ temperature gradient. Both loops contained ferritic and

austenitic steels. Steels containing $1\frac{1}{4}$ and $2\frac{1}{4}$ chromium were decarburized while steels containing 5% chromium or greater were carburized when carbon was initially present in the liquid sodium. When carbon was initially absent from the liquid sodium, 5% chromium stabilized with Ti, and the 7 and 9% chromium steels were approximately neutral in activity. The rate-controlling step for the decarburization of ferritic steels in a loop with a high area ratio of austenitic to ferritic steels was apparently the diffusion of carbon in the ferritic steel.

An additional study by Atomics International (28) was made specifically on type 304 stainless steel between 1000°F and 1600°F. Aluminized static and rotating capsules were used to contain the stainless steel, the carbon additives and the liquid sodium. All samples were held for 100 hours at temperature. The carbon content of type 304 stainless steel in equilibrium with carbon saturated sodium was found to vary from 2.68% at 1000°F to 4.35% at 1600°F. Carbon contents of 15 to 18 ppm resulted in carburization of the stainless steel but no precipitation of carbide phases was noted at 1200°F, although the carbon content of the steel was greater than the solubility in the steel. Because the carbon content was greater than equilibrium in the steel, Anderson and Sneesby postulated that carburization was caused by supersaturation of the austenite which became metastable with respect to precipitation of carbide. The supersaturation of the steel continues until the instability of the austenite is great enough to cause precipitation of the carbide. Since the carbide phase in the stainless steel is essentially insoluble at exposure temperature, the system then acts as if chromium were removed from the alloy. Successive supersaturation and precipitation of carbide, if continued long enough, would eventually result in 90% Fe-10% Ni alloy. All chromium would be precipitated as $(\text{Fe}, \text{Cr})_7\text{C}_3$ with approximately 75% chromium. The remaining Fe-Ni matrix would then start to be carburized.

Loop tests performed on bimetallic pumped loops at GE (29) with 316 stainless steel in the hot leg and $2\frac{1}{4}$ Cr-1 Mo in the cold leg confirmed the carburization and decarburization previously encountered at AI. The carbon data were fitted to the Arrhenius equation coefficients, $k \exp [-Q/RT]$. Coefficients are as follows:

TABLE II.3.2-2

Coefficients of Carburization and Decarburization

O_2 , ppm	Material	k, cm	Q, cal/gm mole
10	$2\frac{1}{4}$ Cr - 1 Mo	3.6×10^{-3}	$8,000 \pm 2,000$
	316SS	1.4×10^{-2}	$5,000 \pm 1,000$
50	$2\frac{1}{4}$ Cr - 1 Mo	2.2×10^{-2}	$4,000 \pm 3,000$
	316SS	2.4×10^{-3}	$12,000 \pm 5,000$

It was apparent that the 316 stainless steel was unable to accept carbon as rapidly as it was made available by the $2\frac{1}{4}$ Cr - 1 Mo steel. The 5 Cr - $\frac{1}{2}$ Mo - Ti steel, tested under similar conditions, was approximately neutral to carbon migration.

Studies at AI (30) have shown that carburization is a problem when hyperstoichiometric carbide is used. The carbon content above 4.8 w/o is present as a dispersed UC_2 phase which is readily decarburized by the liquid sodium by the reaction $UC_2 \xrightarrow{Na} UC + C$ (dissolved in Na). The degree of cladding carburization is dependent on the carbon content above the stoichiometric 4.8 w/o carbon. Stoichiometric and hypostoichiometric uranium carbide do not carburize the cladding.

Work at Atomics International (31) has shown that carbon traps can be built to remove carbon in liquid sodium at temperatures above $1000^\circ F$. The philosophy of a trap is to insert a large surface area of a material which will preferentially carburize, thus protecting other materials in the core. Type 304 and type 410 stainless steels were found to be effective in removing carbon. The 410 was more effective than the 304 in reducing the level of carbon when greater than 40 ppm, but as the carbon content was reduced, the carbon gettering effectiveness of the type 304 increased rapidly. The carbon trap in the Hallam Nuclear Reactor (32) contains 28,200 square feet of 0.020 inch stainless steel foil, dimpled to provide a 0.060

inch gap for sodium flow. Test tabs of 30⁴ stainless steel are inserted for 100 hour runs then subjected to microhardness traverse, metallographic examination and chemical analyses. The hardness traverse can detect as low as 0.19% C, the metallographic analysis can show the depth of case at 0.31% C, while wet chemical analysis will give total carbon analysis. From the measured depth of carburization, coupled with time and temperature of exposure, the surface carbon content can be calculated by the methods described in reference 13.

To test the effects of carburizing on the mechanical properties of 30⁴ stainless steel, this material was carburized in sodium to levels of 0.06 (as-received), 0.27, 1.0, 1.37 and 1.66% and then tensile tested (33). At the 0.27 w/o carbon, room temperature ductility decreased significantly, but elevated temperature effects were not as great. At 1.0 w/o C, elongations at both room and elevated temperatures were markedly decreased. Data comparing normal 30⁴ and high carbon are given below.

TABLE II.3.2-3

Tensile Properties of Type 304 Stainless Steel Containing Two Carbon Levels

<u>Test Temp.</u> <u>°F</u>	<u>Yield Strength, PSI</u> <u>(0.2% Offset)</u>	<u>Tensile Strength,</u> <u>PSI</u>	<u>% Elongation</u> <u>in 1 in.</u>	<u>Reduction</u> <u>of Area, %</u>
<u>0.06 wt. % carbon</u>				
Room	38,900	84,250	52.0	52.0
500	27,300	63,700	37.5	46.9
800	24,200	62,000	35.5	38.8
1050	21,300	55,350	34.8	40.1
1200	20,400	48,350	32.5	42.4
<u>1.66 wt. % carbon</u>				
Room	49,500	135,800	4.9	4.8
500	56,800	107,000	5.5	8.4
800	46,500	99,300	10.0	11.2
1050	41,200	82,100	13.5	21.0
1200	37,600	69,500	16.3	31.3

II.3.2.1.3 Review of Alternate Cladding Materials

At the high temperatures at which the reference stainless steel cladding will operate, the mechanical properties and resistance to mass transfer must be better defined by additional testing. In the event these properties are inadequate, it is necessary to use an alternate material for cladding. The refractory metals, or those with melting points of 3250°F or higher, may be considered. Of these elements, some like osmium, iridium, and rhodium are precious metals. Chromium is brittle and fabricated with difficulty. Hafnium has a large cross section, and its current high price of greater than \$125 per pound removes it from consideration. The possible alternate cladding materials with comparative nuclear parameters are shown in Table II.3.2-4 (34), calculated in comparison to stainless steel on an equal volume basis.

The high absorption and poor breeding ratio incurred by the use of tantalum precludes its use as a cladding material. The effect of molybdenum on the breeding ratio coupled with large coolant reactivity temperature coefficients and small Doppler also rule out the use of this material for cladding.

TABLE II.3.2-4

Values Relative to Fe (Stainless Steel), (34)

Clad Base Metal	Critical Mass	Breeding Ratio	Average Neutron per Absorption	Temperature Coefficient	
				Coolant dk/dT	Doppler dk/dT
Fe	1.00	1.00	1.00	1.00	1.00
Nb	1.14	.83	.91	3.4	.13
V	1.06	.92	.94	0.97	.93
Mo	1.16	.80	.91		
Ta	1.66	.56	.83		
Ti	.99	1.05	1.03		
Zr	.96	1.04	1.01		

According to Okrent (35), niobium may be tolerable in all nuclear respects for a small U^{238} -Pu metal fueled reactor of EBR-II size and a large U^{238} -Pu metal fueled reactor employing niobium might result in an acceptable breeding ratio, but unacceptable sodium void coefficient. He further states that in a large U^{238} -Pu oxide fueled reactor, niobium would be unacceptable not only because of the effects on breeding ratio and sodium void coefficients, but also its reduction in Doppler coefficient. Since a U^{238} -Pu carbide reactor has an enrichment and a spectrum between that of metal and oxide fuel, the use of niobium at the present time appears unacceptable for a large, single core, carbide fueled reactor. New concepts (such as the modular core) or further developments in basic physics data and core characteristics may, however, revive interest in niobium at which time it would be considered in greater detail.

The nuclear properties of titanium would be attractive for a fast breeder. Unfortunately, at temperatures of about 1200°F , the beta phase (or high temperature) alloys have lower tensile strengths than stainless steel (36), thus preventing use of titanium alloys.

Zirconium is also attractive. Atomics International has made alloy development studies on zirconium alloys with additions of aluminum, tin, and molybdenum (37). These alloys were tested in liquid sodium at 1000°F in thermal loops and were found to have acceptable corrosion resistance. The higher alloyed compositions had tensile strengths equal to or greater than type 304 stainless steel at 1200°F . More recent corrosion tests on these alloys have shown anomalous behavior, in that unexpected severe weight losses were encountered in these alloys when tested in liquid sodium at 850°F for 500 to 1000 hours (38). It is apparent that additional development work must be performed on the zirconium-base alloys.

Vanadium is a potential alternate material. Although its nuclear properties, such as slightly greater absorption cross-section and the resultant lowered breeding ratio, may be undesirable for an oxide fueled reactor, it may be satisfactory for a carbide fueled reactor. Static tests in liquid sodium containing 10 ppm of oxygen as Na_2O were performed at Argonne National Laboratory on vanadium (39). After 288 hours exposure at 700°C

(1291°F), unalloyed vanadium was unattacked. Vanadium forms a low melting oxide (V_2O_5) that melts at 670°C (1240°F), but there are other conditions in favor of vanadium in a sodium cooled reactor. Serious efforts with both cold and hot traps must be made to keep the oxygen content low. In addition, according to Smith and Van Thyne (39), based on thermodynamic data, the lower oxides VO and V_2O_3 will form, but the unstable oxide V_2O_5 should not form in sodium. The same source also reports elevated temperature tests showing lower tensile properties for unalloyed vanadium than for 347 stainless steel. This would necessitate alloying for higher strength.

Armour Research Foundation has been conducting alloy development programs for stronger vanadium alloys (40,41). The investigation readily revealed the importance of interstitial contaminants including oxygen, nitrogen and carbon. These elements determined the fabricability of the experimental alloys, particularly the V-5%Ti-20%Nb alloy which was their outstanding alloy. The original developmental ingot could not be cold rolled, but had to be hot rolled almost to finished dimensions. The second ingot had the following interstitial content:

C - 21 to 80 ppm
O₂-135 to 400 ppm
N₂- 98 to 167 ppm

This ingot was cold rolled with a 95% reduction in thickness without difficulty, demonstrating excellent fabricability (42). Armour reported excellent weldability. Mechanical properties are given in Table II.3.2-5.

Additional information on vanadium alloys is confined mainly to tests above 1100°C (2010°F) with no creep information available below this temperature. Operating experience in the Dounreay Fast Reactor has shown a high sensitivity to hydrogen, oxygen, and nitrogen impurities in the sodium (43). Their tests showed that hydrogen contents in the vanadium of 60 to 80 ppm will cause brittle failure in a simple bend test. The raw material contains 10 to 66 ppm H₂. They have found that the use of zirconium in a hot trap, besides the action of the cold trap, is required to remove the hydrogen. The hydrogen is picked up from moisture and forms

sodium hydride. The first cleaning of the cold trap revealed sufficient NaH to embrittle the entire core.

TABLE II.3.2-5

<u>Tensile Properties of V-5%Ti-20%Nb Alloy</u>			
<u>Temperature of Test, °F</u>	<u>0.2% Offset Yield Strength, psi</u>	<u>Ultimate Tensile Strength, psi</u>	<u>Elongation, %</u>
Room	97,700	106,200	19
1200	68,200	86,800	—
1800	63,000	69,500	10

The Dounreay reactor uses nitrogen as a cover gas, as compared to argon for the FBR. When the sodium is above 550°C, the vanadium will getter nitrogen from the sodium. Eighteen hundred ppm of nitrogen will completely embrittle the vanadium. The corrosion rate reported at 350°C in flowing sodium with 6 ppm O₂ is 10⁻⁴ in/mo.

Argonne National Laboratory has just completed exposure of vanadium alloys to a 21-day test in flowing (2 ft/sec) low oxygen (1-5 ppm Na₂O) sodium at 650°C (1200°F) (44). Weight gains or losses were low, but all specimens were brittle at room temperature after exposure. Chemical analyses showed large increases in oxygen with concomitant increases in hardness. Exposure to sodium containing 140 ppm oxygen adversely affected all alloys tested.

Further explanation by Argonne of these tests (45) revealed that uranium was used as the getter for the oxygen in the hot trap. Apparently, the vanadium alloys had a higher affinity for oxygen than the uranium; at the conclusion of the test, the uranium had a low oxygen content. An additional test (45) using zirconium in the hot trap showed good ductility for a V-5 Ti alloy indicating that with good control, vanadium alloys will not be embrittled. Further tests are in progress.

At this time, there is no completely satisfactory substitute for austenitic stainless steel. Available information is inadequate to presently specify a high temperature zirconium or vanadium alloy for use as fuel cladding. In the event, however, that developments in core characteristics do not preclude niobium, a niobium alloy may be specified.

II.3.2.2 Cermet Controlled Expansion Rod

Physical and Mechanical Properties

The physical and mechanical property values used in the design of the Controlled Expansion Element are density, specific heat, thermal conductivity, linear expansion, and modulus of elasticity. Published information on the properties of uranium dioxide - stainless steel cermets covers a wide range of uranium dioxide contents but the number of properties measured for a given cermet composition is limited.

Best estimates of the properties of a 20 v/o UO₂ - 316 SS cermet have been made, neglecting differences between 316 and 316L type steels, over the temperature range of 900 to 2000°F. Because of the uncertainty in deriving the properties, ranges of values are given in Table II.3.2-6.

TABLE II.3.2-6

Properties of a 20 v/o UO₂ - 316 SS Cermet

<u>Property</u>	<u>Temperature</u>		
	<u>900°F</u>	<u>1500°F</u>	<u>2000°F</u>
Density, (g/cc)	8.38-8.48	8.20-8.37	8.06-8.27
Specific Heat, (cals/gm-°C)	0.108-0.122	0.124-0.130	0.130-0.137
Thermal Conductivity, (watts/cm-°C)	0.06-0.12	0.078-0.180	0.073-0.229
Linear Thermal Expansion, (°C ⁻¹) x 10 ⁶	9.7-17.5	10.3-18.5	11.1-20.0
Modulus of Elasticity, (psi) x 10 ⁻⁶	19-25.5	15-20	12.75-17

The reference cermet rods in the CEX assembly have the same diameter and one fifth the volumetric heat generation rate of the carbide fuel rods. Since 20% voidage is incorporated within the carbide fuel rods, the UO_2 loading required in the cermet rods (if the oxide enrichment is the same as in the carbide fuel) is 23.4 v/o (29.8 w/o).

Keller (46) has correlated the irradiation performance of uranium dioxide-stainless steel cermets clad in stainless steel, having fuel loadings of 18 to 30 w/o. This correlation permits a prediction of the allowable burnup which can be achieved without fission product release or blistering or cracking of the cermet. The following table compares the conditions of maximum burnup and maximum surface temperature in the reference design to the allowable burnup predicted from Keller's correlation. The latter burnup values are those below which no failures were found.

From Table II.3.2-7 it is apparent that the anticipated temperature and burnup are acceptable for conditions of maximum burnup and also for the peak cermet surface temperature. The condition of average burnup in the reference design, i.e., 10 a/o uranium, is also acceptable at surface temperatures in the range 1150-1326°F.

TABLE II.3.2-7

Peak Surface Temperature vs Burnup

<u>Peak Surface Temperature, °F</u>	<u>Burnup, a/o Uranium</u>	<u>Allowable Burnup, a/o Uranium (<u>46</u>)</u>	<u>Remarks</u>
1150	13.0	15.9	Max. burnup, reference design
1326	7.5	11.3	Max. temperature, reference design

Factors influencing the life of the cermet include fuel element configuration, distribution of the fuel particles, and burnup rate. However, if the distribution of the fuel particles is such that a continuous stainless

steel matrix undamaged by recoil atoms exists, no premature failures of the cermet are anticipated.

An alternate CEX assembly design has also been considered. In this case, the seven cermet rods are replaced by three rods with a diameter 60% larger than the carbide fuel rods. The volumetric heat generation rate is accordingly lowered to 10% of that of the carbide fuel rods. The required UO_2 loading for equal fuel enrichments in the cermet and carbide fuel rods is 11.7 v/o (15.4 w/o). At these lower oxide loadings, the allowable burnup predicted from Keller's results is 23 a/o uranium and 32 a/o uranium at temperatures of 1326°F and 1150°F, respectively. These allowable burnups are far in excess of those contemplated in the present design.

II.3.2.3 Uranium-Plutonium Carbide Fuel

II.3.2.3.1 Fuel Properties and Performance

A literature survey was performed to determine the properties and performance of carbide fuels. Based on information available early in this study, the property values shown in Table II.3.2-8 were recommended for design purposes:

TABLE II.3.2-8

Properties of Uranium Plutonium Carbide at Several Temperatures

	<u>400°C</u>	<u>600°C</u>	<u>800°C</u>	<u>1000°C</u>
Average linear thermal expansion coefficient x 10^6 , °C ⁻¹	9-10.8	9.8-11.3	10.5-11.3	10.6-11.7
Density, percent of room temperature density	98.8-99.0	98.1-98.3	97.4-97.6	96.7-97.0
Specific heat, cal/mole-°C	13.7-14.6	14.4-15.2	14.6-15.7	14.7-16.1
Thermal conductivity, cal/cm-sec-°C	.041	.045	.048	.052

Continued review of the literature and consideration of additional data have not resulted in significant changes in these values.

The ranges of values reported for thermal expansion (and consequently density) were based largely on uranium carbide values. The expansion coefficient of the mixed carbide appeared to fall near the upper end of these ranges at the higher temperatures. Considering this trend, the data were extrapolated to $11.0-12.7 \times 10^{-6} \text{ }^{\circ}\text{C}^{-1}$ at 1200°C . This range brackets the experimental values of $11.85-12.4 \times 10^{-6} \text{ }^{\circ}\text{C}^{-1}$ subsequently found in the literature. The additional thermal expansion data found would permit the ranges reported in Table II.3.2-8 to be narrowed somewhat.

The thermal conductivity values were based on limited data reported for (U Pu)C, the temperature dependence of the thermal conductivity of uranium carbide, and qualitative LASL results which indicated that the conductivity of the mixed carbide approaches that of UC as the temperature is increased. (47). Subsequent publication of the LASL results allowed confirmation of the tabulated value at 400°C (48). The values recommended in Table II.3.2-8 are slightly lower than would result from linear extrapolation of the LASL results for a fuel composition in the neighborhood of $(\text{U}_{.75}\text{Pu}_{.25})\text{C}$.

A range of $15.3-17.3 \text{ cal/mole } ^{\circ}\text{C}$ was recommended for the specific heat at 1700°C . These values and those reported in Table II.3.2-8 were taken from the estimate of Henney, et al. (49), and the recommendations of an IAEA panel which reviewed the thermochemical properties of carbides (50).

A recommendation was made that the melting point of the mixed carbide be estimated by linear interpolation between the melting points of uranium carbide ($\sim 2400^{\circ}\text{C}$) and the decomposition temperature of plutonium carbide ($\sim 1650^{\circ}\text{C}$). Subsequently, a value of 2470°C (51) was found for $(\text{U}_{.8}\text{Pu}_{.2})\text{C}_{0.95}$.

The available information concerning the irradiation stability of carbide fuels was reviewed to determine the extent of fuel swelling which must be accommodated within the fuel elements. Hahn (52) has reported the lower

limit of observed fuel diameter changes to be about 0.7% per atom percent burnup. Assuming isotropic growth, this corresponds to 2.25% $\Delta V/V$ per 10,000 MWD/MTU burnup. Significantly greater diameter changes have been observed but these are attributed to microcracking or gross cracking followed by thermal ratcheting of the fuel. Both of these mechanisms could be expected to result in a contribution to the fuel diameter increase but should be relatively insensitive to burnup, e.g., the maximum observed fuel diameter increase in the AI program was 6-7% independent of burnup or fuel center temperature (53). These observations apply to burnups in the vicinity of 25,000 MWD/MTU at fuel center temperatures up to approximately 2150°F.

Irradiation of stainless steel - UC dispersions containing 24 w/o uranium carbide resulted in thickness changes (neglecting blisters) up to 1.7% (54). If the density change is assumed to be equal to the thickness change and attributed entirely to fuel swelling, an estimate of approximately 1.7-2.0% $\Delta V/V$ per atom percent burnup results, for the highly restrained fuel irradiated to about 50,000 MWD/MTU at 1700-1800°F. Porosity was observed within the fuel, indicating that fission gas agglomeration may be significant at high burnups even for relatively low irradiation temperatures.

II.3.2.3.2 Specification of Reference Fuel

Based on the foregoing observations, a recommendation was made that the fuel element design provide for a volume increase of the fuel of 20%. Distributed isotropically within the fuel element, the incorporated voidage will also accommodate the observed diameter changes of 6-7% reported by Atomics International.

Selection of the reference fuel composition requires consideration of potential compatibility problems with the specified stainless steel cladding. Moreover, the requirements of a high specific fuel power and the desire to minimize the fuel operating temperature suggests the use of a liquid metal thermal bond.

Present fuel synthesis techniques are only capable of maintaining a target fuel composition within about ± 0.05 weight percent carbon. Thus a fuel rod will contain hyperstoichiometric or hypostoichiometric fuel pellets, or both.

Hyperstoichiometric fuel is decarburized in the presence of sodium above about 1000°F (55). The sodium facilitates transport of the excess carbon (above the stoichiometric composition) to the stainless steel cladding. A requirement that all the fuel be hyperstoichiometric would result in pellets containing up to 0.1 w/o excess carbon. Complete transfer of this amount of carbon to the cladding would result in an increase in the carbon content of the stainless steel up to 1.2 w/o. According to studies at AI (55), the depth of fuel decarburization is approximately linear with time. The decarburization rate at 1200°F (~ 3.7 mils per 1000 hours) would result in about 90% decarburization in three years. The resultant clad embrittlement, as evidenced by irradiation test failures reported by AI (56), precludes the use of hyperstoichiometric fuel with stainless steel cladding and a sodium thermal bond.

