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THE FAST-MIXED SPECTRUM REACTOR
INTERIM REPORT
INITIAL FEASIBILITY STUDY

Prepared by
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January 1979

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DIVISION OF REACTOR RESEARCH AND TECHNOLOGY
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THE FAST-MIXED SPECTRUM REACTOR INTERIM REPORT INITIAL FEASIBILITY STUDY

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January 1979

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EXECUTIVE SUMMARY

The Fast-Mixed Spectrum Reactor (FMSR) is a new concept in fast reactors for the production of electric power. The concept, its limitations, possibilities, and effects of uncertainties is presently being explored in a feasibility study. The analysis so far bears out the validity and feasibility of the concept, although there are still significant design and operating problems requiring solution.

The concept has been tailored to offer excellent non-proliferation characteristics, and at the same time to achieve good utilization of uranium resources. Although the FMSR in its operating characteristics would be a substantial departure from conventional fast breeder designs, it is closely related to breeders in fundamental technology. As a result, most of the current fast breeder R&D programs can be directly utilized in the development of the FMSR. Some extensions and redirection, particularly in the metal fuels area, will be needed to establish feasibility and prepare for demonstration of the reactor. The cost of generating electric power from a FMSR should be approximately equivalent to that from comparable fast breeder reactors. However, fuel pin fabrication costs for an FMSR should be less than those of a conventional fast breeder.

The FMSR has several features which differentiate it from fast breeders. It would operate on a once-through-and-store fuel cycle, with no fuel reprocessing being required. After the first core, and in equilibrium fuel cycle operation, the new fuel added at reload time would consist only of natural or depleted uranium, with no additional fissile material. The plutonium burned in the reactor would be produced in situ by neutron capture, as the result of a high breeding ratio or conversion ratio; significant U-238 fast fission in the hard FMSR spectrum contributes directly to power generation. Fuel would remain

in the reactor a very long time (about 17 years according to current design studies), and the burnup of heavy metal that would be achieved in this period is substantial (about 13-15%). The combination of refueling with natural or depleted uranium and the high burnup would make the FMSR more than 15 times as efficient in uranium resource utilization as a once-through light water reactor (measured in terms of energy per unit mass of natural uranium) on the equilibrium fuel cycle.

The reactor could be started up on medium enriched uranium, at an average enrichment of about 7% and a maximum enrichment of about 11%. These enrichments are well below guideline values commonly used to measure concern over proliferation. The combination of the low initial enrichment of fuel and the deferral of chemical reprocessing, at least for many decades, make FMSR a very attractive concept for longer term reduction of the possibility that the commercial fuel cycle might be used as a springboard for proliferation of nuclear weapons. It would also reduce concern over possible subnational threats, because only the spent fuel would contain weapons-usable material, and the high fission product inventory would render this spent fuel unattractive for long times.

FMSR would also depend on technology developed for the fast breeder program. In most respects this is main line LMFBR or GCFR technology, though in some important respects the technology required is the more advanced FBR technology. The nonnuclear parts of the plant could be identical to those of the corresponding fast breeder, though the gas-cooled FMSR would operate at a higher pressure.

As stated previously, the FMSR has been tailored to operation on the once-through fuel cycle. The discharged fuel would, nevertheless, contain substantial amounts of plutonium - about 600 kg would be discharged each year

for a 1000 MW(e) plant. This plutonium would be produced over and above any needed for continuation of the FMSR fuel cycle. It would therefore be available for use in burner reactors in a symbiotic fuel cycle or other breeders if this were desired.

This report summarizes the results of an initial four-month feasibility study. Reactor physics, fuel cycle, and thermal-hydraulic analyses were performed on a reference design. These results when coupled to a fuel and materials evaluation performed in cooperation with the Argonne National Laboratory indicate that the FMSR is feasible provided the fuels, cladding, and sub-assembly ducts can survive a peak fuel burnup of 15 to 20 atom percent heavy metal and peak fluences of 8×10^{23} (nvt > 0.1 MeV). The results of this short study have also provided a basis for exploring alternative designs requiring significantly lower peak burnup and fluences for their operation.

1. INTRODUCTION

The Fast-Mixed Spectrum Reactor (FMSR) is a new concept in fast reactor design for the production of electric power. The design and its limitations, possibilities, and effects of uncertainties are presently being explored in an initial feasibility study. Analysis so far bears out the validity and feasibility of the concept, though there are still significant design and operating concerns requiring resolution. Both gas-cooled and sodium-cooled versions of the reactor are being studied.