Hypostoichiometric fuel will contain a metallic uranium-plutonium phase. Since the uranium-iron and plutonium iron eutectics melt at 1335°F and 770°F , respectively, eutectic formation is likely if the hypostoichiometric fuel contacts the cladding. Hypostoichiometric fuel irradiated in the AI-3-5 capsule exhibited migration of the metal phase to the fuel cladding interface (56). Although this was not observed in the AI-3-1 test which contained less free uranium and was irradiated to a lower burnup, the possibility of migration of the uranium-plutonium phase to the interface must be considered if hypostoichiometric fuel is specified for this design. Results reported by ANL (57) indicate that penetration of ^{347}Pu stainless steel occurred to a depth of 9 mils when held in contact with U-20 w/o Pu-10 w/o Fs for 748 days at 1022°F . At 1112°F , liquid phase was observed and penetration was more rapid.

Penetration rates of the order of 1 mil per hour are found when stainless steel is held in contact with the iron-uranium eutectic just above its melting point (58). Since the uranium-plutonium-iron eutectic will have

a lower melting point than that exhibited in the uranium-iron system (e.g., U-Pu-Fe forms a liquid phase with stainless below 1112°F), and cladding temperatures are expected to reach approximately 1400°F , the formation of a liquid phase is likely in the event of fuel-cladding contact.

As a result of the present design study, a modification of the composition of hypostoichiometric fuel is recommended for the reference fuel. It is believed that this modification will result in good compatibility between the fuel and the cladding to temperatures greater than 1600°F where carbon transfer by solid state diffusion rather than the formation of a low melting eutectic, may be limiting. The recommended modification is the addition of 1 w/o iron to fuel containing 4.71 ± 0.05 weight percent carbon. The low carbon content will insure that the fuel is free of higher carbide phases and the iron addition is expected to partition to the metallic phase where it will form a (U,Pu) Fe_2 solid solution.

The compatibility advantage afforded by the iron addition may be visualized by reference to Figure II.3.2-8 where partial solidus surfaces in the U-Pu-Fe ternary have been inferred by linear interpolation of the U-Fe and Pu-Fe solidus lines. It is apparent that an iron addition, sufficient to maintain the metallic phase in the fuel above 66.7 a/o iron, results in a increase in the solidus of at least 550°F (linear interpolation was used in the absence of experimental data). Thus, interdiffusion between the cladding and the (U, Pu) Fe_2 phase should not result in the formation of a liquid phase (and consequent rapid cladding penetration) below about 1080°C .

Moreover, the formation of (U, Pu) Fe_2 should result in improved irradiation stability by virtue of its F.C.C. crystal structure and higher melting point (inferred from binary systems).

The cladding, at a maximum temperature of about 1400°F , will have a rupture strength of 4-6,000 psi for 25,000 hours. The maximum tolerable gas release will be only 2-3% of that generated at ten atom percent burnup if the fuel element design incorporates a gas plenum volume equal to one half

Figure II.3.2-8 Approximation to Solidus Surfaces in U-Pu-Fe Ternary
(Inferred by Linear Interpolation)

the fuel volume. (If the cladding thermal stress and a reasonable safety margin are introduced, the allowable release will be lowered).

The previously described irradiation of UC-stainless steel dispersions suggests that gas mobility is significant at high burnups and fuel temperatures of 1700-1800°F. Recent high temperature irradiation results demonstrate that gas release greater than 2-3% must be anticipated (59). The concurrent specification of a high steam generation temperature and stainless steel cladding precludes the possibility of containing the fission gases released by the fuel. The alternate approach, that of venting the fission gases, has been selected for the reference design.

With a vented fuel element design, the gas release can be maximized by the use of low density pellets. A low density, e.g., 90% of theoretical, results in high specific surface area of the fuel and a large fraction of internal porosity which communicates with the surface. This insures that a large portion of the generated gases can escape from the fuel without contributing to fuel growth or swelling. The reference fuel density has been set at 90% of theoretical.

The fuel may be expected to exhibit some plasticity at the fuel temperatures and burnups anticipated. Therefore, some of the fuel growth can be accommodated within the low density pellets. Additional volumetric expansion of the fuel will be accommodated by fuel element voidage exterior to the pellet.

Consideration was given to the possibility of double venting fuel elements to allow continuous sodium flow through the fuel rod. The concept was discarded because of the observed carburization of stainless steel bonded to hypostoichiometric fuel by oxygen contaminated sodium (56). The continuous flow of coolant sodium containing, perhaps, 30 ppm of oxygen would represent essentially an infinite source of oxygen to the fuel. As a result, fuel oxidation and concurrent release of carbon to the sodium would result in significant carburization of the cladding.

The amount of carbon pickup by the cladding as a result of fuel oxidation caused by sodium ingress on cooling and egress on heating was calculated to be approximately 0.005% for 100 thermal cycles between 400 and 1600°F. This amount of carburization would not be detectable in the cladding.

Steady state oxygen diffusion through the vent cannot be estimated without information on the diffusivity of oxygen in sodium.

II.3.2.3.3 Fuel Synthesis and Fabrication

The reference design for the FBR specifies fuel compacts with densities of 90% of theoretical. The advantages of such a low density porous fuel have been described previously in this report. Based on the density criteria and present day technology, the powder metallurgical process was selected as the most feasible method for fabricating the compacts.

It is understood that all work with plutonium will be carried out in protective enclosures, that a high purity inert atmosphere will be employed for handling the carbide powders, and that additional protective measures and safety precautions will be required for handling plutonium with a high content of Pu-240.

Synthesis

Ceramic grade (U, Pu)C powders have been prepared on a laboratory scale by reaction of carbon with the metal (Pu, U) or the oxide (PuO₂, UO₂). Because the metal is prepared from the oxide, direct carbon reduction of the oxide is considered to have greater economic potential. Further reduction of the co-precipitated oxide, represented by the overall reaction $(\text{Pu}, \text{U})\text{O}_2 + 3\text{C} \longrightarrow (\text{Pu}, \text{U})\text{C} + 2\text{CO}$, appears to be the more favorable process. Recent work indicates that this method of synthesis is subject to better control of stoichiometry than reduction of the separate oxides followed by blending and homogenization. Also, there is some question as to whether the latter method will yield a sufficiently homogeneous mixture.

While success has been achieved on an experimental scale employing the above reaction, additional development work is required to (1) develop more positive means of controlling stoichiometry, (2) define more clearly the role of the reaction variables involved, (3) develop means of controlling the synthesis so that a consistent grade of product is obtained, and (4) adapt the procedures to pilot plant scale production.

Pellet Fabrication

The products of the above preparations require crushing and ball milling (other means of powder attrition may also be used) to obtain powders suitable for pressing and sintering. The ball milling step may be the ideal point for the addition of 1 w/o iron to the product (reasons for the iron addition have been explained previously). The addition can be made either by directly introducing 1 w/o iron powder to the ball mill charge or by introducing a mixture of iron and (Pu,U)C powder. Alternatively, the iron can be introduced after the ball milling step by blending.

Once the product is ball milled and the iron successfully introduced, the normal pressing and sintering procedures can be followed. Since powders prepared as described (except for the addition of 1 w/o iron) have been sintered to greater than 95% theoretical density, no major difficulties are anticipated in achieving 90% of theoretical density. The major problems will be in obtaining a consistent density (the consistency will be affected by the preparation history), and in eliminating the necessity for grinding the pellets. If required, however, grinding can be carried out in a glove box.

Loading

Loading of the pellets into the rods will be complicated by the necessity of accurately crimping spacers into specified positions within the rods. The spacers serve to prevent gross fuel movements in the event of a power transient. The entire loading process, including crimping of the spacers, can be carried out semi-automatically within a glove box but this method

will require decontamination of the entire rod later. For this reason, a loading machine has been designed (conceptual stage only) which requires that only the tip of the rod be inserted into the glove box.

The loading procedure requires sodium bonding. Bonding can be accomplished by backfilling heated and evacuated rods with liquid sodium at the reactor site.

However, as the present design requires that the assembled rods be brazed at temperatures above the boiling point of sodium, the introduction of the liquid metal will follow brazing. Several methods of avoiding swelling of sodium loaded rods during brazing were considered but each introduced additional problems and hazards. As presently conceived, the major steps involved in the loading procedure would be:

1. Load pellets and crimp spacer
2. Decontaminate tip of rod
3. Weld special end cap
4. Inspect rods (normal inspection + α -check)
5. Assemble and braze rods
6. Return brazed assembly to protective enclosure
7. Cut tip of special end cap to expose vent hole
8. Evacuate, heat, and backfill rods with liquid sodium
9. Solder vent hole closed
10. Clean and inspect (α -check, leak tightness)

Development work will be required before more detailed procedures can be defined.

II.3.2.4 Fabrication of Fuel Assemblies

The fast breeder will use type 316 stainless steel fuel rods brazed to a hexagonal cross section assembly using ferrules for support and proper spacing. End plates will be fastened by means of locked threads or by welding to pre-selected outer fuel tubes or support rods. No straps (similar in construction to the Yankee assembly) or support grids welded

to containing cans will be used; support will be completely by the use of brazed ferrules.

The use of this type of assembly has the advantage of less pressure drop in the cooling medium than a grid type of assembly, but places an important burden on the integrity of the braze. A literature search was made to find a suitable braze material that would fulfill the following requirements:

1. Be corrosion resistant to liquid sodium.
2. Have acceptable joint integrity and freedom from cracks to insure sound joints.
3. Have a brazing temperature low enough so that grain growth of the base metal will not result in lowering of the mechanical properties.

Based on limited amounts of work performed at other sites, specifically Oak Ridge and Armour Research Foundation, as well as brazing development performed at Westinghouse Atomic Power Division, the brazing alloy of 80%Ni-10%P-10%Cr, coupled with a diffusion bond anneal to provide a ductile bond is recommended. An 88%Ni-3%P-9%Si alloy with a diffusion bond anneal is recommended as a secondary braze alloy. The corrosion resistance of both should be acceptable although considerable development effort is necessary to prove this.

All of the work reported had been performed as part of the Aircraft Nuclear Power Program and although the material was required to resist liquid sodium at 1500°F in the event of an accident, oxidation resistance rather than sodium resistance was of prime importance. Because Inconel was the basic material, only static corrosion tests were performed on brazed stainless steel. Results are given in Table II.3.2-9 (61,62). The Ni-Si-Cr, Ni-P-Si, Ni-Si-Mn alloys, although showing little attack by the liquid sodium, are not considered acceptable for use because of the high brazing temperature necessary or because of observed crack formation.

TABLE II.3.2-9

Results of Static Tests of Experimental Brazing Alloys on Types 304 and 310 Stainless Steel in Liquid
Sodium at 1500°F for 100 Hours (Static Test)

Brazing Alloy Composition wt %	Base Metal (SS)	Brazing Temperature °F	Weight Change		Metallographic Notes
			grams	%	
73Ni- 9Si-18Cr	304	2150	0.0	0.0	No attack along surface of braze fillet.
88Ni- 3P - 9S	310	2050	0.0	0.0	No attack along surface of fillet. Several cracks observed in fillet.
82Ni-10Si- 8Mn	310	2000	-0.0002	-0.018	No attack along fillet. Several cracks observed in fillet.
78Ni-16Si- 6Mn	310	2175	0.0004	0.043	No evidence of attack. Several cracks observed in fillet.
87Ni-13Si	304	2150	-0.0007	-0.068	Surface of braze fillet unattacked.
77Ni-10P-13Cr	304	1850	0.0004	0.054	Less than 0.5 mils of small sub-surface voids.
64Ni-7 Si-30Mn	310	1950	-0.0002	-0.022	One mil erratic surface attack.
80Ni- 9P-11W	310	2000	-0.0002	-0.019	2.5 mil uniform attack along the surface of the braze fillet.
54Ni-10Si-14Cr 19Fe- 3Mo	310	2175	0.0	0.0	Sub-surface voids to a depth of 3 mils along fillet.
89 Ni- 5P- 6Si	310	2000	0.0	0.0	Maximum attack of 4 mils along surface of fillet.
80Ni-11P- 9Si	304	2050	0.0	0.0	Sub-surface voids to a depth of 4 mils along fillet.
89Ni- 8P- 3Si	310	1900	-0.0012	-0.14	Maximum attack of 4 mils along surface of braze fillet.
38Ni-57Mn- 5Cr	310	2000	-0.0004	-0.043	Attack in the form of stringers to a maximum depth of 5 mils not uniform.

The Ni-P-Si alloy, although exhibiting cracks, may be worthy of further consideration if diffusion treatments, as described later in this section, prevent and correct such brittle behavior.

Additional braze materials on Inconel were tested in see-saw tests. In these tests, the sample was encapsulated with sodium in an Inconel tube. The Inconel tube was then placed in a rocking furnace in such a manner that a temperature difference of 400°F existed between the hot zone of 1500°F and the cold zone. The furnace was rocked more than two times per minute to allow mass transfer to occur. Although these were not loop tests, they were the closest equivalent to a loop test on any braze material. These results are shown in Table II.3.2-10 (61,62). Most of these braze alloys contain boron, which, because of the $\text{B}^{10} + \text{n}^1 \longrightarrow \text{Li}^7 + \text{He}^4$ reaction, will cause their integrity under irradiation to be questionable. These alloys, therefore, are not recommended.

The Ni-P alloy placed on components by chemical plating was studied at Oak Ridge (63). This brazing alloy, subsequently used for Yankee, was not sufficiently corrosion resistant to static liquid sodium for application. Stainless steel samples with the nickel phosphorus alloys were electroplated with chromium and then submitted to the Oak Ridge braze cycle (5 minutes at temperature). The corrosion resistance of this Ni-Cr-P alloy was considerably enhanced in static sodium although it was not tested in either see-saw or loop tests. Oak Ridge also objected to the brittleness of this material. Nevertheless, this alloy was one of those recommended by Oak Ridge. This alloy will be discussed in greater detail subsequently.

Subcontracts were later issued to Armour Research Foundation to develop an oxidation and liquid sodium resistant braze material (64,65). Unfortunately, the Armour study did not include tests for corrosion resistance in liquid sodium; corrosion data were used as previously published in the Liquid Metals Handbook (66) and the Welding Journal (62). The outstanding alloy of a total of 92 tested was a mixture of Ni-Cr-Ge which tended to liquefy, i.e., upon heating, a low melting point composition melted and flowed away leaving a skeletal residue that did not melt.

TABLE II.3.2-10

Brazing Alloys in Inconel T Joints Seesaw Tested in Liquid Sodium
for 100 Hours at a Hot Zone Temperature of 1500°F

<u>Brazing Alloy Composition, %</u>	<u>Weight Change</u>		<u>Metallographic Notes</u>
	<u>grams</u>	<u>%</u>	
91.2Ni-4.5Si-2.9B-1.4Fe	-0.0011	-0.073	No attack along surface of braze fillet.
82.1Ni-7.0Cr-4.5Si-2.9B 3.5Fe	-0.0009	-0.071	One mil erratic attack along surface of braze fillet.
83.4Ni-6.0Cr-5.0Si-3.0B 2.5Fe-0.1C	-0.0007	-0.051	Subsurface voids to a maximum depth of 1.5 mils along surface of braze fillet.
93.2Ni-3.5Si-1.9B-1.4Fe	-0.0012	-0.077	1.5 mil very erratic surface attack along fillet.
70Ni-13Ge-11Cr-6Si	-0.0023	-0.139	Non-uniform attack along surface of braze fillet to a depth of 2.5 mils.
50.0Ni-11.8Si-29.3Fe 3.5P-5.4Mo	-0.0069	0.622	2.5 mil uniform attack along surface of fillet.
70Ni-10Si-20Cr	-0.0018	-0.163	3 mil uniform attack along braze fillet.
73.2Ni-13.5Cr-4.5Si 3.5B- 4.5Fe-0.8C	0	0	Very erratic string attack to a maximum depth of 4 mils along surface of braze fillet.
65Ni-25Ge-10Cr	-0.0019	-0.113	Intermittent surface attack to a maximum depth of 4 mils along braze fillet.

The electroless Ni-P plate was successfully used as a braze for Yankee. The microstructure of this alloy when brazed for only five minutes, as reported by Oak Ridge and confirmed in initial studies at APD (67,68), reveals two distinct phases: a light-colored region rich in nickel and a darker-colored region, the brittle Ni_3P phase. Because the Ni_3P phase has little ductility, King of APD held the brazement at brazing temperatures for various lengths of time, up to 16 hours. As the brazement was held at temperatures for various lengths of time, diffusion of the phosphorus into the stainless cladding occurred. This allowed the formation of a more ductile, corrosion resistant, single phase braze that covered the bond gap. This diffusion treatment also worked for Micro-braze 50 (80Ni-10P-10Cr) which was subsequently used for repair brazes.

As previously stated, Oak Ridge electroplated chromium on to the Ni-P chemical plate. When heated to melting, as for brazing, the chromium-nickel-phosphorus mixed with no loss of braze coverage. Microstructures of this alloy after exposure to liquid sodium at 1500°F show preferential attack of the Ni_3P phase with little attack of the Ni rich phase. The presence of chromium reduced the amount of attack. Therefore, it is reasonable that if a Yankee style of subassembly were to be made, ferrules chemically plated with Ni-P and subsequently electroplated with chromium could be used to apply the desired braze alloy. The use of the diffusion bonding heat treatment would give a ductile single phase that should be more corrosion resistant. However, experimental evidence is required to test this assumption.

Based on the above description of diffusion treatment giving a ductile single phase, the 88%Ni-3%P-9%Si alloy should be considered as a secondary choice. The alloy was not attacked in 1500°F static sodium but did exhibit cracks because of the brittle Ni_3P phase. This brittleness should be corrected by the diffusion treatment. The high brazing temperature of 2050°F and the effort required for placement of the braze material relegate this alloy to a secondary position unless the corrosion resistance of the Ni-P-Cr alloy should prove unacceptable.

II.3.2.5 Consideration of Other Materials Requirements

This portion of the materials survey will include the following:

1. Control rods and mechanisms for their operation.
2. Pressure vessel, piping, pumps, valves, traps and heat exchangers.

Other core materials such as fuel cladding and assembly, fuels, and alternate fuel cladding materials are discussed in other sections. Materials for biological shielding such as magnetite high density concrete are beyond the scope of this survey and will not be discussed.

Control Rods

The control rods will be 316 stainless steel tubes containing natural (non-enriched) boron carbide powder. The B_4C powder will be vibratory compacted into 316 stainless steel tubes to an approximate density of 64% of theoretical. Under irradiation, the boron n, α reaction will generate lithium and helium. At the temperatures involved (1200°F coolant temperature plus gamma heating), up to 67% of the theoretical helium may be released depending on the % B^{10} burnup (70,71). The B_4C will also tend to fragment under irradiation. Venting of the generated helium will be accomplished without contact between the B_4C and sodium coolant to preclude the possibility of carbon transport or entrainment of B_4C fragments.

The control rod drive mechanisms will use a threaded shaft (race or mechanical jack) of cold worked Haynes 25 with Stellite No. 3 balls or jacks. These materials are extremely hard (Rockwell C of 55 to 60) and have been used successfully in the sodium graphite experiment, where because of gamma heating, the temperature of operation was expected to be 1200°F (72). It is important that the Haynes 25 be cold worked (and ground to size) as it is necessary to have two hard surfaces in contact with each other to avoid fretting or wear. Since these are both cobalt-base alloys, it is desirable to keep wear with subsequent particle release

to a minimum. Other materials normal for this purpose in pressurized water reactors, such as 17-4 PH or Inconel X, would not be satisfactory because these alloys either age at the temperature of operation or lose their strength under irradiation (73).

Pressure Vessel and Piping

The main tank and piping will be an austenitic stainless steel, probably type 304. Although type 316 has been extensively tested in liquid sodium at 1200°F, there is little reason to believe that any significant differences in mass transfer would be observed in type 304 SS. In those areas where higher strength is necessary, the thickness of the 304 SS may be increased, or type 316 SS may be substituted. Welding of the type 304 should provide no major problems. Atomics International (74) used type 304 stainless steel for the main tank of the Hallam Reactor. The loading face shield and core clamp assembly were made from type 405 ferritic steel because of its low coefficient of expansion and its good welding characteristics.

Piping will be type 304 and will be field welded. Weld surfaces exposed to liquid sodium will be made with inert gas shielding. The remainder of the weld would be completed using manual shielded metal arc processes.

Tanks, Valves and Pumps

Atomics International reported that for the sodium graphite reactor, all material for tanks, valves, and pumps, except for facing materials and packing is type 304 stainless steel (72). Stainless steel cannot be used for facing materials as there is a strong tendency for self welding or diffusion bonding to occur with conditions of intimate contact under pressure in the liquid metal. Under these conditions, the stainless steel will tend to weld together in sodium at temperatures much lower than those required for normal resistance welding. For example, after an exposure of 30 days at 1300°F in liquid sodium, 347 stainless steel bonded to itself

with a bond strength of 28,500 psi (75). Because of this, the valves for the Hallam Nuclear Power Facility used cobalt base alloys such as Stellite No. 6 and Stellite No. 12 to prevent galling of the moving parts and to provide wear resistance for valve seats (76). At Hallam, the temperature of operation is 1000°F. Self welding which would be anticipated above 1200°F was not expected to occur. High temperature graphitized asbestos reinforced with Inconel wire was used for stem packing. It is anticipated that these same materials will be used in the FBR depending on the design of the valves or gates, although wear and corrosion tests will have to be made to evaluate these materials for use at 1200°F.

Hot, Cold and Carbon Traps

The use of hot and cold traps in the external piping system reduces the oxygen and hydrogen contents of the liquid sodium. The cold trap removes sodium oxide and sodium hydride by taking advantage of the temperature dependence of their solubility in sodium. The SRE uses toluene-cooled cold traps operating at 231°F to precipitate the sodium oxide and filter it onto the stainless steel mesh. Two of these cold traps can reduce a 100 ppm oxide concentration in 52,000 lbs of sodium to 10 ppm in 58 hours (72).

The hot trap used for the SGR operates at a higher temperature than ambient sodium coolant temperature and includes a large surface area of zirconium sheet exposed to the sodium (72). A small stream of the primary sodium loop passes through the hot trap where the zirconium getters the oxygen from the sodium.

The carbon trap at the Hallam Reactor is an insulated type 316 stainless steel can containing 28,800 square feet of 20 mil stainless steel foil (77). As with the hot trap, a small stream of sodium from the primary loop is heated and preferentially carburizes the foil rather than the other component parts of the core.

Intermediate Heat Exchanger

Both the intermediate heat exchangers for the SGR (72) and for Hallam (77) were made from type 30⁴ stainless steel. Because of the lower cost of 30⁴, the FBR will also use type 30⁴ stainless steel. Welding junctions of 30⁴ to 316 piping will use type 30⁴ filler metal.

REFERENCES

Section II.3.2

1. Osborne, M. F., Long, E. L., Jr., Gas Cooled Reactor Program Quarterly Progress Report for Period ending March 31, 1962, ORNL 3302, September, 1962.
2. Neimark, L. A., Kittel, J. H., Crothers, C. C., Irradiation of PuC and UC-PuC, paper presented at Symposium on Carbides in Nuclear Energy, Harwell, November 5-7, 1963.
3. Hahn, R. D., A Study of Uranium Carbide and Cladding Materials for High Temperature Sodium Cooled Reactors, NAA-SR-7696, August, 1962.
4. Hare, A. W., Rough, F. A., The Effect of High Burnup Irradiation on Massive Uranium Carbide, BMI 1491, January 1961.
5. Strasser, A., personal communication, September, 1963.
6. Crane, J. and Gordon, E., Effects of Fabrication and Composition on the Irradiation Stability of Uranium Carbide, paper presented at Symposium on Carbides in Nuclear Energy, Harwell, November 5-7, 1963.
7. Bradbury, B. T., Cole, J. E., Frost, B. R. T., Lambert, J. D. B., Irradiation Effects in U-C and (U, Pu)-C Fuel Pellets, paper presented at Symposium on Carbides in Nuclear Energy, Harwell, November 5-7, 1963.

Section II.3.2.1.1

8. Progress Report 29, Jan 1963, MSAR-63-33, "Effect of 1200°F Sodium on Austenitic and Ferritic Steel".
9. Progress Report 33, May 1963, MSAR-63-91, "Effect of 1200°F Sodium on Austenitic and Ferritic Steel, Physical Properties of Materials".

10. Sticha, "Structural Stability of Commercial Wrought Austenitic Steels for Power Plant Piping to 1450°F", American Power Conference, March 1960.

General References

In the discussion of the behavior of stainless steel at elevated temperatures, several general references have been examined and are listed without specific reference in the text.

- a. Metals Handbook, 8th Edition, Vol. 1, "Properties and Selection of Metals" Published A.S.M.
- b. W. F. Summens and H. C. Cross, "The Elevated Temperature Properties of Stainless Steel", ASTM Special Technical Publication No. 12A.
- c. M. T. Simnad, "The Stability and Properties of Alloy Steels at Elevated Temperatures", General Dynamics Corporation GAMD-1769.
- d. R. E. Schreiber, "Mechanical Properties of Irradiated Stainless Steels, A Compilation of Data in the Literature", D.P. 579.

Section II.3.2.1.2

11. Liquid Metals Handbook, Second Edition, Atomic Energy Commission and Department of the Navy, Washington 25, D. C., June 1952.
12. A. de Brasunas, "Liquid Metal Corrosion, Corrosion, Volume 9, 1953, pp 78-84.
13. E. E. Hoffman, "Liquid Metal Corrosion," Symposium of Corrosion Fundamentals University of Tennessee Press, 1956.
14. Liquid Metals Handbook, NaK Supplement, Atomic Energy Commission and Department of the Navy, Washington 25, D. C., July 1955.

15. B. R. Frost, "Advances in Liquid Metal Technology," Journal of Nuclear Materials, Volume 7, (1962) No. 2, p 109.
16. E. G. Brush, and R. F. Koenig, "Behavior of Materials in Non-aggressive Liquid Metals," Nuclear Metallurgy, AIME, No. 2, February 1956, pp 21-32.
17. L. S. Darken and R. W. Gurry, The Physical Chemistry of Metals, Mc Graw-Hill, N. Y., N. Y. 1953.
18. J. M. McKee, "Sodium Corrosion as a Function of Time," Preprint V-114, ASME Conference on Nuclear Engineering and Science, April 6-9, 1959, Engineers Joint Council, 29 W. 39th Street, N. Y., N. Y., 1959.
19. W. R. Holman, "Mass Transfer by High Temperature Liquid Sodium, Interim Progress Report 1," AECU 4071, February 1958.
20. W. R. Holman, "Mass Transfer by High Temperature Liquid Sodium, Interim Progress Report 2", AECU 4072, October 1958.
21. R. W. Lockhart, G. Billuris and M. R. Lane, "Sodium Mass Transfer: I Test Loop Design," GEAP 3725, June 1962.
22. F. J. Hetzler and R. S. Young, "Sodium Mass Transfer: II Screening Test Data and Analysis, Volume 2, Metallurgy," GEAP 3726, June 1962.
23. L. E. Pohl, "Sodium Mass Transfer: IV, 1962 Corrosion Sample Data," GEAP 4181, January 1963.
24. R. W. Lockhart, Verbal Discussion of paper by R. W. Lockhart and G. Billuris, "Materials Selection Consideration for FCR and SEFOR," Nuclear Metallurgy, Volume IX, AIME, N. Y., N. Y., 1963, presented at The Symposium on Materials for Sodium Cooled Reactors, 1963, Winter Meeting of the American Nuclear Society.

25. L. F. Epstein, "Static and Dynamic Corrosion and Mass Transfer in Liquid Metal Systems," Chemical Engineering Progress Symposium Series, No. 53, p 67.
26. J. G. Gratton, "Solubility of Carbon in Sodium at Elevated Temperatures," KAPL 1807, June 30, 1957.
27. W. C. Hayes and O. C. Shepard, "Corrosion and Decarburization of the Ferritic Chromium-Molybdenum Steels in Sodium Coolant Systems" NAA-SR-2973, December 1958.
28. W. J. Anderson and G. V. Sneesby, "Carburization of Austenitic Stainless Steels In Liquid Sodium," NAA-SR-5282, September 1960.
29. F. J. Hetzler and R. S. Young, "Sodium Mass Transfer: II, Screening Test Data and Analysis, Volume 2 - Metallurgy," GEAP 3726, June 1962.
30. B. A. Webb, "Carburization of Austenitic Stainless Steel by Uranium Carbides in Sodium Systems," NAA-SR-6246, May 31, 1963.
31. W. J. Anderson, "Removal of Carbon from Liquid Sodium Systems," NAA-SR-6386, 1961.
32. "Hallam Nuclear Power Facility, Reactor Operations Analysis Program Semiannual Progress Report No. 1, September 1, 1962 through February 28, 1963," NAA-SR-8401, July 1963.
33. Reactor Materials, US AEC, Government Printing Office, Washington, D. C., Vol. 6, Number 3, pp 46-47, August 1963.

Section II.3.2.1.3

34. F. M. Heck, private communication, June 21, 1963.
35. D. Okrent, "Nuclear Considerations in Selection of Materials", Nuclear Metallurgy, Volume 9, American Institute of Mining, Metallurgical and

Petroleum Engineers, New York 17, N. Y., 1963.

36. C. R. Tipton, Jr., Reactor Handbook, Volume 1, Materials, Interscience Publishers, Inc., New York, New York, 1960.
37. R. K. Wagner and H. E. Kline, "High Strength Zirconium Alloys", Transactions of the ASM, Cleveland, Ohio, Volume 53, pp. 713-727, 1960.
38. Reactor Materials, Superintendent of Documents, Washington 25, D. C., Volume 6, No. 3, August 1963.
39. K. F. Smith and R. J. Van Thyne, "Selected Properties of Vanadium Alloys for Reactor Application", ANL-5661, May 1957.
40. B. R. Rajala and F. C. Holtz, "Improved Vanadium Based Alloys, Final Report for Period December 1, 1960 to November 30, 1961," ARF-2210-6, 1961.
41. B. R. Rajala and F. C. Holtz, "Improved Vanadium Based Alloys, Progress Reports 1-6 for 1962," ARF-2231-1 through 6, 1962.
42. T. E. Tietz and J. W. Wilson, "Mechanical, Oxidation and Thermal Property Data for Seven Refractory Metals and Their Alloys, Final Report," NP-11108, 1961.
43. V. M. Sinclair, R. A. N. Pool and A. E. Ross, "Impurities in Liquid Metal Coolant and Their Effect on the Fuel Canning Materials Niobium and Vanadium," Nuclear Reactor Chemistry, Second Conference, Gatlinburg, Tenn., October 10-12, 1961, TID-7622.
44. R. E. Machery, W. R. Simmons, and R. A. Noland, "Materials for Argonne National Laboratory Sodium Cooled Reactors", Nuclear Metallurgy, Volume 9, American Institute of Mining, Metallurgical and Petroleum Engineers, New York 17, N. Y., 1963.

45. Personal communication, Sherman Greenberg to D. B. Scott, 11/19/63.

Section II.3.2.2

46. Keller, Donald L., "Predicting Burnup of Stainless-UO₂ Cermet Fuels", Nucleonics Vol. 19, No. 6, June, 1961.

Section II.3.2.3

47. J. A. Leary, LASL, personal communication.
48. J. A. Leary, W. J. Moramon, W. N. Miner, F. W. Schonofeld, LAMS 2949, Quarterly Status Report on Solid Plutonium Fuels Program for period to June 30, 1963.
49. J. Henney, N. A. Hill, D. T. Livey, AERE-R 4175, A Review of Data on Uranium Carbides in the Light of Recent Phase Equilibrium Studies.
50. The Uranium-Carbon and Plutonium Carbon Systems, IAEA Technical Report Series No. 14, February, 1963.
51. A. Strasser, D. Stahl, Properties of Solid Solution Uranium Plutonium Carbides, paper presented at Harwell Conference, November, 1963.
52. Uranium Ceramic Fuels, US/UK Research Newsletter No. 15, Jan, 1963.
53. A Study of Uranium Carbide and Cladding Materials for High Temperatures Sodium Cooled Reactors, R. D. Hahn, NAA SR 7696, August 15, 1962.
54. R. W. Dayton and C. R. Tipton, Jr., Progress Relating to Civilian Applications, BMI 1324, February, 1959.

Section II.3.2.3.2

55. B. A. Webb, Carburization of Austenitic Stainless Steel by Uranium Carbides in Sodium System, NAA SR 6246, May 31, 1963.

56. R. D. Hahn, A Study of Uranium Carbide and Cladding Materials for High Temperature Sodium Cooled Reactors, NAA SR 7696, August, 1962.
57. W. C. Hayes, Uranium Carbide Development Program at Atomics International, 4th Carbide Conference, May 20, 21, 1963.
58. R. E. Macherey, Simmons, W. R., Noland, R. A., Materials for Argonne National Laboratory Sodium Cooled Reactors, Nuclear Metallurgy, Vol. 9, Materials for Sodium Cooled Reactors, 1963.
59. ANL 6658, Reactor Development Program Progress Report, November 1962.
60. J. Crane, United Nuclear Research, personal communication.

Section II.3.2.4

61. E. E. Hoffman, P. Patricia, C. F. Leiten, Jr., and G. M. Slaughter, "An Evaluation of the Corrosion and Oxidation Resistance of High Temperature Brazing Alloys," ORNL-1934, 1956.
62. G. M. Slaughter, C. F. Leiten, Jr., P. Patricia, E. E. Hoffman, and W. D. Manly, "Sodium Corrosion and Oxidation Resistance of High Temperature Brazing Alloys," Welding Journal, V.36, pp. 217S-225S, May 1957.
63. P. Patricia, G. M. Slaughter and W. D. Manly, "Electroless Plated Brazing Alloys," ORNL-2243, April 1957.
64. D. A. Canonico and H. Schwartzbart, "Development of Oxidation and Liquid Sodium Resistant Brazing Alloys," WADC-TR-57-648, 1957.
65. D. A. Canonico and H. Schwartzbart, "Development of Oxidation and Liquid Sodium Resistant Brazing Alloys," Welding Journal, Vol. 39, pp. 122S-128S, March 1960.

66. Liquid Metals Handbook, Second Edition, Atomic Energy Commission and Department of the Navy, Washington, D. C., 1952.
67. P. P. King, "Joining Fuel Rods into Subassemblies for YAEC Fuel Elements," YAEC-108, 1958.
68. P. P. King, "The Formation and Statistical Evaluation of Diffusion Bonded Brazed Joints in Yankee Subassemblies," YAEC-160, 1959.
69. Ibid.

Section II.3.2.5

70. E. W. Hoyt, "Information on the Use of Boron Carbide as a Nuclear Control and Poison Material," GEAP 3680, 1961.
71. W. D. Valovage, "Effect of Irradiation on Hot Pressed Boron Carbide," KAPL-1403, 1955.
72. R. R. Hayward, "Sodium Graphite Reactor Materials Survey," NAA-SR-4013, September, 1959.
73. C. G. Collins, G. L. Hammons, F. C. Robertshaw and W. H. Coutts, "Radiation Effects on Stress Rupture Properties of Inconel X and A-286", APEX 676, 1961.
74. J. E. Mahlmeister, W. V. Haberer, D. F. Casey, J. Susnir, T. Ricci, and W. S. Peck, "Engineering and Constructing the Hallam Nuclear Power Facility Reactor Structure," NAA-SR-7366, 1962.
75. E. G. Bush and R. Koenig, "Behavior of Materials in Nonaggressive Liquid Metals," Nuclear Metallurgy, Vol 2, AIME, N.Y., N.Y., 1956.
76. B. Brooks, R. Galantine, and F. Bergonzoli, "The Selection, Design Modification and Analysis of Sodium Valves for Hallam Nuclear Power Facility," NAA-SR-5463, 1960.

77. O. J. Faust, C. L. Larson, R. M. Mills, D. I. Monnie, R. J. Walters, O. G. Jenkins, Jr., and R. R. Steins, "Hallam Nuclear Power Facility-Reactor Operations Analysis Program Semiannual Progress Report No. 1, September 1, 1962 through February 28, 1963, NAA-SR-8401, July 1, 1963.

III. COST ANALYSIS

III.1 GROUND RULES

The following basic ground rules have been set by the AEC for this study.

Depleted uranium shall be assumed to contain 0.3 wt.% U^{235} .

Fuel reprocessing costs are those of the AEC as published in the Federal Register.

Shipping is to be by rail between St. Louis, Missouri, as the reactor site, and Augusta, Georgia, as the reprocessing site.

Interest rate on fuel fabrication costs is assumed to be 6% of the book value. The book value of the fabrication costs is assumed to decrease linearly from the initial value to zero during the period of the fuel's residence in the core.

Ownership of both fissionable and fertile material is assumed to rest with the AEC.

III.2 FUEL CYCLE AND COST COMPONENTS

III.2.1 Equilibrium Fuel Cycle

The mixtures of isotopes utilized in the reference fuel cycle are those characteristic of the equilibrium cycle. The equilibrium cycle. (Figure III.2.1-1) is achieved when the fresh core fuel for two succeeding cycles is identical and is achieved solely by blending spent blanket fuel with spent core fuel. Fresh blanket fuel is always depleted uranium.

There are two alternate equilibrium fuel cycles. The one illustrated requires processing the radial blanket separately from the core and axial blanket, followed by blending with the core only that amount of radial blanket Pu required to replace the net burnup. The alternate cycle processes all fuel simultaneously, so that the excess fuel for sale is identical in isotopic composition with that used in the reactor.

The alternate procedure has two major advantages. First, the total cost of processing is reduced (when processing cost is enrichment dependent), because the average enrichment is lowered by the mixing process. (This assumes that the processor will accept the risk of inadequate mixing.) Second, new reactors will achieve equilibrium conditions quickly because the available fuel will have equilibrium composition.

The alternate procedure has several disadvantages that stem primarily from the fact that the equilibrium fuel will be higher in Pu^{239} . This produces a smaller β_{eff} , a higher critical mass, and a lower breeding ratio. Furthermore, with the price schedule proposed, wherein Pu^{240} has no value, one would actually be giving away a fertile material (Pu^{240}) which is more desirable than the U^{238} needed to replace it.

The portion of the total processing charge due directly to chemical processing, exclusive of losses and conversion charges (which would not be altered), is small at reference design conditions; thus the potential saving on processing is of the order of 0.03 to 0.05 mills/kwh. This is further offset by an increase in inventory and a reduction in Pu credit.

Figure III.2.1-1 Equilibrium Fuel Cycle

The zero value assumed for Pu^{240} , the reduced β_{eff} , and the limits this places on radial blanket life make this alternate undesirable.

The radial blanket material, as can be seen in Figure III.2.1-1, is depleted UO_2 , rather than depleted UC. The rapid oxidation of UC on exposure to moist air, with the evolution of acetylene, requires inert atmosphere handling. As depleted UO_2 does not impose this requirement, handling, pelletizing and loading are less expensive for depleted UO_2 than for depleted UC. Production costs of UO_2 are also less than for UC. On these bases, UO_2 has an advantage over UC for radial blanket use.

The axial blanket presents a different problem. The coolant fraction in the axial blanket is set by the core requirements, rather than by the blanket requirements. Consequently, the volume fraction available for axial blanket material is only about one-half that available for radial blanket material; thus the competition for neutrons between structure and fertile material is enhanced in the axial blanket. This is partially compensated for by the greater metal density in UC compared to UO_2 ; i.e., the neutron economy of the axial blanket is more strongly influenced by the difference in metal density between UC and UO_2 than is that of the radial blanket.

It is less expensive to fabricate the axial blanket by extending the core clad tubes than to fabricate completely separate assemblies. Handling operations are reduced, the number of end fittings is reduced, and some structure is eliminated. The axial blanket material will then be exposed to the same sodium bond as the core fuel. If UO_2 were used in the axial blankets, release of oxygen into the sodium bond would result in oxidation of the (U, Pu)C in the core, with the consequent swelling of the fuel and the release of carbon. This carbon would be transferred to the clad and would reduce its ductility. As a result of all the above considerations, the reference axial blanket material is therefore depleted UC.

The exact quantities of the various fuel isotopes vary with the fuel management procedure because of variations in the flux spectrum as seen by different fuel assemblies. These variations are small and have a

negligible effect on the fuel cycle cost. The particular set of values used for the fuel cycle analysis is characteristic of the Roundelay fuel management scheme whereby fuel is replaced on a partial basis (one-third or one-sixth) in a pattern over the entire core. The average core composition is thus identical after each refueling.

The fuel inventory, in and out, is shown in Table III.2.1-1. These values may be altered somewhat by different fuel management procedures, by different control systems, or by other design variations which affect the fuel. The basic design is sufficiently well established, however, that gross changes in the inventory data are unlikely. Thus fuel cycle data based on the values in Table III.2.1-1 are reasonable representations of the ultimate fuel cycle of this type.

III.2.2 Inventory

The fuel inventory of any nuclear power plant consists of an "active" inventory (the fuel in the reactor) plus an "inactive" inventory -- e.g., the fuel in storage and fabrication.

The most important part of the total inventory cost in a fast breeder is the fissile plutonium inventory. Even though there is about 20 times as much U^{238} as there is fissile plutonium, the difference in values of \$3 per kg for U^{238} versus \$10,000 per kg for fissile plutonium is overwhelming. The uranium inventory value is of the order of 0.6% of the plutonium inventory value, therefore the inventory cost is almost independent of the cost of uranium. Doubling the uranium cost would increase total inventory by only about 0.6%. Similarly, when core changes are being studied, the effect on inventory of changing the uranium content can be neglected.

The value of fertile plutonium has been taken to be zero. If fertile plutonium, Pu^{240} , is valued with respect to U^{238} the error in neglecting the value of Pu^{240} can be seen to be negligible. As a breeding material, Pu^{240} is worth 2.5 to 3 times as much as U^{238} ; i.e., the ratio of capture cross sections is such that 2.5 to 3 times as many U^{238} nuclei as Pu^{240} nuclei are required to achieve a given breeding rate. As a neutron emitter,

TABLE III.2.1-1

Equilibrium Fuel Cycle Inventory

	<u>Core plus Axial Blanket, kg</u>		<u>Radial Blanket, kg</u>	
	<u>In</u>	<u>Out</u>	<u>In</u>	<u>Out</u>
Pu ²³⁹	3,469	2,943	--	2,067
Pu ²⁴⁰	1,527	1,506	--	76
Pu ²⁴¹	216	214	--	4
Pu ²⁴²	53	53	--	neg.
Total Pu	5,265	4,716	--	2,147
U ²³⁵	24	20	169	109
U ²³⁸	26,345	24,470	56,886	53,816
Total Fuel	31,634	29,206	57,055	56,072
Fissile fuel/total fuel	0.117	0.109	0.003	0.039

Note: These data are for 3-year life of both core and blanket.

Pu^{240} is also 2.5 to 3 times as good as U^{238} . Consequently, it seems reasonable to value Pu^{240} at 2.5 to 3 times the worth of U^{238} . With this range of value, and with the small quantity of Pu^{240} present relative to the quantity of U^{238} , complete neglect of the value of Pu^{240} is considered fully justified.

The magnitude of the "inactive" inventory cost depends upon the total length of time required for spent fuel cooling, shipping, processing, fabricating and storing. Although this total time may be reducible to 6 to 8 months, a one year out-of-pile period currently appears more reasonable. Part of this inventory time might be reduced, however, if several similar plants were to be refueled at different times during the year and exchanged their fuels.

At some penalty to the fuel cycle cost, out-of-pile time will be taken to be one year or the nearest even number of refueling periods. The out-of-pile inventory is one-third the core inventory, plus five percent for spare assemblies, making a total of 0.35 of the core inventory for one-year refueling, or 0.34 for six-month refueling.

III.2.3 Fabrication

There is currently no experience in the large-scale manufacture of ceramic fast breeder fuel containing plutonium. Fabrication estimates are therefore based on expectation rather than upon experience. A detailed study has been performed for the AEC of the fabrication cost of mixed PuC-UC fuels (1). With appropriate modification, this data will be used in estimating the cost of core and axial blanket assemblies.

The cost of depleted uranium blanket assemblies will be taken as a flat \$50/kg U for large volume production. This is consistent with the reference cited above, and with postulated future prices for slightly enriched fuel.

The fabrication cost of plutonium-bearing fuel is sensitive to the throughput of the fabricating facility. The fabrication cost in \$/kg appears to vary approximately as the annual throughput to the $(-1/3)$ power. Doubling the annual throughput should reduce the fabrication cost by about 20%.

The axial blanket material is identical with the core region material except that no plutonium is present. For a given annual throughput, a fabrication facility not handling plutonium would have costs 10 to 15% lower than a facility handling plutonium. A large part of this difference rests with the cost of plutonium losses. This fact, combined with the variation of cost with plant capacity, makes it most economical to use a common production facility for core and axial blanket material.

A fabrication plant capable of handling about 10,000 kg per year of uranium plus plutonium as carbide would be required to provide the replacement fuel required by one 1000 MWe plant of the reference design at 100 MWD/kg operating at high load factor. Such a facility would require three years to manufacture the first core, and would not be built in a nuclear economy with 1000 MWe plants. However, to provide an estimate of the highest anticipated fabrication cost, the fabrication cost in this size plant will be used as an upper limit. A fabrication plant having a capacity of 20,000 kg per year is not beyond the capability of a single large utility installing two or more large plants within two or three years of each other. Such a plant would require about 18 months for the first core loading. The fabrication cost to be expected of a 20,000 kg/yr will be used as a reference value. For an optimistic value, the cost characteristic of a plant of 30,000 kg/yr will be taken.

The cost of carbide powder preparation from nitrates is calculated exactly as recommended (1). The losses represent 25% depleted uranium, and 75% mixed uranium and plutonium in the total annual throughput. The resulting values are shown in Table III.2.3-1 along with the resulting cost in \$/kg of uranium plus plutonium in core plus axial blanket.

Pelletization is handled similarly, and is shown in Table III.2.3-2.

Rod encapsulation and assembly operations require minor modification from the basic system. The rod length and diameter are similar, but brazing and sodium-filling require additional equipment and personnel. Machinists were added to the personnel list as they were omitted in the reference (1). The results are shown in Table III.2.3-3. The brazing furnace facilities

were included at \$17,000, and sodium back-filling equipment at \$40,000 for the base plant (53,000 rods/yr). Total direct-charge personnel were increased to 24 for the base plant.

Estimated product materials costs appear in Table III.2.3-4 and follow the reference for cladding and end caps. Spacers, sodium, and cermet rods have been added; and bundle hardware costs were increased by 50% because the costs derived from the reference were considered too low.

The resulting reference fabrication cost is \$189/kg. High and low estimates are \$240/kg and \$168/kg, respectively.

Table III.2.1-1

CARBIDE POWDER PREPARATION

Item	<u>Annual Charges</u>		
	<u>10,000 kg/yr</u>	<u>20,000 kg/yr</u>	<u>30,000 kg/yr</u>
Equipment	41,026	61,058	77,484
Buildings	37,771	48,900	56,912
Personnel	128,802	144,940	155,486
O & M	122,351	210,037	291,785
Subtotal	329,950	464,935	581,667
G & A (15%)	49,492	69,740	87,250
Losses (0.5%)	58,388	116,775	175,163
Subtotal	437,947	651,683	844,430
Overhead (56%)	245,250	364,942	472,881
Total \$/yr	683,197	1,016,625	1,317,311
Total \$/kg	67.53	50.25	43.40

- Notes: (1) Personnel and O & M include indirect and direct charges.
 (2) Buildings include administrative charges.
 (3) Losses assume 75% core, 25% axial blanket.
 (4) \$/kg corrected for 1.167% in cermet rods.

Table III.2.1-2

CARBIDE PELLETIZATION

Item	<u>Annual Charges</u>		
	<u>10,000 kg/yr</u>	<u>20,000 kg/yr</u>	<u>30,000 kg/yr</u>
Equipment	18,718	29,730	39,017
Buildings	53,626	69,410	80,783
Personnel	215,474	296,905	362,269
O & M	30,703	46,700	59,992
Subtotal	318,521	442,745	542,061
G & A (15%)	47,778	66,412	81,309
Losses (0.5%)	58,388	116,775	175,163
Subtotal	424,804	626,165	798,883
Overhead (56%)	237,890	350,652	447,374
Total \$/yr	662,694	976,817	1,246,257
Total \$/kg	65.50	48.28	41.06

- Notes:
- (1) Personnel and O & M include indirect and direct charges.
 - (2) Buildings include administrative charges.
 - (3) Losses assume 75% core, 25% axial blanket.
 - (4) \$/kg corrected for 1.167% in cermet rods.

TABLE III.2.3-3

Encapsulation and Assembly

Item	<u>Annual Charge</u>		
	<u>20,000 rods/yr</u>	<u>40,000 rods/yr</u>	<u>60,000 rods/yr</u>
Equipment	103,437	147,544	186,002
Buildings	31,433	194,791	246,284
Personnel	164,916	224,793	272,285
O & M	25,705	40,922	54,133
Subtotal	325,491	460,506	572,702
G & A (15%)	48,824	69,076	85,905
Subtotal	374,315	529,582	658,607
Overhead (56%)	209,616	296,566	368,820
Total \$/yr	583,931	826,148	1,027,427
Total \$/1000 rods	29,197	20,654	17,124
Total \$/assembly	7,007	4,957	4,110
Total \$/kg	55.74	39.43	32.69

- Notes:
- (1) Personnel and O & M include indirect and direct charges.
 - (2) Buildings include administrative charges.
 - (3) Rod numbers are based on half-length rods in CEX assembly.
 - (4) \$/kg corrected for 1.167% in cermet.
 - (5) 20,000 half-length rods correspond approximately to 10,000 kg.

TABLE III.2.3-4

Estimated Product Materials Cost

<u>Item</u>	<u>\$/1000 rods</u>
Cladding	928
End Caps	1,476
Spacers	800
Bundle Hardware	9,183
Sodium (\$0.20/lb)	25
Cermet Rods (\$160 each)	4,667
Subtotal	17,079
Overhead (56%)	9,564
Total \$/1000 rods	26,643
Total \$/assembly	6,394
Total \$/kg	50.86

TABLE III.2.3-5

Fabrication Summary, \$/kg

	<u>10,000 kg/yr</u>	<u>20,000 kg/yr</u>	<u>30,000 kg/yr</u>
	High Value	Reference Value	Low Value
Powder Preparation	67.53	50.25	43.40
Pelletization	65.50	48.28	41.06
Encapsulation & Assembly	55.74	39.43	32.69
Product Materials	50.86	50.86	50.86
Total	240.	189.	168.

REFERENCES

1. Collins, G. D., "Comparative Study of PuC-UC and $\text{PuO}_2\text{-UO}_2$ as Fast Reactor Fuel. Part II - Economic Considerations" GEAP-3880, Nov. 15, 1962.
2. Zebroski, E. L., Collins, G. D., and Alter, H. W., "Plutonium Fuel Processing and Fabrication for Fast Ceramic Reactors" GEAP-3876, Feb. 1, 1962.
3. Collins, G. D., "Fabrication Cost Estimate for UO_2 and Mixed $\text{PuO}_2\text{-UO}_2$ Fuel" GEAP-3824, Jan. 24, 1962.

III.2.4 Processing Cost

The chemical processing of irradiated fuel from core and blanket, to decontaminate and recover fertile and fissile material, will be priced on the basis of the standard AEC plant as published in the Federal Register.

These costs are based on a hypothetical plant with a maximum throughput of 1000 kg of uranium per day. The plant throughput as a function of U-235 enrichment is specified as shown in Table III.2.4-1, with the values based on initial enrichment of the fuel material. This initial enrichment basis was originally intended for burner reactors. Thorium base fuels are processed on the basis of final concentrations of fissile material, rather than initial.

Plutonium fast breeder fuel is not explicitly included in the AEC plant. Since fast breeder fuel may contain either more or less fissile material after irradiation than before irradiation, it appears reasonable to propose that fast breeder fuel be processed on the basis of the fissile content as delivered to the processor, although this is the more expensive overall arrangement.

Using the AEC plant, in which throughput is set by reactivity, the difference between the reactivity worth of plutonium and uranium must be considered. Since the number of neutrons per absorption is approximately the same for U-235 and fissile plutonium, an approximation to the relative effective worth can be found by taking the ratio of the 2200 m/sec fission cross sections. The approximate relative worth of Pu-239 is $750/580=1.29$, and that of Pu-241 is $1100/580=1.90$.

The axial blanket region forms an integral part of each core assembly and therefore must be processed with the core region. It is assumed that the head end mixing is adequate for the mixed enrichment to set the throughput.

The radial blanket region consists of separate assemblies of oxide rather than carbide. It is assumed that the radial blanket will be processed

separately from the core. Since two different head ends would be required, it would be possible to dissolve the oxide blanket and the carbide fuel separately and then blend them before further processing. In such a case, the throughput would be set by the mixture; therefore, this possibility has not been used. It will be assumed that no cleanup or "turnaround" time is required after processing the radial blanket if the core is processed immediately following.

With refueling intervals of six months or longer, batch sizes are sufficiently large that "turnaround" time will be eight days for all cases.

A daily charge of \$17,500 will be used for the plant.

TABLE III.2.4-1

Separation Plant Capacity vs. Enrichment

<u>Initial Fuel Enrichment*, w/o U-235</u>	<u>Separation Rate, kg U/day</u>
93	44
80	50
60	65
50	78
40	98
30	132
25	161
20	215
15	310
10	537
8	740
7	781
6	825
5	875
4	930
3 or less	1,000

*Linear interpolation is permitted.

III.2.5 Shipping

Shipping costs for highly irradiated fuels can only be approximated because no such fuel has yet been shipped commercially. Vented, sodium bonded fuel increases the uncertainty. Fortunately, the best estimates of shipping produce only small contributions to total fuel cycle cost. Errors of as much as 50% in shipping cost estimates represent cost differentials of only 0.01 to .02 mills/kwh.

Based on Yankee shipping cask experience, it is estimated that a rail cask adequate for seven core assemblies or 19 blanket assemblies would weigh about 100 tons and cost approximately $\$5 \times 10^5$. This would include a cooling capacity of 75 to 100 kw, depending on the on-site storage time.

Allowing 21 days for a round trip between power plant and processing plant, and including turn-around time at both ends, one such cask would be adequate. To provide for unplanned down-time and to reduce the total recycle time, it is considered more reasonable to specify employment of two such casks. These would be shared with other plants to further reduce recycle time. Two casks, at 14% annual carrying charge, will therefore be fully charged to the fuel cycle.

An allowance of \$10,000/yr will be made for maintenance of each cask.

The fuel handling operation for cask loading in the plant will be performed by the regular O & M crew. Estimating that this will require one additional man, an allowance of \$8,000/yr will be made for manpower.

The core assemblies are vented and bonded with sodium which cannot be removed at the plant. The assemblies will be individually sealed into returnable canisters containing a low-melting metal alloy (probably lead-antimony) before insertion into the cask. Such an alloy, having no density change on solidifying, will not damage the fuel assembly if it solidifies. An allowance of \$200 per trip is estimated as adequate to cover make-up of metal alloy expended in unloading the fuel.

The freight charge is estimated to be \$5027 per trip, based on estimated rail freight from St. Louis, Missouri to Augusta, Georgia. There is a small difference in the weight between 19 blanket assemblies and 7 core assemblies in canisters, but this will be neglected for estimating purposes.

The blanket assemblies are unvented and are not sodium bonded. It is assumed they will be shipped immersed in the cask coolant.

III.2.6 Working Capital

The AEC has specified that the interest on fuel fabrication, i.e., the working capital, shall be computed at ~~6%~~ per year on the book value. The book value is assumed to decrease linearly to zero over the in-core residence time. Assuming simple interest on working capital, the above then represents ~~6%~~ on one-half the total fabrication cost. That is, at the middle of any year, there will be 1/3 of the core with 5/6 of initial value, 1/3 with 1/2 the initial value, and 1/3 with 1/6 the initial value, or a total of one-half the initial value. This calculation is for annual refueling, although refueling semi-annually would yield similar results.

The fuel cycle cost for working capital can therefore be expressed as 0.5×0.06 times the fuel cycle fabrication cost.

The total working capital charge will be computed as 0.030 times the fuel cycle cost for fabrication.

III.2.7 Fuel Components

The fuel in a fast breeder reactor divides naturally into three regions; core, axial blanket, and radial blanket. Requirements placed upon the fuel in these three regions are different. It is therefore essential to distinguish the individual contributions to the fuel cycle cost made by these three distinct regions in order that the relative importance of changes in each region can be ascertained. It is also instructive to determine the order of magnitude of each contribution to the total fuel cycle cost in order

that the effect of changes can be rapidly estimated.

In the fuel cycle calculations for all-uranium fueled reactors, it is customary to include the cost of converting $U(NO_3)_2$ to UF_6 in the chemical processing cost because the spent uranium must be returned to the diffusion plant for re-enrichment. In the fast breeder cycle, this is not done because only depleted (or natural) uranium is used. Therefore, chemical processing can be considered essentially complete when decontaminated $U(NO_3)_2$ is achieved. The conversion from $U(NO_3)_2$ to UO_2 or to UC is included in the fabrication cost, replacing the UF_6 to UO_2 conversion included in fabrication for all-uranium reactors.

Similarly, because plutonium is to be recycled back into a reactor as a ceramic, it is not correct to convert plutonium into metal form. The processing cycle is therefore considered complete when decontaminated $Pu(NO_3)_4$ is obtained. Conversion from $Pu(NO_3)_4$ to PuO_2 or to PuC is included in the fabrication cost, along with that for the $U(NO_3)_2$ conversion.

In the following material, the criterion used was that any factor contributing less than 0.0005 mills/kwh may be safely neglected. The uncertainty in the total fuel cycle cost is greater than 0.05 mills/kwh, so that the quantities thus neglected form less than 1% of the total uncertainty. The final fuel cost figures will be rounded to the nearest 0.01 mills/kwh, although this degree of precision implies more accuracy than is considered justified. Thus the neglected quantities, of the order of 0.0005 and less, would need to be from 10 to 20 in number to alter the final figures. They are not this numerous.

III.2.7.1 Core

The following development illustrates the arithmetic used in evaluating FBR cores. The numerical examples given are solely to illustrate order-of-magnitude and are not to be construed as representing the actual values for a particular reactor. Efficiency will be taken to be 40%.

1. Fabrication

The fabrication cost can be expressed as:

$$\frac{F_c}{24\eta B} \text{ mills/kwh} = \frac{0.1041 F_c}{B} \text{ mills/kwh}$$

where F_c = \$/kg metal in core for core fabrication
 B = Average MWD_{th} from reactor per kg metal in core
 η = Plant thermal efficiency

For F_c = \$200/kg, η = 0.40, B = 100 MWD/kg.

$$\frac{200}{24 \times 0.40 \times 100} = 0.2085 \text{ mills/kwh}$$

2. Inventory

- a. The active uranium inventory charge resulting from the inventory charges while in active use can be expressed as:

$$\frac{U(1-e)}{8760\eta f P_s} \text{ mills/kwh} = \frac{0.00001355 U(1-e)}{f P_s} \text{ mills/kwh}$$

where U = \$/kg price of uranium
 P_s = (MW_{th} from reactor)/(kg metal in core)
 i = annual carrying charge for fuel = 0.0475
 f = Plant load factor
 e = (kg total plutonium)/(kg U plus Pu in core)

For $U = \$3/\text{kg}$, $P_s = 0.1 \text{ MW}_{\text{th}}/\text{kg}$, $f = 0.80$, $e = 0.2$

$$\frac{0.00001355 \times 3 \times 0.8}{0.8 \times 0.1} = 0.0004 \text{ mills/kwh}$$

The active plutonium inventory charge resulting from the inventory charges while in active use can be expressed as:

$$\frac{P_i e^*}{8760 f P_s} \text{ mills/kwh} = \frac{0.00001355 P_e^*}{f P_s} \text{ mills/kwh}$$

where $P = \$/\text{kg}$ price of plutonium

$e^* = (\text{kg fissile Pu})/(\text{kg metal in core})$

For $P = 10^4 \$/\text{kg}$, $P_s = 0.1 \text{ MW}_{\text{th}}/\text{kg}$, $f = 0.80$, $e^* = 0.15$

$$\frac{0.00001355 \times 10^4 \times 0.15}{0.8 \times 0.1} = 0.254 \text{ mills/kwh}$$

- b. The inactive uranium inventory charge resulting from the inventory charges while in storage, fabrication, etc., can be expressed as:

$$\frac{U t (1-e)}{24 \eta B} \text{ mills/kwh} = \frac{0.00495 U t (1-e)}{B} \text{ mills/kwh}$$

where $t =$ total time in years.

For $U = \$3/\text{kg}$, $t = 1 \text{ yr.}$, $B = 100 \text{ MWD/kg}$, $e = 0.20$

$$\frac{0.00495 \times 3 \times 1 \times 0.80}{100} = 0.000119 \text{ mills/kwh}$$

The inactive plutonium inventory can be expressed as:

$$\frac{P_i t e^*}{24 \eta B} \text{ mills/kwh} = \frac{0.00495 P_t e^*}{B} \text{ mills/kwh}$$

For $P = 10^4 \$/kg$, $t = 1 \text{ yr.}$, $e^* = 0.15$, $B = 100 \text{ MWD}_{th}/kg$

$$\frac{0.00495 \times 10^4 \times 1 \times 0.15}{100} = 0.0742 \text{ mills/kwh}$$

Note that the core uranium inventory charge is less than 0.2% of the core plutonium charge. Since the value of e^* (or the critical mass) cannot be calculated to an accuracy of 1%, the neglect of the core uranium inventory charge is justified.

3. Burnup

The charge for loss of uranium can be expressed as:

$$\frac{(1-e)Uu}{24\eta B} \text{ mills/kwh} = 0.1041 \frac{Uu(1-e)}{B} \text{ mills/kwh}$$

where u = fraction of uranium lost

For $U = \$3/kg$, $u = 0.10$ (high), $B = 100 \text{ MWD}_{th} \text{ kg}$, $e = 0.2$

$$\frac{0.8 \times 0.1041 \times 3 \times 0.10}{100} = 0.00025 \text{ mills/kwh}$$

This can be neglected.

The charge for loss of fissile plutonium can be expressed as:

$$\frac{e^* P p}{24\eta B} \text{ mills/kwh} = \frac{0.1041 P p e^*}{B} \text{ mills/kwh}$$

where p = fractional loss of fissile plutonium

For $P = 10^4 \$/kg$, $e^* = 0.15$, $p = 0.20$, $B = 100 \text{ MWD}/kg$

$$\frac{0.1041 \times 10^4 \times 0.20 \times 0.15}{100} = 0.312 \text{ mills/kwh}$$

4. Shipping

The shipping cost can be expressed as:

$$\frac{S_c}{24\eta B} \text{ mills/kwh} = 0.1041 \frac{S_c}{B} \text{ mills/kwh}$$

where S_c = shipping cost in \$/kg metal for core.

For $S_c = 12.5$ \$/kg, $B = 100$ MWD/kg

$$\frac{0.1041 \times 12.5}{100} = 0.013 \text{ mills/kwh}$$

5. Processing

a. The direct processing charge can be expressed as:

$$\frac{D}{24 B} \left(\frac{1}{r} + \frac{8}{T} \right) \text{ mills/kwh} = \frac{0.1041 D}{B} \left(\frac{1}{r} + \frac{8}{T} \right) \text{ mills/kwh}$$

where D = daily charge for processing plant

r = daily throughput of plant in kg

T = total kg of U plus Pu per refueling

This is correct for $\frac{1}{r} \geq \frac{8}{T}$. For $\frac{2}{T} \leq \frac{1}{r} < \frac{8}{T}$, replace $(\frac{1}{r} + \frac{8}{T})$

with $(\frac{2}{r})$, and for $\frac{1}{r} < \frac{2}{T}$, replace $(\frac{1}{r} + \frac{8}{T})$ with $(\frac{1}{r} + \frac{2}{T})$.

For $D = 17,500$ \$/day, $B = 100$ MWD/kg, $T = 8000$ kg, $r = 250$ kg/day

$$\frac{0.1041 \times 17500}{100} \left(\frac{1}{250} + \frac{8}{8000} \right) = 0.090 \text{ mills/kwh}$$

b. The loss of uranium in the processing cycle may be expressed as:

$$\frac{(1 - e) (1 - u) U L_u}{24 B} \text{ mills/kwh} = \frac{0.1041 U L_u (1 - u) (1 - e)}{B}$$

Where L_u = fraction of uranium

For $U = \$3/\text{kg}$, $L_u = 0.02$, $B = 100 \text{ MWD/kg}$, neglecting u and e .

$$\frac{0.1041 \times 3 \times 0.02}{100} = 0.0000625 \text{ mills/kwh}$$

This can be neglected.

The loss of fissile plutonium may be expressed as:

$$\frac{e^* (1 - p) P L_p}{24 B} \text{ mills/kwh} = \frac{0.1041 (1 - p) P L_p e^*}{B} \text{ mills/kwh}$$

where L_p = fractional loss of plutonium

For $p = 0.2$, $P = 10^4$, $L_p = 0.02$, $e^* = 0.15$, $B = 100 \text{ MWD/kg}$.

$$\frac{0.1041 \times 0.8 \times 10^4 \times 0.02 \times 0.15}{100} = 0.025 \text{ mills/kwh}$$

6. Plutonium Credit

The plutonium credit is included in the charge for burnup for the core. For the data used in the example, there is a net positive charge for plutonium burnup. There is, of course, a credit for production concealed in the burnup charge.

7. Fuel Rod Diameter Optimization

Economic optimization of rod size is highly desirable. Unfortunately, it depends very strongly on the relationship between rod diameter and fabrication cost. This relationship is not yet well established, hence optimization can at best indicate whether or not a chosen value of rod diameter is a reasonable selection.

The total fuel cycle cost, f , can be written as:

$$f = \frac{0.1041 F}{B} + \frac{0.1596e^*}{P_s} + M \quad (\text{at } 85\% \text{ load factor})$$

where $F = \$/\text{kg}$ for core plus axial blanket fabrication

$M =$ costs which do not vary with rod diameter

$$\frac{\partial f}{\partial d} = \frac{0.1041}{B} \frac{\partial F}{\partial d} + \frac{0.1596}{P_s} \frac{\partial e^*}{\partial d} + 0.1596e^* \frac{\partial}{\partial d} \left(\frac{1}{P_s} \right)$$

where $d =$ fuel rod diameter

The optimum value of d is defined by

$$\frac{\partial f}{\partial d} = 0 = \frac{0.1041}{B} \frac{\partial F}{\partial d} + \frac{0.1596}{P_s} \frac{\partial e^*}{\partial d} - \frac{0.1596e^*}{P_s^2} \frac{\partial P_s}{\partial d}$$

or

$$\frac{\partial F}{\partial d} + \frac{1.53B}{P_s} \frac{\partial e^*}{\partial d} - \frac{1.53Be^*}{P_s^2} \frac{\partial P_s}{\partial d} = 0$$

For a small variation about a reference fuel diameter, d_o , the following assumptions are approximately correct:

1. The enrichment is constant, $e^* = e_o^*$
2. The kw/ft are constant, $P_s = P_{so} d_o^2/d^2$
3. The $\$/\text{ft}$ for fabrication are constant, $F = F_o d_o^2/d^2$

where the zero subscript refers in each case to reference design values.

Inserting these values in the equation for optimum yields

$$\frac{-2 F_o d_o^2}{d^3} + \frac{1.53 e_o^* B d^4 2 P_{so} d_o^2}{P_{so}^2 d_o^4 d^3} = 0$$

$$d^4 = d_o^4 \left(\frac{P_{so} F_o}{1.53 B e_o^*} \right)$$

$$d = d_o \left(\frac{P_{so} F_o}{1.53 B e_o^*} \right)^{1/4}$$

For the approximate reference conditions previously used; $P_{so} = 0.1 \text{ MW/kg}$,

$B_o = 100 \text{ MWD/kg}$, $F_o = \$200/\text{kg}$, $e_o^* = 0.15$, this yields

$$d = \left(\frac{0.1 \times 200}{1.53 \times 100 \times 0.15} \right)^{1/4} d_o = 0.966 d_o$$

From this value it may be concluded that the reference fuel diameter is not significantly far from optimum if it results in approximately the values used as reference conditions.

III.2.7.2 Axial Blanket

The following development illustrates the arithmetic used in evaluating axial blankets of FBR cores. The numerical examples given are solely to illustrate order-of-magnitude and are not to be construed as representing the actual values for a particular reactor. Efficiency will be taken to be 40%.

1. Fabrication

The fabrication cost can be expressed as:

$$\frac{AF_a}{24\eta B} \text{ mills/kwh} = \frac{0.1041 AF_a}{B} \text{ mills/kwh}$$

where $A = (\text{kg metal in blanket})/(\text{kg metal in core})$

$F_a = \$/\text{kg metal in blanket for blanket fabrication}$
(Incremental)

$B = \text{Average MWD}_{th} \text{ from reactor per kg metal in core}$

$\eta = \text{Plant thermal efficiency}$

For $A = 0.5$, $F_a = \$60/\text{kg}$, $\eta = 0.40$, $B = 100 \text{ MWD/kg}$,

$$\frac{0.5 \times 60}{24 \times 0.40 \times 100} = 0.031 \text{ mills/kwh}$$

2. Inventory

- a. The active inventory charge resulting from the inventory charges while in active use can be expressed as:

$$\frac{AU_i}{8760 \eta f P_s} \text{ mills/kwh} = \frac{0.00001355 AU}{f P_s} \text{ mills/kwh}$$

where $U = \text{\$/kg price of U metal}$

$P_s = (\text{MW}_{th} \text{ from reactor}) / (\text{kg metal in core})$

$i = \text{annual carrying charge for fuel} = 0.0475$

$f = \text{plant load factor}$

For $A = 0.5$, $U = \$3/\text{kg}$, $P_s = 0.1 \text{ MW}_{th}/\text{kg}$, $f = 0.80$

$$\frac{0.00001355 \times 0.5 \times 3}{0.1 \times 0.8} = 0.000254 \text{ mills/kwh}$$

- b. The inactive inventory charge resulting from the inventory charges while in storage, fabrication, etc., can be expressed as:

$$\frac{AUit}{24 \eta B} \text{ mills/kwh} = \frac{0.00495 AUt}{B} \text{ mills/kwh}$$

where $t = \text{total time in years.}$

For $A = 0.5$, $U = \$3/\text{kg}$, $t = 1 \text{ yr.}$, $B = 100 \text{ MWD/kg}$

$$\frac{0.00495 \times 0.5 \times 3 \times 1}{100} = 0.0000744 \text{ mills/kwh}$$

Note that the total magnitude of the axial blanket inventory is of the order of 0.0003 mills/kwh. This may certainly be neglected in terms of the total fuel cycle cost. For calculations relating to blanket only, inventory is only of the order of one percent of fabrication. As the uncertainty in fabrication is much greater than this, no real accuracy is

lost by neglecting axial blanket inventory in all calculations. Note that an increase in $U \times i$ by a factor of 10 would provide a blanket inventory contribution of less than 0.003 mills/kwh.

3. Burnup

The charge for loss of uranium can be expressed as:

$$\frac{AUu}{24\eta B} \text{ mills/kwh} = \frac{0.1041 AUu}{B} \text{ mills/kwh}$$

where u = fractional loss of uranium.

For $A = 0.5$, $U = \$3/\text{kg}$, $B = 100$, $u = 0.05$ (very high)

$$\frac{0.1041 \times 0.5 \times 3 \times 0.05}{100} = 0.0000781 \text{ mills/kwh}$$

This can be neglected in all calculations.

4. Shipping

The shipping cost can be expressed as:

$$\frac{AS_a}{24\eta B} \text{ mills/kwh} = \frac{0.1041 AS_a}{B} \text{ mills/kwh}$$

where S_a = incremental shipping cost in $\$/\text{kg}$ metal to ship axial blanket with core.

For $A = 0.5$, $S_a = \$5/\text{kg}$, $B = 100 \text{ MWD/kg}$

$$\frac{0.1041 \times 0.5 \times 5}{100} = 0.0026 \text{ mills/kwh}$$

5. Processing

a. For integral axial blankets, the presence of the axial blanket serves to lower the enrichment of the core plus blanket. For a processing plant whose daily throughput is controlled by criticality (e.g., the standard AEC plant), lowering of the enrichment raises the daily throughput. For typical FBR values, the increased throughput for core plus blanket is equal to or greater than the throughput required to process core plus axial blanket in the same number of days required to process the core only. Because the axial blanket thus receives a "free ride" through the processing plant, approximate calculations for this cost factor may be neglected. For exact calculations, this is best calculated by adjusting the core processing cost calculation by using the total mass of core plus blanket and average enrichment of core plus blanket.

b. The loss of U in the processing cycle may be expressed as:

$$\frac{(1-u) AU L_u}{24\eta B} \text{ mills/kwh} = \frac{0.1041 AU L_u(1-u)}{B} \text{ mills/kwh}$$

where L_u = total fractional loss of uranium

For $A = 0.5$, $U = \$3/\text{kg}$, $B = 100 \text{ MWD/kg}$, $L_u = 0.02$, neglecting u ,

$$\frac{0.1041 \times 0.5 \times 3 \times 0.02}{100} = 0.000031 \text{ mills/kwh}$$

This can be neglected.

- c. The loss of Pu in the processing cycle can be expressed as L_p times the Pu credit for the axial blanket, where L_p is the total fractional loss of Pu. The L_p here may have a different value than the L_u . The Pu loss is not negligible.

6. Plutonium Credit

The credit for the production of Pu can be expressed as:

$$\frac{AP}{24\eta B} \times (\text{Pu}^{\text{fissile}} \text{ produced per kg U in blanket}) \text{ mills/kwh}$$

where $P = \$/\text{kg}$, value of fissile Pu.

The $\text{Pu}^{\text{fissile}}$ produced per kg U in blanket can be approximately computed as:

$$\frac{gBC_a}{10^3 A}$$

where g = grams of core fissile material destroyed per MWD_{th}

C_a = axial blanket breeding ratio.

Then the credit is:

$$\frac{gPC_a}{24 \times 10^3} \text{ mills/kwh} = 0.1041 \times 10^{-3} gPC_a \text{ mills/kwh}$$

For $g = 1 \text{ gm/MWD}_{th}$, $P = \$10^4/\text{kg}$, $C_a = 0.05$

$$0.1041 \times 10^{-3} \times 1 \times 10^4 \times 0.05 = 0.052 \text{ mills/kwh} .$$

Axial Blanket Optimization

Combining all the axial blanket fuel cost factors, the total contribution is:

$$\left[\frac{0.1041 AF_a}{B} + \frac{0.1041 AS_a}{B} - 0.1041 \times 10^{-3} gPC_a (1-L_p) \right]$$

(assuming all Pu produced is fissile) or

$$0.1041 \left[\frac{A}{B} (F_a + S_a) - gC_a (1-L_p) P \times 10^{-3} \right]$$

If this is set equal to zero, a relationship between the factors can be determined for which the net axial blanket cost is zero.

for $B = 100 \text{ MWD/kg}$, $g = 1 \text{ g/MWD}$, $L = 0.02$, $p = 10 \times 10^3 \text{ \$/kg}$:

$$\frac{C_a}{A} = \frac{F_a + S_a}{100 \times 1 \times 0.98 \times 10 \times 10^3 \times 10^{-3}}$$

$$\frac{C_a}{A} = \frac{F_a + S_a}{980}$$

The range of $(F_a + S_a)$ may be from \$25/kg to \$100/kg. Thus, the range of minimum C_a/A is from 0.025 to 0.102.

Preliminary calculations show that C_a/A can at least be obtained in this range. The next step is to optimize the total contribution of axial blanket to fuel cycle cost, requiring values of C_a as a function of A . The local breeding in a region dx about x , where x is the length of the axial blanket, is approximately expressed by:

$$\alpha e^{-\beta x} dx$$

The total breeding up to a distance x is given by

$$C_a(x) = \int_0^x \alpha e^{-\beta x} dx = \frac{\alpha}{\beta} (1 - e^{-\beta x}) .$$

Given two values of C_a at distance x_1 and x_2 , the values for α and β can be determined.

$$C_a(x_1) = \frac{\alpha}{\beta} (1 - e^{-\beta x_1})$$

$$C_a(x_2) = \frac{\alpha}{\beta} (1 - e^{-\beta x_2})$$

$$\frac{C_a(x_1)}{C_a(x_2)} = \frac{1 - e^{-\beta x_1}}{1 - e^{-\beta x_2}}$$

From this expression β can be determined, and, knowing β , α is defined by:

$$\alpha = \frac{\beta C_a(x_1)}{1 - e^{-\beta x_1}} = \frac{\beta C_a(x_2)}{1 - e^{-\beta x_2}}$$

Note that for the reference core module, $A \simeq 2x/6 = x/3$.

The factor 2 is employed because there are two axial blankets; and the factor 6 is used because the reference core is 6 feet long. An integral blanket is assumed so that core and blanket masses are both simply proportional to length.

For optimization, the values used for C_a should be the difference in total breeding with and without blanket, rather than the actual breeding in blanket. The difference results from changes in core breeding with blanket thickness.

For an integral blanket on a reference core module, we have the following values:

<u>x</u>	<u>A</u>	<u>$C_a(x) - C_a(0)$</u>
0	0	0
0.75 ft.	0.25	0.0486
1.50 ft.	0.50	0.0636

$$\frac{1-e^{-0.75\beta}}{1-e^{-1.50\beta}} = \frac{0.0486}{0.0636} = 0.764$$

$$\beta = 1.56$$

$$\alpha = \frac{1.56 \times 0.0636}{0.903} = 0.110, \quad \frac{\alpha}{\beta} = 0.0705.$$

$$\text{Then } C_a(x) = 0.0705(1-e^{-1.56x}) = 0.0705(1-0.21^x)$$

$$C_a(A) = 0.0705(1-0.00935^A).$$

Then assuming: $(1-L_p) = (1 - 0.02) = 0.98$

$$P = 10 \times 10^3 = 10,000 \text{ \$/kg}$$

$$g = 1\text{g/MWD}$$

The blanket contribution to fuel cycle cost becomes:

$$0.1041 \left[\left(\frac{F_a + S_a}{B} \right) A - 0.691(1 - 0.00935^A) \right]$$

The maxima can be determined by the standard technique:

$$\left(\frac{F_a + S_a}{B} \right) + 0.691 \ln(0.00935) 0.00935^A = 0$$

$$A = \ln \frac{- (F_a + S_a)/B}{0.691 \ln(0.00935)} \ln(0.00935).$$

From this we may also determine the value of $(F_a + S_a)/B$ for which the maximum credit occurs when $A = 0$. That is, we can determine the value of the parameter for which no axial blanket is economically feasible. The value of A is zero when

$$\frac{- (F_a + S_a)/B}{0.691 \ln(0.00935)} = 1$$

or the maximum value of $(F_a + S_a)/B$ for which the blanket credit is not negative is

$$\frac{F_a + S_a}{B} = - 0.691 \ln(0.00935) = 3.2 .$$

The absolute magnitude of this value should not be taken as accurate. The assumption on the breeding properties of the axial blanket is not exact; but the important thing to note is that the value is not large. For very low burnup fuels (e.g., certain metallic alloys) it is very unlikely that use of axial blankets could be justified unless they

could be transferred from a spent fuel assembly to a fresh fuel assembly.

The axial blanket contribution to fuel cycle cost can be plotted as a function of A with $(F_a + S_a)/B$ as a parameter. Figure III.2.7-1 is such a plot of mills/kwh versus A (and blanket thickness). The actual value of $(F_a + S_a)/B$ for a future FBR is difficult to determine with precision. For carbide fuels, a reasonable range is probably from 0.50 to 0.75 at about 100 MWD/kg and appears as the hatched band of Figure III.2.7-1.

Note that in all cases the maxima are flat, i.e., the fuel cycle credit varies slowly for small variations about the optimum blanket length. Thus, an optimum chosen for one set of cost data will not be extremely far off if the costs change somewhat. Similarly, trial calculations show that the optima are not very sensitive to small changes in breeding.

Using the assumption that $(F_a + S_a)/B$ will fall in the range of 0.50 to 0.75, an axial blanket length of one foot is recommended for the reference core module.

Figure III.2.7-1 Fuel Cost Savings vs. Axial Blanket Size

III.2.7.3 Radial Blanket

The following development illustrates the arithmetic used in evaluating radial blankets of FBR cores. The numerical examples given are solely to illustrate order-of-magnitude and are not to be construed as representing the actual values for a particular reactor. Efficiency will be taken to be 40%.

1. Fabrication

The fabrication cost can be expressed as:

$$\frac{R F_r}{n \cdot 24 \eta \cdot B} \text{ mills/kwh} = \frac{0.1041 R F_r}{n \cdot B} \text{ mills/kwh}$$

where $R = (\text{kg metal in blanket})/(\text{kg metal in core})$

$F_r = \$/\text{kg metal in blanket for blanket fabrication}$

$B = \text{Average MW}_{th} \text{ from reactor per Kg metal in core}$

$\eta = \text{Plant thermal efficiency}$

$n = \text{Number of core cycles in one blanket assembly life}$

For $R = 2.5$, $F_r = \$50/\text{kg}$, $\eta = 0.40$, $B = 100 \text{ MW}/\text{kg}$, $n = 1$

$$\frac{2.5 \times 50}{24 \times 0.40 \times 100} = 0.130 \text{ mills/kwh}$$

2. Inventory

a. The active inventory charge resulting from the inventory charges while in active use can be expressed as:

$$\frac{R U_i}{n \cdot 8760 \cdot \eta f P_s} \text{ mills/kwh} = \frac{0.00001355 R U}{P_s f n} \text{ mills/kwh}$$

where $U = \$/\text{kg price of U metal}$

$P_s = (\text{MW}_{th} \text{ from reactor})/(\text{kg metal in core})$

$i = \text{annual carrying charge for fuel} = 0.0475$

f = plant load factor

For R = 2.5, U = \$3/kg, $P_s = 0.1 \text{ MW}_{th}$, n = 1, f = 0.80

$$\frac{0.00001355 \times 2.5 \times 3}{0.1 \times 0.8} = 0.00127 \text{ mills/kwh}$$

- b. The inactive inventory charge resulting from the inventory charges while in storage, fabrication, etc., can be expressed as:

$$\frac{RUit}{n \ 24 \ \eta B} \text{ mills/kwh} = \frac{0.00495 \ RUt}{B \ n} \text{ mills/kwh}$$

where t = total time in years.

For R = 2.5, U = \$3/kg, t = 1 yr., B = 100 MWD/kg, n = 1

$$\frac{0.00495 \times 2.5 \times 3 \times 1}{100} = 0.000302 \text{ mills/kwh}$$

The total magnitude of the radial blanket inventory is of the order of 0.0016 mills/kwh, and may certainly be neglected in all but the most exact calculations. For calculations relating to blanket only, inventory is only of the order of one percent of fabrication and the uncertainty in fabrication is much greater than this. Therefore, no real accuracy is lost by neglecting radial blanket inventory in such calculations unless the magnitude of R increases substantially or n falls below unity. Note that an increase in U x i by a factor of 10 would be a total blanket inventory contribution of less than 0.016 mills/kwh.

3. Burnup

The charge for loss of uranium can be expressed as:

$$\frac{RUu}{n \ 24 \ \eta B} \text{ mills/kwh} = \frac{0.1041 \ RUu}{n \ B} \text{ mills/kwh}$$

where u = fractional loss of uranium.

For $R = 2.5$, $U = \$3/\text{kg}$, $B = 100$, $u = 0.05$ (very high), $n = 1$

$$\frac{0.1041 \times 2.5 \times 3 \times 0.05}{100} = 0.0003905 \text{ mills/kwh}$$

This can be neglected in all calculations provided R/n is less than about three.

4. Shipping

The shipping cost can be expressed as:

$$\frac{RS_r}{n^{24} \eta B} \text{ mills/kwh} = \frac{0.1041 RS_r}{B n} \text{ mills/kwh}$$

where S_r = incremental shipping cost in $\$/\text{kg}$ metal to ship radial blanket

For $A = 2.5$, $S_r = \$10/\text{kg}$, $B = 100 \text{ MWD/kg}$, $n = 1$

$$\frac{0.1041 \times 2.5 \times 10}{100} = -.027 \text{ mills/kwh}$$

5. Processing

- a. Mixing of the radial blanket with core plus axial blanket for reprocessing would effect a reduction in total processing cost when processing plant throughput is critically controlled, as it is in the standard AEC plant. This credit will not be used here because safe operation of a processing plant may not permit the assumption of complete mixing, and because the use of two different materials (oxides and carbides) may require different process head-ends.

The direct processing charge can be expressed as

$$\frac{R}{n} \frac{D}{24 \eta B} \left(\frac{1}{r} + \frac{8}{T} \right) \text{ mills/kwh} = 0.1041 \frac{RD}{nB} \left(\frac{1}{r} + \frac{8}{T} \right) \text{ mills/kwh}$$

where D = daily charge for processing plant

r = daily throughput of plant in kg

T = total kg of U per refueling

This is correct for $\frac{1}{r} \geq \frac{8}{T}$. For $\frac{2}{T} \leq \frac{1}{r} < \frac{8}{T}$,

replace $(\frac{1}{r} + \frac{8}{T})$ with $(\frac{2}{r})$, and for $\frac{1}{r} < \frac{2}{T}$,

replace $(\frac{1}{r} + \frac{8}{T})$ with $(\frac{1}{r} + \frac{2}{T})$.

For R = 2.5, D = \$17,500/day, n = 1, B = 100 MWD/kg,

T = 20,000 kg, r = 1000 kg/day

$$0.1081 \frac{2.5 \times 17500}{100} (\frac{1}{1000} + \frac{8}{20000}) = 0.0637 \text{ mills/kwh}$$

NOTE: The term $\frac{8}{T}$ may be dropped when core and blanket are processed in succession.

- b. The loss of uranium in the processing cycle may be expressed as

$$\frac{(1-u) \text{ RU } L_u}{24 \eta \text{ Bn}} \text{ mills/kwh} = \frac{0.1041 (1-u) \text{ RU } L_u}{n \text{ B}} \text{ mills/kwh}$$

where L_u = total fractional loss of uranium in processing.

For R = 2.5, n = 1, U = \$3/kg, B = 100 MWD/kg, L_u = 0.02, neglecting u,

$$\frac{0.1041 \times 2.5 \times 3 \times 0.02}{100} = 0.00016 \text{ mills/kwh}$$

This can be neglected.

- c. The loss of plutonium in the processing cycle can be expressed as L_p times the Pu credit for the radial blanket (which see), where L_p

is the fractional loss of Pu. The value of L_p may be different from the value of L_u . The Pu loss is not negligible.

6. Plutonium Credit

The credit for the production of plutonium in the radial blanket can be expressed as:

$$\frac{R}{n} \times \frac{P}{24 \eta_B} \times (\text{Pu}^{\text{fissile}} \text{ produced/kg U in blanket}) \text{ mills/kwh}$$

where P = value of fissile plutonium, \$/kg

The $\text{Pu}^{\text{fissile}}$ produced/kg/U in the blanket can be approximately computed as:

$$\frac{g B C_r n}{10^3 R}$$

where g = grams of core fissile material destroyed/ MWD_{th}

C_r = radial blanket breeding ratio

then the Pu credit becomes approximately:

$$\frac{g P C_r}{24 \eta 10^3} \text{ mills/kwh} = 0.1041 \times 10^{-3} g P C_r \text{ mills/kwh}$$

for $g = 1 \text{ gm/MWD}_{th}$, $P = 10^4 \text{ $/kg}$, $C_r = 0.8$

$$0.1041 \times 10^{-3} \times 1 \times 10^4 \times 0.8 = 0.833$$

Radial Blanket Thickness

The thickness of the radial blanket between modules is set by consideration of the sodium temperature coefficient. This was determined to be a thickness of two blanket assemblies around each module, with one assembly thickness of graphite between modules.

The thickness of radial blanket on the outside of the core does not exert a strong influence on the core characteristics, hence it can be determined by economics.

Physics studies of isolated modules showed that the addition of a third row of blanket assemblies would increase the total breeding ratio of the module by 0.10. Because the additional blanket would actually surround only one half of six of the seven modules, the effect on the total reactor would be approximately $0.1 \times 0.5 \times 6/7 = 0.043$. To achieve this gain would require increasing the number of blankets by 28.6%. These additional assemblies would remain in place about 4.5 times the average life of the remainder of the blanket. If the average blanket life is three years, the life of the outside assemblies would be 13.5 years, or nearly half the plant life.

If the plutonium credit of about 0.045 mills/kwh is reduced to its present worth as of the time when the average of the remainder of the blanket is available (10.5 years at 6%), the result is 0.024 mills/kwh. The additional fabrication cost would be approximately

$$\frac{0.130 \times 0.286}{4.5} = 0.008 \text{ mills/kwh,}$$

where the factor 4.5 is n for these assemblies. The shipping and reprocessing cost would be approximately

$$\frac{0.108 \times 0.286}{4.5} \times 0.54 = 0.004 \text{ mills/kwh,}$$

where the factor 0.54 is the present worth factor. The net gain would thus be 0.012 mills/kwh. Against this is the additional cost of reactor vessel and structure being 10+ inches greater in diameter, and the additional working capital requirements. It may not be possible to leave assemblies in place for this long a time.

If the additional assemblies were removed at the same time as the remainder of the blanket assemblies, then the additional costs would be $4.5 \times 0.012 = 0.057$ mills/kwh, resulting in a net cost of 0.030 mills/kwh.

The reference design, therefore, has only two rows of blanket assemblies surrounding each module. This seems to be near optimum and preserves the symmetry of each module.

It should be noted, however, that the additional breeding could be achieved at very low cost if intrinsically desirable.

III.2.7.4 Total Reactor

It is of interest to tabulate the order-of-magnitude fuel cycle values computed above to get a picture of the overall cycle.

TABLE III.2.7-1 Fuel Cycle Values

	<u>Core</u>	<u>Ax. Blanket</u>	<u>Rad. Blanket</u>	<u>Total</u>
Fabrication	0.209	0.031	0.130	0.370
Working Capital	0.007	0.001	0.006	0.014
Inventory (active)	0.250	--	--	0.250
(inactive)	0.075	--	--	0.075
Burnup	0.312	--	--	0.312
Shipping	0.013	0.003	0.027	0.043
Processing (direct)	0.091	--	0.064	0.155
(losses)	0.025	0.001	0.017	0.043
Pu Credit	<u>--</u>	<u>(0.052)</u>	<u>(0.833)</u>	<u>(0.885)</u>
Total	0.982	(0.016)	(0.589)	(0.377)

Thus with the ground rules used, and the fabrication costs assumed, the core can be expected to contribute a cost in the neighborhood of 1 mill/kwh, and the blanket in the neighborhood of minus 0.6 mills,kwh, for a net fuel cycle cost in the neighborhood of 0.4 mills/kwh.

From Table II.2.7-1, no one component of the fuel cycle cost is seen as inordinately large.

Residence time of the radial blanket for the reference design has been set by sodium reactivity coefficient considerations as determined by the average plutonium in the blanket. If the blanket breeding should ultimately prove to be less than presently calculated, this will permit a longer residence time. Consequently, a reduction in plutonium credit due to lower breeding than calculated can be compensated by a reduction in the positive components achieved through extension of the residence time. Thus the net blanket credit is only weakly dependent on the blanket breeding ratio.

Consolidated Fuel Cycle Charges

The separate components of the fuel cycle as detailed in Sections III.2.7.1 through III.2.7.3 are essential for optimization and parametric studies. For computation of the fuel cycle cost of a fixed design, it is convenient to consolidate the computations and make use of the calculated inventories in and out.

Fabrication - The fabrication cost is:

$$\frac{0.1041}{B} \left(F_c + A F_r + \frac{R F_r}{n} \right) = \frac{0.1041 (F(1+A) + R F_r/n)}{B} \text{ mills/kwh}$$

where F is the total fabrication cost of a core assembly with blanket divided by the total weight of uranium plus plutonium in the assembly.

Inventory - The inventory cost is:

$$\frac{0.1355 e^* Q}{f P_s} = \frac{0.1355 Q}{f P_s} \times \left(\frac{\text{Total kg fissile Pu in Core}}{\text{Total kg U + Pu in Core}} \right) \text{ mills/kwh}$$

where Q is one plus the fractional out-of-pile inventory. For annual refueling, Q is 1.35. For semi-annual refueling, Q is 1.175. This includes 5% spare assemblies in each case.

For completeness, the total uranium inventory may be included at:

$$\frac{0.00004065 Q}{f P_s} \times \frac{(\text{Total kg U in reactor})}{(\text{Total kg U + Pu in core})} \text{ mills/kwh}$$

Shipping - The shipping cost is:

$$\frac{0.1041}{B} (S_c + A S_a + R S_r/n) = \frac{0.1041}{B} (S(1+A) + R S_r/n) \text{ mills/kwh}$$

Where S is the shipping cost of core plus axial blanket per kilogram of total uranium plus plutonium.

Processing - The direct processing cost is:

$$\frac{0.1041 D}{B} \left(\frac{1}{r_1} + \frac{8}{T_1} + \frac{R}{nr_2} \right) = \frac{0.1041 D}{B} \frac{(\text{Total Days to Process One Cycle})}{(\text{Total kg U + Pu in Core/No. of Cycles})}$$

where the subscript 1 refers to core plus axial blanket values, and the subscript 2 refers to radial blanket values.

The losses are:

$$\frac{0.1041 PL_p}{B} \times \frac{(\text{Total kg fissile Pu out per cycle})}{(\text{Total kg U + Pu into core per cycle})} \text{ mills/kwh}$$

Note that only the processing plant loss is to be included here. The conversion loss cost is charged to fabrication.

Plutonium credit - The plutonium credit is:

$$0.1041 P \frac{(\text{Gross excess fissile Pu per cycle})}{(\text{Total kg U + Pu into core per cycle})} \text{ mills/kwh}$$

Note that there is no charge for burnup, as this is included in the plutonium credit.

Working Capital - The working capital charge is 0.031 times the total fabrication cost.

III.3 FUEL CYCLE COST

III.3.1 Reference Fuel Cycle

The reference reactor characteristics are summarized in Table III.3.1-1. The reactor is designed to provide 6 months full power operation between refuelings. At 85% load factor, this represents 7.05 months of operating time between refuelings.

Using the reference core and axial blanket fabrication cost of \$189/kg and a radial blanket cost of \$50/kg, the total dollars of fabrication per kg of core fuel (uranium plus plutonium) are

$$1.333 \times 189 + 2.41 \times 50/1.333 = \$342.34/\text{kg}$$

Hence, using the formula stated in Section III.2.7.4, the fabrication contribution to fuel cycle cost is

$$0.1041 \times 342.34/115.5 = 0.309 \text{ mills/kwh}$$

Working capital is then

$$0.031 \times 0.309 = 0.010 \text{ mills/kwh}$$

The plutonium inventory depends upon the recycle time through the factor Q, which is the ratio of total fuel cycle inventory to reactor inventory. For the reference cycle, with 7.05 months refueling and 14.1 months out of core, and with 5% spare assemblies, Q is 1.342. The plutonium inventory charge is then

$$(0.1355 \times 0.1557 \times 1.342)/(0.85 \times 0.1055) = 0.316 \text{ mills/kwh}$$

The uranium inventory cost is

$$\frac{0.00004065 \times 1.342}{0.85 \times 0.1055} \times \frac{83424}{23677} = 0.002 \text{ mills/kwh}$$

At 85% load factor, 10 cask-car trips per year are required carrying core assemblies, and 5 trips per year carrying blanket assemblies. Using two cask cars, this results in \$18.668/kg for core assemblies and \$6.847/kg for

TABLE III.3.1-1

Reference Reactor Data

Load Factor	0.85
Refueling Interval, months	7.05
Refueling Time	4 days
Number of Core Assemblies per Refueling	42
Number of Blanket Assemblies per Refueling	44 (or 45)
Plutonium Fraction in Core, Average	0.218
Fissile Fraction of Core (in), (e^*)	0.156
(out)	0.141
Fissile Fraction of Core + Axial Blanket (in)	0.116
(out)	0.108
Fissile Fraction of Radial Blanket with Reshuffling (in)	0.003
with Reshuffling (out)	0.051
Axial Blanket Weight/Core Weight, (A)	0.333
Radial Blanket Weight/Core Weight, (R)	2.41
Core Cycles/Blanket Cycle, (n)	1.333
Radial Blanket Assemblies/Core Assemblies	1.42
Core Specific Power	0.0915 MW/kg
Effective Core Specific Power (inc. blanket contribution), P_s)	0.1056 MW/kg
Core Burnup	100 MWD/kg
Effective Core Burnup (inc. blanket contribution), (B)	115.5 MWD/kg
Net Fissile Plutonium Production per Year (no losses)	438 kg
(w/losses)	412 kg
Core Inventory Doubling Time (85% load factor w/losses)	9 years
Total Doubling Time (14 months recycle time w/losses)	12 years
(7 months recycle time w/losses)	10.5 years
Total Fissile Plutonium Inventory	3,685 kg
Total Core Inventory (U + Pu)	23,677 kg
Total Uranium Inventory	83,424 kg
Net Excess Fissile Plutonium per Refueling after losses	240 kg

blanket assemblies. The total dollars of shipping per kilogram of core fuel (uranium plus plutonium) is

$$(1.333 \times 18.668) + (2.41 \times 6.847) = \$41.38/\text{kg},$$

and the shipping contribution to the fuel cycle cost is

$$0.1041 \times 41.38/115.5 = 0.037 \text{ mills/kwh.}$$

Adjusting the actual fissile enrichments upward for the greater reactivity of plutonium, 15.8 days are required for core processing per cycle, and 9 days for blanket. Adding 8 days turn-around time gives a total of 32.8 days of processing plant time. The contribution to fuel cycle cost is

$$\frac{0.1041 \times 17,500}{115.5} \times \frac{32.8}{23,677/6} = 0.131 \text{ mills/kwh}$$

The processing losses are

$$\frac{0.1041 \times 10^4 \times 10^{-2}}{115.5} \times \frac{871}{3946} = 0.020$$

The plutonium credit is

$$\frac{0.1041 \times 10^4 \times 240}{115.5 \times 3946} = 0.550 \text{ mills/kwh}$$

Summarizing the above yields:

Fabrication	0.309 mills/kwh
Working Capital	0.010
Pu Inventory	0.316
U Inventory	0.002
Shipping	0.037
Processing	0.131
Losses	0.020
Pu Credit	(0.550)
Net Fuel Cycle Cost	0.28 mills/kwh

If more pessimistic values are taken, an estimate of the worst possible cost can be obtained. Assume that fabrication is \$240/kg and \$60/kg for core and radial blanket assemblies respectively, that the load factor is 80%, the out-of-core time is two refueling periods (15 months); and processing cost is \$25,000/day. Then the fuel cycle cost becomes:

Fabrication	0.386 mills/kwh
Working Capital	0.013
Pu Inventory	0.336
U Inventory	0.002
Shipping	0.037
Processing	0.187
Losses	0.020
Pu Credit	(0.550)
Net Fuel Cycle Cost	0.43 mills/kwh

If more optimistic values are taken, an estimate of the best possible cost can be obtained. Assume fabrication is \$168/kg and \$40/kg for core and blanket assemblies respectively, that the load factor is 90%; the out-of-core time is reduced to one refueling period (6.6 months at 90% l.f.); losses are cut in half; and processing cost is reduced from \$17,500/day to \$15,000/day. The fuel cycle cost then becomes:

Fabrication	0.267 mills/kwh
Working Capital	0.008
Pu Inventory	0.262
U Inventory	0.002
Shipping	0.037
Processing	0.112
Losses	0.010
Pu Credit	(0.550)
Net Fuel Cycle Cost	0.15 mills/kwh

The range of fuel cycle cost thus estimated is 0.15 -- 0.28 -- 0.43 mills/kwhr or 0.28 ± 0.15 mills/kwh.

III.3.2 Parameter Variations

Using the equations developed in Section III.2, the effect of varying the important parameters can be determined. Figures III.3.2-1 through III.3.2-6 show the change in net fuel cycle cost from the reference design value of 0.28 mills/kwh resulting from a change in a parameter.

From Figure III.3.2-1 it can be seen that the design value is beyond the knee in the curve of specific power. A reduction of 24% in effective specific power increases fuel cycle cost by 0.1 mill/kwh, but an increase of 42% is required to reduce fuel cycle cost by 0.1 mill/kwh.

Figures III.3.2-2 and III.3.2-3 illustrate the sensitivity of net fuel cycle cost to enrichment and breeding ratio.

Figure III.3.2-4 illustrates the variation of net fuel cycle cost with effective burnup. Decreasing average effective burnup from 115.5 MWD/kg (100 MWD/kg in core) to 86.5 MWD/kg (75 MWD/kg in core), for instance, results in an increase of about 0.17 mills/kwh.

Figure III.3.2-5 illustrates the sensitivity of the net fuel cycle cost to changes in fabrication cost.

Figure III.3.2-6 illustrates the variation with plutonium value.

Figure III.3.2-1 Variation of Fuel Cycle Cost with Specific Power

Figure III.3.2-2 Variation of Fuel Cycle Cost with Fissile Fraction in Core

Figure III.3.2-3 Variation of Fuel Cycle Cost with Breeding Ratio

Figure III.3.2-4 Variation of Fuel Cycle Cost with Effective Burnup

Figure III.3.2-5 Variation of Fuel Cycle Cost with Fabrication Cost

Figure III.3.2-6 Variation of Fuel Cycle Cost with Plutonium Value

III.4 CAPITAL COSTS

In accordance with the scope of work defined by the contract, estimates of capital costs for selected reactor system components and equipment have been prepared. In general, the cost data presented was generated from cost file sources and manufacturing engineering studies. Cost quotations from outside vendors were not obtained in most cases. Since the effort described in this report did not include the detailed design and layout of heat transfer apparatus, pumps, steam generators, turbine generators and structures, no attempt has been made to evaluate the costs of these items. It is assumed that a complete capital cost evaluation of the 1000 MWe FBR will be prepared in "follow-on" studies which will include the services of an Architect-Engineering firm.

The specific items for which cost estimates were prepared are the reactor vessel, reactor vessel shield plug, reactor vessel internals, control rods, control rod drives, core instrumental devices, the primary reactor shield and the fuel handling equipment. The installation charges (equipment and labor) and the cost of concrete shields and structures normally provided by architect-engineering services are not included in the capital cost estimates presented in Table III.4-1.

TABLE III.4-1

Reactor Equipment Costs^{*}

1. Reactor Vessel		
Vessel and Internal Shields	\$1,050,000	
Seals, Flow Valves, Operators & Misc.	<u>150,000</u>	1,200,000
2. Reactor Vessel Shield Plug (Includes Top Guides and Baffles)		340,000
3. Reactor Shield and Vessel Support		
Shield Graphite	915,000	
Shield Tank and Support Cylinder	<u>215,000</u>	1,130,000
4. Core Structure and Control Assemblies		
61-Rod Absorber Assembly (28)	280,000	
Control Rod Guideway (28)	84,000	
Guide Tube (28)	56,000	
Drive Rod and Couplings (28)	112,000	
Pinion Assembly Housing, Drive Rack (28)	210,000	
Drive Mechanism & Position Indicator (CVTR-General Mills) (28)	182,000	
Amplifiers (8)	60,000	
Motor Control (8)	20,000	
Position Readout LVDT's, Multi-Turn Pot, Power Supply, Indicator (28)	56,000	
Top Locating Grid	100,000	
Core Support Grid	450,000	
Graphite Reflector Cans (174)	385,000	
Graphite Reflector, 100-lb Reactor Grade Graphite, Machined	<u>87,000</u>	\$2,082,000

*The costs tabulated are "procured costs" and do not include G&A, engineering follow, installation, and interest charges.

TABLE III.4-1 (Cont'd)

5. Core Control and Instrumental Devices

Nuclear Plant Control System

Process Instrumentation for: \$ 400,000

Control Rods
Coolant Flow Distribution
Outlet Temperature
Primary Coolant Flow Control

Safety & Interlocks (for above) 40,000

Control Board (for above) 90,000

530,000

Plant Instrumentation

Process Instrumentation for: 300,000

Secondary Loop Flow Control
Steam Generator Temperature Control
Steam Pressure Control
Reheater Temperature Control
Boiler Feedwater Control

Control Board (for above) 60,000

360,000

Operation Nuclear Control System

Fast Fission Counters (15) 22,500

Long Compensated Fast Ionization Chambers (15) 45,000

Cabinets of Transistorized Amplifiers,
Ratemeters, Bistables, etc. (7) 420,000

487,500

Operational Radiation Monitoring & Analysis Systems

Operational Radiation Monitoring 150,000

Sodium Analysis System 100,000

Cover Gas Analysis System 50,000

\$ 300,000

TABLE III.4-1 (Cont'd)

6. Fuel Handling Equipment^{*}

Refueling Manipulator and Controls	\$ 350,000	
Shield Plug Handling Crane	125,000	
Viewing Equipment	140,000	
Misc. Handling Storage & Maintenance Equip.	<u>210,000</u>	
		\$ 825,000

^{*} Does not include hot cell and shipping cask.

IV. RESEARCH & DEVELOPMENT PROGRAM

IV. RESEARCH AND DEVELOPMENT PROGRAM

IV.1 INTRODUCTION AND OVERALL SCHEDULE

There appears to be no basic technical or safety reason which would prohibit an immediate start on a nuclear power plant of the general configuration of this design. The economic potential, however, might be seriously compromised by an immediate start since provision would have to be made to accommodate the extremes of every uncertainty. The recommended course of action is therefore a program of research and development progressing through a prototype and culminating in a large plant.

Although every uncertainty will not be removed by any research and development program, the ranges of the various uncertainties will be narrowed and more closely identified. For example, on the basis of present firm evidence, it cannot be proved conclusively that a primary loop mixed mean top temperature of more than 1000°F is acceptable nor than 1250°F is unacceptable. A combination of basic mass transfer data and experimental operation of fuel and components is required to narrow this uncertainty.

The major problems can be divided into the areas of fuel development, nuclear design, transient study, primary system development, and large component development. In each of these areas four different types of work are required.

The first type is generally described as research. This covers the work of generating, gathering, and interpreting basic data and achieving a better understanding of fast breeder problems not directly associated with any specific design.

The second type of work is the development of technology and physical hardware relatively specific to a particular design concept.

The third and fourth types of work are the design and construction of a prototype, and the first large plant, respectively.

The following table, Summary of the Research and Development Program, showing the time scale involved, is divided by type of work, with the functional areas exhibited under each. In this way, the progress, and culmination in the first large plant, can be best portrayed. The detailed programs are divided into functional areas.

It will be noted that the development work generally begins concurrently with the research. This is possible because sufficient fundamental work has already been performed to permit preliminary design to proceed.

Final design and construction of the prototype plant is scheduled to start 2.5 years after the beginning of the program. This permits final fuel design to benefit from the first 4.5 years of the program, and components and primary system equipment to benefit from 3 to 4 years of development. It is the unique combination of design features of this concept which offers the potential of starting at this relatively early date without compromise on breeding.

SUMMARY OF THE RESEARCH AND DEVELOPMENT PROGRAM

		years
		. 1 . 2 . 3 . 4 . 5 . 6 . 7 . 8 . 9 . 10 . 11 . 12 . 13 . 14 . 15
Research	<u>Fuel</u>	
	<u>Nuclear</u>	
	<u>Transients</u>	
	<u>Primary Systems</u>	
	<u>Components</u>	
Development	<u>Fuel</u>	
	<u>Nuclear</u>	
	<u>Transients</u>	
	<u>Primary Systems</u>	
	<u>Components</u>	
Prototype	<u>Conceptual Design</u>	
	<u>Fuel</u>	
	<u>Nuclear</u>	
	<u>Transients & Control</u>	
	 <u>Primary Systems</u>	
	 <u>Components</u>	
	<u>Architect Engineering</u>	
	<u>Construction</u>	
	 <u>Operation & Evaluation</u> ...	
Large Plant	<u>Conceptual Design Study</u>	<u>Design & Construction</u>
	<u>Concept Development</u>	<u>Operation</u>

IV.2 PROGRAM

IV.2.1 FUEL DEVELOPMENT

The detailed behavior of the fuel (fuel material, clad, assembly) exerts a profound effect on the reactor economics. No ceramic fuel has been operated in this country in a fast reactor. Extensive work is required in both the research and development areas.

Fuel development should include the work following, on approximately the time schedule shown on the following page.

A. Research

Irradiation Studies

The uranium-plutonium carbide fuel section describes the reference fuel for the 1000 MWe Fast Breeder Reactor. Fabrication of the reference fuel is based largely on current technology; however, several areas of investigation must be pursued before a fuel can be confidently recommended for this high burnup, high temperature service. Additional studies are also warranted to attempt to improve performance, e. g., by alloying the carbide for improved high temperature strength and resistance to fission gas swelling and compatibility with clad.

Irradiation testing of the reference fuel is required to ensure fuel-cladding compatibility during in pile operation; to demonstrate the effect of a small amount of iron addition to the carbide; to demonstrate the efficiency of voids within and external to the fuel in minimizing cladding strain; and to evaluate fuel performance during thermal transients.

Introduction of a finely dispersed second phase within UC grains may provide nucleating sites for fission gas bubbles. A sufficiently high density of such sites would minimize bubble size, and the surface tension of the fuel

<u>FUEL DEVELOPMENT</u>														
	<u>Years</u>													
	.	1	.	2	.	3	.	4	.	5	.	6	.	7
<u>Research</u>														
Irradiation Studies (U,Pu)C W/Fe in Na, SS														
Environmental Studies SS in Na W/Controlled impurities														
Braze Development														
Fuel Properties Studies														
<u>Development</u>														
Mech. Design Confirmation														
Test Assembly Development														
Assembly Testing														
Sodium Fill Development														
<u>Prototype Fuel</u>														
Design														
Fabrication														
<u>Prototype Operation</u>														

*Begin

*Begin

would then prove effective in reducing fission gas-induced swelling. Alternatively, alloying additions which result in improved high temperature strength of the fuel may provide improved irradiation stability. These concepts should all be experimentally tested.

Irradiation testing of the reference stainless steel in a fast neutron spectrum is essential to the use of this material as a high endurance fuel cladding for fast reactors. Satisfactory performance will be required after exposures to integrated fast neutron dose of the order of 10^{23} fast nvt, at temperature well in excess of those experienced to date.

Resilient members are required in the reference FBR fuel assembly design. Age-hardenable alloys such as Inconel X and A286, frequently used as high temperature spring materials, apparently overage and lose their stress rupture strength during irradiation at high temperatures. Haynes 25 and other non-age-hardenable alloys may not be subject to loss of creep strength under irradiation. Tests must be performed to establish this. These basic experimental data can all be obtained in capsule irradiation tests.

Irradiation test capsules can be designed which will provide the information required. A minimum irradiation and analysis test period of three years is planned. As new information is developed, new problems may appear which would require additional testing.

In addition to developing the required information on the reference fuel system, $(U,Pu)C$ - $(U,Pu)Fe_2$ in stainless steel, data should be concurrently developed on at least one alternate fuel material $(U,Pu)O_2$ and one or more alternate clad materials. This will provide backup for the basic effort. Alternate fuel and clad may be economically less desirable, but still useful if some unanticipated major difficulty appears in the reference fuel.

Maximum information will be obtained from these irradiation studies by coordination with several development programs. In addition to the materials property data, nuclear data can be obtained by performing isotopic analyses of the irradiated fuel, and environmental data can be obtained by chemical analysis of fuel, sodium, and clad. Further, an indication of the degree of escape of fission products can be obtained by having several chambers within the capsule connected by apertures characteristic of vent design.

Environmental Studies

The effect of prolonged exposure at high temperature in a sodium environment must be evaluated. Measurements of creep rupture strength, steady state creep rates, and fatigue strength should be made after long-term exposure to flowing sodium at temperatures up to 1400°F. The formation of a ferritic surface layer, observed on austenitic stainless steels after high temperature exposure to sodium, may be expected to influence the mechanical properties of the cladding.

The resistance of the reference cladding material, type 316L stainless steel, to mass transfer in a dynamic sodium system must be evaluated at temperatures above 1200°F under conditions of sodium velocity and loop geometry simulating those anticipated in actual service.

The maximum allowable cladding temperature is predicated on mass transfer considerations. Studies of mass transfer conducted to date do not permit a reliable estimate of the extent of corrosion which will occur at the high temperatures and sodium flow velocities anticipated. Test results are also confounded by loop geometry which may allow the sodium to approach a saturated condition in the hot leg, thus resulting in lower experimental corrosion rates than would be observed in a reactor, where such an approach to saturation will not occur to the same degree. These data must be evaluated by including a geometry factor to extrapolate to reactor conditions. Proper design of test parameters would obviate this shortcoming and provide more reliable data.

Liquid sodium will remove all traces of film, grease, and oxides from metal surfaces. In this condition, mating surfaces will tend to self-weld or gall. Below 1000°F self-welding does not occur if mating surfaces consist of dissimilar metals. Comparable behavior above 1000°F must be verified by experiment. Dissimilar metal systems must be tested in liquid sodium with pressure applied to the mating surfaces to determine the amount of self-welding or galling.

Corrosion test loops must be designed, fabricated, and utilized to measure the materials properties discussed above in high-purity sodium as a function of temperature and sodium flow velocity, and to determine the changes produced by the presence of various amounts of oxygen, carbon, and nitrogen in the sodium. Tests of physical properties of corroded samples can be performed separately from the corrosion loops in sodium and at the temperatures of interest, provided the samples are protected during the transfer from the corrosion loop to the test set-up. These tests will supplement work currently in progress.

Brazing

Brazing is specified as the method of fuel rod assembly. No brazing alloy has been established as suitable for nuclear core use in liquid sodium at the temperatures of interest. A program is required to develop a suitable braze filler metal which is resistant to attack by liquid sodium. These studies should include not only corrosion resistance to liquid sodium, which can be determined in the test facilities of the environmental studies, but also elevated temperature mechanical properties, e.g., tensile strength, creep strength, etc. The development of a method of application of the braze filler metal which insures repetitive optimum gap size may require development. Preliminary screening studies should be utilized to limit the number of alternates studied in detail.

Fuel Properties Studies

Process development is recommended in the areas of fuel synthesis and fabrication. Improved control of fuel stoichiometry could eliminate the need for alloy additions to ensure compatibility. Studies are required on the reference fuel system to determine the optimum synthesis route and method of introducing the desired iron addition. Sintering procedures should be optimized to permit satisfactory control of pellet density and to ensure the elimination of the pellet grinding operation by adequate dimensional control.

Additions of iron for compatibility, or other alloying additions for strength and swelling resistance, may be expected to have significant effects on other physical and mechanical properties. For example, the presence of a second phase, or alloying additions in the carbide phase, may have a pronounced effect on the thermal conductivity of the fuel. Strength improvements may influence synthesis techniques and pellet fabrication.

Battelle has achieved marked improvement in the room temperature strength of uranium carbide by additions of refractory metals or their carbides. The influence of these additions on the uranium-plutonium carbide system, however, has not been reported. Moreover, the improvement at elevated temperatures resulting from such additions should be investigated.

A program is therefore recommended to define the influence of alloying additions on the room temperature and elevated temperature mechanical properties of uranium-plutonium carbides. A parallel investigation of the thermal conductivity of alloyed carbides is also required.

The compatibility of stainless steel and uranium-plutonium carbide, containing a small addition of iron, must be investigated. It is necessary to demonstrate that the iron addition effectively combines with the uranium-plutonium metallic phase. Harwell investigators have successfully made additions of 10 w/o UFe_2 .

B. Development

The engineering development phase of the fuel development program is devoted primarily to the design, fabrication and testing of prototypical fuel assemblies. This provides the test of all elements of the fuel simultaneously, and in an operating environment.

Mechanical Design Confirmation

The reference fuel design is more detailed than is customary for a conceptual design. It has not, however, had the full engineering analysis treatment that would be given a design to be built. A more detailed design should be carried out to ensure that the assemblies to be tested in an experimental reactor actually incorporate the essential features which will be characteristic of the full-sized assemblies. This analysis will establish the critical points to be tested, and may reveal certain features that require modification.

Test Assembly Development

Following the detail study of the large assembly (above) and the choice of a test facility, a fuel element design is required that will adequately test the essential features of the large design. It is desirable to produce two alternate test designs in order to minimize the dependency of the test results on the specific details of the test assembly.

Fabrication development is required and can proceed concurrently with the later stages of the design effort.

Several dummy assemblies should be built for mechanical testing. These can utilize depleted uranium.

Sufficient test assemblies must be manufactured to obtain meaningful data over the range of design variation.

Assembly Testing

The dummy assemblies should be tested in a sodium or NaK loop. Pressure drops should be measured, and the behavior of the assembly under conditions of flow and external vibration investigated. Water loop testing might be used for part of the tests, however, tests at temperature should be in sodium. Impulse testing, utilizing electrical heating, should be performed.

The fueled assemblies should be tested in an existing fast reactor facility such as EBR-II or Enrico Fermi to determine their physics response and endurance. After preliminary testing and kinetics studies (analytic and experimental), it would be highly desirable to culminate this test program with the operation of a full core. The test facilities mentioned could be adapted to full-core testing from which one could extrapolate to full module behavior.

A limited number of vented assemblies could be tested by inserting a contained loop within the test facility. A full core could be tested to moderate burnup without venting, or modifications could be made to the primary system to permit use of a full core of vented assemblies.

Sodium Fill Development

Sodium bonding and venting is probably essential for achieving very high burnup at high heat rates because of fission gas pressure and pellet swelling. A desirable method of achieving the sodium bond is to fill the fuel assemblies at the power plant. This can be accomplished by loading each assembly into an autoclave. After sealing the autoclave, it is evacuated, and temperature brought up to about 400°F. This will melt the low temperature solder plugs in the vent and permit evacuation of the rods by the vacuum pump. The whole autoclave is then filled with sodium and the pressure brought back to atmospheric, thus filling the rods with sodium. The assembly can then be removed to the fuel storage pool until refueling time.

This system is quite feasible in principle, but the practical reliability needs to be demonstrated. This is not required for the test assembly, but would simplify the fabrication of prototype and large reactor fuel by eliminating the handling of sodium and cleanup of the rods after filling.

By filling a significant number of individual tubes and checking them for gaps and bubbles, the feasibility could be established or modifications developed.

Pulse Testing

It would be highly desirable that mock-ups of the proposed fuel design be tested in a pulse test facility such as TREAT. This would provide experimental demonstration of the behavior of the fuel concept under extreme stress. The efficacy of the vent design to reduce clad stress during severe transients could be determined. The extent of fuel movement under these conditions could be assessed, and the response of the cermet rod to very high stress for very short periods could be measured. Preliminary studies utilizing electrical heating could improve the design and interpretation of TREAT data.

A series of integral transient experiments appear desirable in a facility which would be similar in concept to the BORAX-I or SPERT facilities. The objective of these tests would be an experimental determination of large fast reactor transient characteristics. Of particular importance would be a series of stability experiments commencing with cores having negative sodium coefficients and progressing to cores with positive sodium coefficients in small steps. Tests to destruction should then be conducted to establish the importance of the autocatalytic effect of positive sodium coefficients.

C. Prototype Fuel

When research and development work on fuel, physics, etc., has reached the point where the major areas of uncertainty can be confidently bounded, it will be appropriate to make a detailed design for a fuel assembly for a prototype or power demonstration reactor.

If the preliminary research and development proceeds as anticipated, then the design and fabrication of the prototype fuel consists of execution of the details of the original reference design fuel. In practice, the design and fabrication of prototype fuel will be a synthesis of the experimental information generated and the development and optimization of the conceptual designs.

A special effort will be required on the part of the designer to accommodate the detail design to available fabrication facilities. With only 36 to 40 assemblies required for a prototype first core, and 12 to 15 assemblies per year thereafter, the unit fabrication cost will be higher than that for the large reactors. This must be minimized in order that the potential of the large reactor not be obscured by excessively high prototype fuel fabrication costs.

IV.2.2 Nuclear Development

Because of the complexity and close coupling of a large number of physical phenomena, an extensive program to further investigate the physics of a fast reactor core is needed. Experimental verification of the basic nuclear design will be required. Uncertainties in reactivity coefficients, prompt neutron lifetime and effective delayed neutron fraction influence the stability and safety of the reactor. The final design must accommodate the extremes of these uncertainties. Similarly, uncertainties in breeding ratio, fuel inventory, permissible excess reactivity, and allowable fuel and blanket exposure directly affect the economics of the reactor. Details of mechanical design in both the core and external system can affect the coupling of the various core reactivity coefficients and hence must be investigated thoroughly as to their effect on the safety and stability of the reactor. The programming of both fuel and blanket replacement and rearrangement affects the reactivity coefficients of the core as well as power distribution and economics. The materials and methods used to control the reactor affect both neutron economy and reactivity coefficients, hence must be investigated more thoroughly.

To develop the technology to the point where a large prototype or an economic full scale plant can be built, the basic data and calculational techniques must be refined so that important reactivity coefficients and other important nuclear parameters can be determined within a workable degree of certainty. It will then be necessary to evaluate the effect of uncertainties in the various coefficients and parameters on other aspects of design. Some modifications of the basic design, methods of fuel and blanket programming and reactivity control may be necessary to minimize the influence of uncertainties in basic data.

The research and development program should include the following analytical and experimental studies on approximately the time schedule shown.

A. Fundamental Research

Basic Physics Data

To adequately fill the gaps in the available information concerning basic

REACTOR PHYSICS AND NUCLEAR DESIGN

	years	
	. 1 . 2 . 3 . 4 . 5 . 6 . 7 .	
<u>Fundamental Research</u>		
Basic Physics Data		<u>Exp. Interpretation, Theoretical Studies</u>
Methods Development		<u>Coefficients</u> - - -
Cooperation with National Lab. on Critical Experiments		_____ - - -
<u>Engineering Development</u>		
Fuel Management Studies		<u>Core, Blanket</u>
Control Methods Studies		<u>Fuel, Moderator, Poison</u>
Fuel Test		<u>Design</u>
		_____ Follow _____
<u>Prototype Fuel</u>		
Design		_____ - - -
Critical Exp.		_____
Follow		_____ - - -
<u>Prototype Operation</u>		
		*Begin

nuclear data, further study of the materials of interest for fast reactor cores in the energy range between 100 ev and 10 Mev is required. Such study should take the form of direct cross section measurements as a function of energy by means of neutron beams produced in particle accelerators, and of indirect and integral measurements carried out in reactors. The marked variation of the capture to fission ratio of Pu-239 with neutron energy results in core reactivity being very sensitive to the slowing down properties of the materials of the fuel, coolant, and structure. Detailed information concerning the spectral variation of elastic and inelastic scattering, as well as that for fission and capture, is needed in order to fulfill the required determinations of core loading, control requirements and reactivity coefficients.

To properly utilize the experimental data available from basic experiments in neutron physics, a theoretical program of analysis and interpretation is required. Such a program involves the evaluation of experimental data and the recasting of the data into the proper form for use in reactor calculations. In addition, the theoretical program would involve utilization of theoretical models of the nucleus to interpolate between the available data points. A continuous range of data required for detailed calculations would thus be provided. The development or adaptation of digital computer programs is required to implement the theoretical analysis and to carry out the necessary processing of data to be utilized in design studies.

Development of Methods and Computer Programs for Reactor Analysis

Availability of suitable nuclear data is a prerequisite to accurate reactor analysis. However, to properly utilize such data, calculational procedures must be available which permit the adequate theoretical description of the reactor and the consequent determination of its significant characteristics. Such characteristics are adequately determined if it is possible to compute, as a function of position in the reactor, all important reaction rates and the variation of these reaction rates over the range of possible reactor operating conditions.

Such calculations require the availability of multi-group transport and diffusion theory computer programs in one- and two-dimensional geometry as

well as some method for accounting for effects in the third spatial dimension. In addition to simple spatial calculation, versions of such programs must be available which permit determination of the variation of the fuel composition with time, so that meaningful fuel management studies can be conducted. To adequately evaluate the kinetic characteristics of the reactor, special calculational methods must be employed for the determination of the sodium coefficient and the Doppler coefficient, as well as the effective neutron lifetime and the value for the effective delayed neutron fraction.

Although a good start has been made in the direction of development of digital computer programs for the calculations described above, substantial further work is required to permit the realistic development of a well engineered design. Such an effort involves further studies of the multi-group procedures for the accurate representation of high energy scattering resonances, and increased emphasis on two dimensional analysis to enable calculation of the degree of refinement required for detailed design. Special attention would be directed to the problem of accurately determining the detailed spatial effects associated with partial sodium loss and the development of computer programs suitable for the calculation of such effects, and for the accurate determination of power distribution under such circumstances. Further effort should be directed to improving current methods of determining the Doppler coefficient. The most fruitful direction of such development effort is considered to lie in the development and refinement of the Monte Carlo techniques as utilized for the calculation of Doppler coefficient in the study reported here.

Cooperation with National Laboratories on Critical Experiments

In order to assess the validity and accuracy of theoretical methods and basic data to be utilized in design studies, comparisons of theoretical results with those obtained in reactor experiments are mandatory. This effort would involve, first, the review and evaluation of data already available; and, second, the planning and analysis of such additional experiments as are required to prove out the calculational methods. This experimental program will be oriented in such a way as to provide a test

for analysis over a range of parameters which would be encountered in a realistic design. If specific problems are encountered in obtaining agreement between theory and experiment, special experiments will be desired to obtain information necessary for the revision of analytical methods.

Many critical experiments have already been conducted at the various National Laboratories on fast reactor cores. Work currently in progress by ANL using the ZPR III with a central zone of simulated uranium carbide is probably most applicable to the current design. A review of available experimental data should be made to select experiments which have nuclear characteristics within the range of parameters of interest. Detailed calculations should be made of the measured experimental quantities, using the best set of methods and constants developed for design studies to check these methods and constants against experiment.

Measurement of Doppler coefficients have been made using oscillator techniques on the AETR critical assembly, on the SIR critical and at Harwell. A theoretical evaluation of the experimental accuracy of these measurements should be made. The interpretation of experimental data obtained by oscillating a small heated sample in a region of cool material may not be straightforward since the fine structure of the neutron energy spectrum incident on the sample is characteristic of the cool region. Hence an incorrect estimate of the Doppler coefficient may be obtained unless details of the experiment are carefully taken into account.

Measurements of Doppler coefficients of U-238 have been obtained by heating rods in the central region of a multiregion assembly in the ZPR III. This central region was simulated uranium carbide buffered to mock up a 4000 liter core. Similar measurements are planned at ANL in ZPR VI and IX. A theoretical evaluation of the accuracy of these measurements should be made. It may prove desirable to conduct additional experiments to investigate the effects of heterogeneity and interference between plutonium and uranium isotopes and the sodium resonance on the Doppler coefficient.

Experimental data on the power coefficients of the Dounreay, Fermi, and EBR II reactors should be analyzed when it becomes available. Oscillator measurements on these reactors may allow separation of sodium, Doppler, and mechanical reactivity coefficients. While not directly applicable to a mixed plutonium-uranium carbide system, they should provide a test of basic theoretical methods.

As an extension of the critical experiment program under way at ANL using the ZPR critical assembly machines, it would be desirable to conduct an experiment using the material volume fractions and fuel isotopic compositions of this design. The usual quantities which can be measured in this type of a critical such as critical loadings, reactivity worth of various fuel, coolant, and structural materials, void coefficients and control material worths would be determined. This critical experiment would serve primarily to test the adequacy of calculational methods.

B. Engineering Development

Control Studies

Several methods could be employed to control the reactor. The reference design uses natural boron carbide as a poison material to achieve the necessary control. Since boron obtains much of its control effectiveness by removal of low energy neutrons, and therefore may adversely affect both the Doppler and sodium coefficients, alternate control schemes may prove more suitable. Areas which should be investigated include the use of alternate poison materials which may not affect the coefficients of the reactor as much as, or may be more effective than, boron. Another possible control scheme would employ fuel movement for safety and regulation while perhaps retaining the central poison rod in each module for shim control.

Each control scheme should be evaluated as to its effect on reactivity coefficients, power distribution, breeding ratio, and overall economics, as well as its influence on the safety and operability of the overall plant.

The lifetime of control elements must be evaluated and methods for replacement of control elements must be studied as part of the fuel management studies.

Fuel Management Studies

The programming of fuel additions and the rearrangement of fuel during periodic shutdowns must be investigated in detail.

Calculations made during this study indicate that the fuel programs that lead to flattened power distributions (and are therefore desirable from a thermal design standpoint) appear to provide more positive coolant temperature and void reactivity coefficients. It will be necessary to select an optimum fuel program considering the effect on reactivity coefficients, power distribution, shim control requirements, breeding ratio, and economics of the overall plant.

Fuel depletion studies in r-z geometry* will be required to include the effects of non-uniform axial burnout in the fuel management studies since axial flattening due to fuel burnup and flux distortions due to shim rods may influence the factors mentioned above.

Since plutonium of the equilibrium isotopic concentration would not be available to start up a full scale plant, it will be necessary to study the fuel loadings and nuclear characteristics of the core during the first few cycles before equilibrium is approached. It will be necessary to calculate the reactivity coefficients and other important variables at a number of points between startup and equilibrium conditions. Startup with U-235 instead of plutonium is an alternative which should be investigated. This will entail an investigation of the nuclear characteristics of the reactor with various mixtures of U-235 and Pu in the core.

*Two dimensional studies in radial and axial planes.

Blanket Management Studies

The buildup of plutonium in the blanket tends to reduce the neutron leakage from the core, hence make the core sodium coefficient more positive. Buildup of plutonium in the blanket also affects the core power distribution, reactivity and worth of control rods. Studies to determine the optimum method of programming blanket element replacement and movement are needed to achieve the most desirable core power distribution and longest blanket life but still maintaining negative sodium coefficients.

A more detailed study of non-uniform buildup of plutonium in the axial direction and corners of the blanket is needed. A study of the effect of plutonium distribution on the coupling between core modules should be made.

Power peaking factors in the blanket region are quite high. It will be necessary to investigate the blanket power shape using a transport theory model since the multi-group diffusion model may not prove adequate to calculate the power shape near the blanket boundary.

Fuel Test Design

Detailed nuclear design of fuel assemblies for testing in a test reactor such as EBR-2 or Fermi is required. The possible compromises required to adapt large plant fuel for operation in the available test facility must be examined. This will ensure that the test assemblies yield the maximum useful data.

A fuel follow program is essential to the test program. It is here that the first verification of absorption rates and breeding will be achieved beyond that possible in capsule tests. Isotopic analyses must be performed on the spent fuel in order that the calculated values can be checked for accuracy.

C. Detailed Nuclear Design of a Prototype

When the research and development work on basic physics, stability studies, design optimization studies, etc., have reached a point where the areas of uncertainty can be bounded, it will be appropriate to make a detailed design of a prototype or power demonstration reactor.

During a detailed design study, a number of relatively minor problems come to light which must be solved before an operable plant can be constructed. Provisions for cooling and extra structural material for neutron sources, instrumentation, etc., may alter the design parameters. A detailed design is needed to ensure that all major problems have been solved.

Engineering Mock-up Critical

A full-scale critical experiment of one module of the reactor should be constructed. This experiment will serve to verify worth of the reactor control system, initial fuel loading, and power distribution in the reactor with a fresh blanket. The importance function of various materials can be measured and used to check theoretical predictions. This should prove useful in predicting the behavior of reactivity and power distribution with burnup.

Core Follow

A detailed physics core follow program should be carried out to ensure that the maximum available information is obtained from the prototype operation.

Analytical studies of anticipated changes in reactivity and reactivity coefficients should be coordinated with the changes experimentally observed.

Isotopic examination of spent fuel should be conducted to verify calculated absorption rates. This is essential to accurate prediction of long term fuel behavior and breeding gains, and supplements the follow of test reactor fuel.

IV.2.3 Transient Studies

Transient study is set out as a separate topic from nuclear design because of the importance which has been attached to the control and safety problems of fast reactors. The Research and Development Program should include the following analytical and experimental studies on approximately the time schedule shown on the following page.

A. Research

The bulk of the work described under this topic is described as research in as much as it provides a better general understanding of the behavior of large fast reactors. Such work must, however, be performed within the framework of a detailed system.

Criteria Development

A study is required to determine the range of accidents which should be considered in large fast reactors. For instance, it is important to establish the maximum rate at which reactivity can be physically inserted. It is also important to distinguish between reactivity changes which, by their physical nature, are slower than the control system and those which are faster.

Stability Studies

Even though the net sodium coefficient of the reactor is negative, a region exists at the center of each module where the coefficient is positive. It is anticipated that the local negative Doppler coefficient will be of sufficient magnitude to stabilize the reactor against any local spatial instabilities. A thorough analysis of spatially-dependent power transients and the effects of local coolant vaporization will be required. The effects of hydraulic instabilities induced by local vaporization of coolant should also be considered in these studies. The prompt negative reactivity coefficient associated with the CEX fuel element should contribute to stability against local vaporization of sodium. Since a slow positive

Transient Studies

Years

. 1 . 2 . 3 . 4 . 5 . 6 . 7 .

Research

Criteria Development

Stability and Safety Studies

Development

Test Reactor Analysis

Hazards Anal.

Licensing

Interpretation of Test Data

Prototype Fuel

Analysis

Operational

Hazards

License

Control System Development

Prototype Operation

*Begin

coefficient is also associated with the CEX element due to clad expansion of the carbide fuel rods, and a slow negative coefficient due to fuel element bowing, unstable oscillations may occur at certain frequencies. The literature on stability indicates that this combination of prompt negative and slow positive coefficients will probably be stable. A thorough analysis of the transfer function of the entire nuclear, mechanical, and hydraulic systems will be needed, however. If non-linearities exist in any of the phenomena and lead to important reactivity changes, it will be necessary to make a more sophisticated analysis than by simple examination of the transfer function. Although a number of theoretical methods for studying non-linear systems are possible, those selected will depend on the detailed character of problems encountered.

A detailed simulation of the nuclear, mechanical, and hydraulic system should be made on a large analog or digital analog computer. This simulation should include all significant time-dependent reactivity coefficients, and their spatial dependence. Variations in flow rates and pressure drop through individual core modules and regions of these modules should be cranked into the simulation where they are sensitive to the local power generation rate. Using this simulator, a study of response of the core to normal operational transients should be made, and the stability of the system to small step and sinusoidal reactivity changes determined. Typical examples of operational transients would include:

1. Loss of power to one or more of the main sodium coolant pumps.
2. Loss of power to one or more of the secondary sodium pumps.
3. Loss of load without turbine trip or scram.
4. Uncontrolled rod withdrawal at startup.
5. Uncontrolled rod withdrawal at power.
6. Accidental dropping of a control rod in one region.

Accident and Safety Studies

Using the simulation discussed in the preceding section, it will be possible to study some of the abnormal conditions which could occur. Step reactivity

additions and ramp reactivity increases with various slopes can be investigated to the limit that non-linearities or melting of parts occur to render the simulation inadequate.

Studies of the reactivity effects of major fuel displacements due to clad or fuel melting should be made. The model assumed for redistribution of fuel due to meltdown should be based on experimental studies of melting of fuel elements in a reactor such as TREAT. There is a need for a transient test study of multiple rod bundle with provision for sodium flow and transient vaporization to arrive at a realistic model for fuel displacement.

An extension of the present methods of calculating energy releases from secondary criticalities due to fuel displacement is needed. Present methods are probably overly conservative since the dispersive effects of vaporizing sodium are usually neglected. Since the nuclear coupling between core modules is fairly weak, a meltdown and secondary criticality could possibly occur in one module. The effect of a major excursion in one module on the remainder of the core will thus have to be investigated in detail.

It is anticipated that the results of these studies of accidental transients would show that the instrumentation provided for by the other R & D programs, aided by the inherent safety provided by the cermet rods and the modular design, would be capable of limiting nuclear and plant process excursions to values well within safe limits.

B. Development

Having established a test reactor in which prototype fuel assemblies may be inserted, a hazards analysis is required prior to obtaining a license for their insertion.

After insertion, interpretation of actual test data in light of calculated transient behavior should be a part of the fuel follow program.

C. Detailed Transient Analysis of a Prototype

A thorough analysis, by simulation, of the prototype plant, reactor, and control system is required. The previous work will provide the basic tools required.

A complete transient and hazards analysis is required, and a core follow program should be conducted to verify the detailed calculations.

IV.2.4 Primary System Development

Under this heading is discussed the development of all primary system equipment exclusive of fuel and major components. The research and development program should include the following analytical and experimental studies on approximately the time schedule shown on the following page.

A. Research

Liquid Metal Heat Transfer: Analytical Study

It is necessary to carry out an analysis of conduction and eddy transport of heat within a liquid metal coolant channel under axial flow conditions in a tube bundle. Heat transfer under these conditions is amenable to theoretical analysis since the transfer of heat from rods to coolant takes place primarily by the mechanism of conduction for the reactor designs presently contemplated.

The basic heat transfer study for a unit cell involves the following:

1. Coupling the heat conduction problem within the clad and fuel with the conduction transport problem within the coolant. Such coupling is necessary in order to examine the circumferential variation in heat flux and temperatures at the clad surface.
2. Coupling of adjacent channels to consider the effect of interchannel heat transport. Incorporation of empirical information on eddy mixing transport to provide a basis for comparison of eddy and conduction transport.
3. Examining the effect of rod displacements due to mechanical tolerances and rod bowing.
4. Developing a model relating eddy diffusivities of heat and momentum in order to extend the accuracy of the analytical results. Extensive digital and analogue computer code and modeling development would be required to carry out this theoretical phase.

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Development

Plutonium Decay Heat

Exp. Measurements

Large Plant Concept Dev.

Design, Analysis, Testing-Fuel Handling, Cover Gas, Flow Decay

Design, Fabrication, Testing

*
Begin

In support of the analytical program, existing experimental data on rod bundle heat transfer will be gathered and compared with theoretical results. Attempts will be made to establish if the presence of oxides, entrained gases, or mechanical displacement of rods could explain experimental discrepancies existing for Na and NaK systems. The analytical results will also serve to extrapolate thermal performance from the temperature involved in the experimental phase to those encountered in a reactor design.

Theoretical analysis will be employed to develop conduction solutions in a single flow channel or unit cell. An electric analogue plotter with provision for heat generation modeling will be used to check the results. A more extensive program involving interchannel mixing and heat conduction will be required, using a digital program. Loss-of-coolant and loss-of-flow studies must be carried out. A literature survey and gathering of experimental data will be pursued throughout the period.

Liquid Metal Heat Transfer: Experimental Study

The objective of this study is to develop a better understanding of single phase liquid metal heat transfer for application to the design of a rod type nuclear reactor. Experiments with tubes or annuli are of little value in aiding the thermal design of a rod bundle geometry. The reason for this is that, due to the dominant effect of conduction in the transport process, the equivalent diameter is not a significant factor in the correlation of liquid metal heat transfer data.

Heat transfer sensitivity to rod bowing and variations in rod pitch will be systematically examined by displacement of selected rods within the bundle. This phase of the task requires precision in the design and construction of the rod bundles since the relative rod positions must be known with accuracy throughout any given test period. Additional geometric effects, such as the presence of grids or ferrules, will form an inherent part of the experimental phase.

Effects of mixing and inter-channel heat transport will also be a part of this phase of the task and will aid in developing an engineering hot channel factor on enthalpy rise. Extensive temperature instrumentation is required to examine circumferential variations in rod surface temperature and axial temperature gradients in adjacent flow channels.

In addition to studying effects of variations in geometry, it is equally important to examine effects of oxide in the coolant or the appearance of other contaminants in the system. This requires long-term studies as well as short-duration tests wherein contaminants are injected into the system. Cold traps and hot traps will be incorporated into the system for test purposes. Accurate and continuous monitoring of the loop chemistry is required under this phase of the task.

Results of this task will be used to supplement and extend the knowledge gained from the analytical study.

Only two experimental programs are known to be reported in the literature regarding liquid metal heat transfer in rod bundles. Work at Brookhaven National Laboratories and Russian studies are outlined as follows:

1. Experiments at Brookhaven involved 13 and 19-rod bundles and mercury and sodium-potassium coolants. Rod pitch to diameter ratios of 1.38 and 1.75 were used. Reproducibility of data in the mercury tests was achieved only after redesign of the test section and use of a loop with greater capacity. Early tests occasionally resulted in Nusselt values 50% lower than anticipated after dismantling the rod bundle or after rotation of the central test rod. Lack of reproducibility in these early tests was attributed to rod bowing or rod displacement. The redesigned unit resulting in improved reproducibility of data maintained the rods in tension in order to minimize bowing.

Experiments with sodium-potassium coolant has been yielding strangely low Nusselt values. This drastic reduction in heat transfer has been noted on occasion after startup. No definitive explanation has been offered, but a sensitivity to contaminants is suspected.

2. Borishanski and Firsova briefly summarized their heat transfer results for longitudinal flow of sodium in a tube bundle with a rod pitch-to-diameter ratio 1.2. Some of their data fell as low as 1/3 of the analytical predictions. Of course, the analytical predictions were based upon a clean system with evenly spaced rods. It is not known what might have caused the discrepancy. The Soviet authors present a correlation of their data and recommend it as a working equation valid for pitch-to-diameter ratios ranging from 1.2 to 1.75.

This program would involve design and construction of a loop and various rod bundle test sections as well as operation of the system for purposes of gathering heat transfer data. Tests of up to 400 hours duration will be performed to assess effects of contaminants. Pressure drop measurements will be made in conjunction with the heat transfer tests. A digital computer code will aid in the data reduction for development of a heat transfer correlation. Results of the analytical phase of the program will be used to guide the planning of tests and will be employed in the final comparison and assessment of experimental data.

Fission Product Release

It is essential to determine the degree of release of the various fission products formed within the fuel. This information is required in order to design the clean-up system. Even though the anticipated burnup is high, the in-core time is only three years, and the maximum fuel temperature is low. The fraction of fission products which is released should therefore be low. This must be determined.

Experimental determination of fission product solubilities in liquid sodium must be performed. Out-of-pile leaching experiments can be correlated with analysis of sodium from fuel irradiation capsules.

In-pile loop experiments should be performed to establish the degree of deposition of fission products to be expected on surfaces exposed to the contaminated sodium. These dynamic tests will supplement the data obtained under static conditions in the fuel irradiation capsules. Corrosion test specimens should be included to determine the effect, if any, of fission products on the corrosion behavior of sodium.

Analytic calculations of diffusion of fission products through the fuel should be performed and checked against experiment. Additionally, the rate of release from the static bond sodium to the flowing coolant must be established. These calculations can be corroborated by the capsule irradiation experiments of Section IV.2.1.

Fission Product Removal

Pure liquid sodium is known to be compatible with a wide variety of metals over a wide temperature range. However, impurities such as oxygen, hydrogen, carbon, and calcium lead to increased corrosion, mass transport, plugging of lines and fouling of surfaces because of precipitation of insolubles, and modification of steels by carburization or nitridation depending on their concentration levels. Therefore, it is necessary during reactor operations to maintain the sodium at a high purity level by continual purification since the introduction of the above-mentioned impurities into the reactor is inevitable.

With the use of the vented fuel rod, control of the level of fission products in the primary liquid coolant becomes extremely important. Consequently, purification and decontamination requirements would increase. Although traps, cold or hot, have not been specifically designed for fission product

removal, it has been reported that fission products from a damaged fuel element have been collected in a cold trap designed to remove sodium oxide and similar impurities. The chemical form of the fission products collected in the cold trap is not known.

The fission product - liquid sodium complex may increase the corrosivity of the liquid sodium at elevated temperatures by reacting with the sodium to give corrosive complexes or insoluble erosive particulates. It has been postulated that the fission products react with the sodium oxide to give fission-product oxides. This is the reason reported in the literature for their removal by cold trapping. This scavenging effect may result in lower corrosion. Physico-chemical data are needed to determine the behavior of the fission products in liquid sodium.

The functioning of hot and cold traps is not well understood, even for the removal of the basic impurities, oxygen and carbon. Trap behavior with respect to fission products especially needs to be established. Basic experiments need to be performed to evaluate the effects of geometry, temperature gradients, surface condition, turbulence and velocity. Additional data are needed on the crystallization characteristics of fission products, sodium oxide, and carbon from sodium solutions as a function of degree of supersaturation, etc.

Finally, this program should provide for the operation of an in-pile loop in which the combined effects of fission product evolution and removal can be measured at different impurity levels.

Decay Heat From Plutonium Fuel

The refueling system design for this plant is influenced by the cooling required by the spent fuel assembly as it is lifted out of the reactor pool. Experimental verification of the amount of heat released by the fission products of plutonium as a function of decay time is needed. Such information could be obtained from a heat balance experiment at PRTR on an irradiated plutonium bearing fuel assembly.

Somewhat more accurate information could be developed by irradiating several pins, representing several degrees of plutonium recycle, in a fast reactor, and measuring the heat output of these pins by calorimetry.

B. Development

Large Plant Concept Development

The engineering development is primarily concerned with the analysis, design, and testing, where feasible, of the systems components required for the large plant. In general, the prototype will incorporate the concepts developed for the large plant, so that actual environmental testing will be accomplished in the prototype.

Study, analysis, and detailed design are required to fully establish the fuel handling system and associated components.

An extensive study is required on the cover gas system. Points to be established are the degree of leakage which is acceptable and the degree which is probable; the effect of sodium vapor on the cover gas system components and other components exposed to the gas; and the amount of cooling required.

Studies are required of flow decay after loss of pump power to establish the requirements for pump brakes and emergency power supply. Studies of circulation are needed to check the ability of the system to remove shutdown heat effectively.

An analysis is required of the piping system to establish the validity of eliminating primary loop valves.

High Temperature Instrumentation

The reference design of the "Operational" Nuclear Instrumentation system as given in Section II.2.4.6 presupposes the use of fast neutron detectors located in thimbles in the graphite areas of the core. It is anticipated that the temperatures at the tops of these thimbles will be approximately the same as the mixed mean outlet temperature of the core sodium coolant. This high temperature, along with the high gamma flux expected, would subject the fast neutron detectors to very severe operating conditions. It is necessary to develop neutron detectors which could cover the full range of nuclear instrumentation required under the severe environmental conditions anticipated.

A detailed investigation is required of the types of detectors, and the materials available for construction to fit the needs of the instrumentation system. This would be followed by construction of prototypes, and their testing in facilities that would simulate the operating conditions within the FBR as nearly as possible.

Preliminary discussions with suppliers have indicated that fast neutron proportional counters containing helium-3 as the activation gas would be limited in operation to about 200°C due to the changes in gas multiplication over a larger temperature range. However, it may prove feasible to construct fission counters and fission chambers using uranium or plutonium for operation up to a maximum of 700°C (about 1300°F). The addition of the gamma heating to the environmental temperatures, however, makes the successful development of detectors for direct insertion in thimbles in the core extremely difficult.

If the successful development of detectors for direct insertion in thimbles in the graphite areas of the core cannot be accomplished, one alternate is a cooling system for the detectors. For example, liquid sodium at about 350° to 400°F might be circulated from outside the vessel around the instrument thimbles. Another alternate for power measurements would be activation measurements on a rapidly circulating fluid.

Recycle Cold Trap

The cold trap in a contaminated system will be extremely radioactive and it will not be possible for personnel to approach it for normal removal and replacement. Therefore, the cold traps (and the hot traps) must have internals which can be removed by remotely operated cranes. Alternatively, the traps could be designed to recycle themselves automatically, depositing their accumulation of trapped material in a separate container for disposal.

Several possibilities exist for each of these approaches. These must be investigated, and the most promising reduced to hardware and tested to demonstrate its practicality.

C. Prototype Primary Systems

The primary systems aspect of the prototype design is that of detailed reactor design. It is not within the scope of this document to detail the design program for the prototype reactor. As an example of the scope of work required, however, the following discussion is a resume of the reactor vessel design effort.

Reactor Vessel Development

The design of the reactor vessel for the 1000 MWe FBR has been influenced by the desirability of providing an inlet coolant plenum where flow may be properly distributed to each of the seven cores and to the blanket region of the reactor. This design does not appear unduly complex or costly, and flow distribution requirements may not be as severe as anticipated. In that case, a simple single-wall vessel arrangement with multiple inlet nozzles could prove less costly.

The development of thermal shields which will provide satisfactory protection to the reactor vessel wall during both steady state and transient operation is essential to construction of a successful liquid-metal-containing reactor vessel. The development and selection of reactor vessel head seals which are

both leaktight and easy to maintain certainly requires investigation. Vessel closures of the freeze seal, liquid seal, and gasket seal type require study to determine the preferred system. Since the reactor vessel shield plug in this concept must be removed at yearly intervals for refueling, it is important that its dimensional stability be preserved under all operating conditions. Careful design and analysis will be required to achieve this. In addition, the weight of the shield plug must be minimized to ease its physical handling during refueling. Flow distribution and flow distribution devices, such as those proposed in the design, must be carefully designed, tested and evaluated to establish their ultimate reliability and feasibility. Type 304 stainless steel has been tentatively selected as the construction material for the reactor vessel. This choice must be carefully reviewed from both the materials (strength and corrosion) and economic viewpoints.

The development program necessary to determine the practicability of the proposed reactor vessel requires experimental investigations in addition to design and analysis efforts. Design and analysis studies will be performed to determine what transients (temperature and pressure) the reactor vessel must withstand. The suitable thermal shields will then be developed, designed and analyzed for effectiveness. Thermal expansion behavior of all parts of the vessel will be analyzed under all operating conditions to determine that no intolerable distortions or stresses occur. Flow distribution devices within the reactor vessel inlet plenum will be designed and analyzed for hydraulic and mechanical suitability.

Experimental investigations will be conducted to verify the essential portions of the design and analysis work. The specific thermal shield design will be mocked-up and tested in sodium to determine its effectiveness and reliability. Material selected for reactor vessel construction will be subjected to corrosion and properties tests in sodium at the operating conditions. The flow distribution devices will be constructed and subjected

to simulated sodium flow and temperature conditions to determine their suitability. Heat losses and temperature distribution through the top shield plug of the reactor vessel will be evaluated experimentally to provide necessary design and cooling requirement data. A full-scale reactor vessel closure will be mocked-up in a suitable cover gas and liquid metal environment and subjected to repeated sealing and removal operations to evaluate any problems in closure design.

IV.2.5 Components

The design of large system components is outside the scope of this study. It is important to recognize, however, that extensive development work is required on large sodium pumps, intermediate heat exchangers and steam generators.

Sodium pumps are preferred in the primary system hot leg in order to minimize the reactor vessel pressure, and the plug seal requirements. This places stringent demands on the pump bearings and impeller. Experimental development must be carried out to produce designs adequate for the required service.

Large sodium steam generators have not been developed. These are a prerequisite to the ability to construct a 1000 MWe plant. Work is currently underway under an AEC sponsored program for one such design.

IV.2.6 Conceptual Design Studies

The effort reported in this document was restricted to essentially the nuclear portion of the plant. It is essential to develop the remainder of the plant to the same stage, and to estimate the capital cost requirements for construction. Such a study will require about one year to perform, and is a necessary complement to the present study. With all of these results available, a definitive assessment of the overall competitive position of the larger fast breeder can be made. This study is needed immediately in order that conceptual design studies can be initiated for a prototype.

As the whole plant conceptual design develops, certain problem areas will be recognized. These problem areas should be carried beyond the concept stage sufficiently far to establish firm solutions. This large plant concept development work is essential to proper direction of the prototype plant.

Finally, a conceptual design study for a prototype is required. This should be done concurrently with the second half of the large plant study above. The conceptual design study for the prototype should be carried out in sufficient detail to provide a good estimate of the costs to be incurred, including those of operation and maintenance.

IV.2.7 Prototype

The final phase of the development program is the design, construction, and operation of a prototype or a power demonstration plant. A single module prototype would permit verification of essentially all phases of the design except module coupling and costs. With each module control system operating like a separate reactor, the essential contribution of the coupling is to improve the controllability of the individual modules, and modeling this effect is not essential. The capital cost per kilowatt of capacity, and the fabrication and processing cost per kilogram of fuel will all be several times as high for a prototype as for a large plant. A power demonstration reactor consisting of perhaps three modules would be a better demonstration of large plant economics. If the engineering development program carries fuel testing through to the stage of operating a full core in a modified test reactor, then the power demonstration step should be a feasible alternate to the prototype step.

On the basis of present knowledge, the reference design conditions appear to be achievable.

The fuel cycle economics at the reference conditions are so favorable, however, that should the development effort not fully achieve the target reference conditions the concept would still be an economically competitive power plant. The existence of this large economic margin is very important in establishing the justification for this development effort.

REPORTS PUBLISHED UNDER CONTRACT AT(30-1)-3251

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WCAP-4400	Large Fast Breeder Reactor Design Study Monthly Progress Report No. 1 (June-July, 1963)
WCAP-4401	Large Fast Breeder Reactor Design Study Monthly Progress Report No. 2 (August, 1963)
WCAP-4402	Liquid Metal Heat Transfer and Pressure Drop (in preparation)
WCAP-4403	Large Fast Breeder Reactor Design Study Monthly Progress Report No. 3 (September, 1963)
WCAP-4404	Large Fast Breeder Reactor Design Study Monthly Progress Report No. 4 (October, 1963)
WCAP-4405	Large Fast Breeder Reactor Design Study Monthly Progress Report No. 5 (November, 1963)
WCAP-4406	Simulation of the Temperature Distribution in a Clad (in preparation)
WCAP-4407	Analog Simulation of the Stress Load in a Fast Reactor Cermet (in preparation)

ACKNOWLEDGMENTS

ACKNOWLEDGMENTS

The writers wish to acknowledge the Advisory Board of the "Liquid Metal Fast Breeder Reactor Design Study" for their generous aid and assistance. The Advisory Board convened on a monthly basis. Progress of the functional working groups was reviewed; many constructive suggestions and comments were received. The Advisory Board was composed of:

R. J. Creagan, Manager, Advanced Development and Planning
P. G. DeHuff, Manager, Reactor Engineering and Materials
T. Stern, Manager, Plant Projects
J. H. Wright, Manager, Advanced Systems

The writers are indebted to the following individuals for aid in editing and compiling this report: J. Kilpatrick, M. R. Sealy, W. H. Comtois, A. D. Bush, and L. R. Rees.

The artist's sketches of the "Modular Core Array" and "CEX Fuel Assembly" were prepared by J. H. Sanders of the Illustrations Group.