The concept has been tailored to offer excellent nonproliferation characteristics and, at the same time, achieve good utilization of uranium resources. Although the FMSR is a substantial departure from conventional fast breeder designs with regard to important operating characteristics, it is closely related to breeders in fundamental technology. As a result, only extensions of current fast breeder Research and Development programs, rather than new R & D programs, would be needed to establish feasibility and prepare for demonstration of the reactor. The cost of generating electric power produced using a reactor of FMSR design should be reasonably close to that of conventional fast breeder reactors; fuel fabrications costs for a FMSR, however, should be less than those of a more conventional fast breeder.

The FMSR has several features which differentiate it significantly from conventional fast breeders. It would operate on a once-through-and-store fuel cycle, with no fuel reprocessing being required. After the first core, and in equilibrium operation, the new fuel charged at reload time would consist only of natural or depleted uranium, with no fissile makeup required. The plutonium burned in the reactor would be produced in situ by neutron capture, as a result of the high breeding ratio or conversion ratio of the FMSR. Fuel would remain in the reactor a very long time (about 17 years according to current

design studies), and the burnup of heavy metal that would be achieved in this period is substantial (about 13-15 atom percent). The combination of refueling with natural or depleted uranium and the high burnup would make the FMSR more than 15 times as efficient in uranium utilization as a once-through light water reactor (measured in terms of energy per unit mass of natural uranium), for both the gas-cooled and liquid-metal cooled FMSR reactors operating on their respective equilibrium cycles.

The reactor could be started up on medium enriched uranium, at an average enrichment of about 7% and a maximum enrichment of about 11%. These are well below guideline values commonly used to measure concern over proliferation. The combination of low initial enrichment of fuel and the deferral of chemical reprocessing, at least for many decades, make the FMSR a very attractive concept for longer term reduction of the possibility that the commercial fuel cycle might be used as a springboard for proliferation of nuclear weapons. It would also reduce concern over possible subnational threats, because only the spent fuel would contain weapons-usable material, and the high fission product inventory would render this spent fuel to be unattractive for long times.

The FMSR would depend only on technology developed for the fast breeder program. In most respects, this is main line LMFBR or GCFR technology, though in some important respects the technology chosen is the more advanced FBR technology. The nonnuclear parts of the plant could be identical to those of the corresponding fast breeder, though the gas-cooled FMSR would operate at a higher pressure.

As stated previously, the FMSR has been tailored for operation on the once-through fuel cycle. Nevertheless, the discharged fuel would contain

substantial amounts of plutonium -- about 600 kg each year for a 1000 MW(e) plant. This plutonium would be produced over and above any needed for continuation of the FMSR fuel cycle. It would therefore be available for use in burner reactors in a symbiotic fuel cycle if this were desired.

This report is a summary of progress which has been made over an initial period of about four months, during which the primary goal has been to explore the feasibility of the FMSR concept. Major attention has focused on reactor physics and fuel cycle studies, to define the base design. The parameters used in the reactor physics calculations have been in part checked by parallel thermohydraulics calculations. A central aspect of the feasibility of this design is the requirement for the proposed nuclear fuel and the steels used for clad and duct walls to survive the irradiation levels which would be experienced. Argonne National Laboratory has been providing valuable assistance in exploring this area. Results of the preliminary evaluations performed by ANL are summarized in Appendix B of this report.

2. THE CONCEPT

2.1 PRINCIPALS OF OPERATION

The Fast-Mixed Spectrum Reactor concept is an extension of the heterogeneous fast breeder reactor and is also a variation of the coupled fast-thermal reactor studied by R. Avery⁽¹⁻³⁾. The objective of Avery's study was to increase the prompt neutron lifetime and thereby greatly reduce the energy yield of hypothetical major accidents.

Unlike the heterogeneous fast breeder reactor whose core makeup feed consists of enriched plutonium as well as natural uranium, the FMSR is designed to be self-sustaining on an equilibrium feed of natural or depleted uranium alone. No enrichment or reprocessing plants are needed once the reactor has achieved an equilibrium fuel cycle. As in the coupled fast-thermal concept, the FMSR uses a moderator bearing region. The objective now, however, is to provide additional reactivity at low enrichment, and to improve power flattening and design flexibility.

The objectives for the FMSR reactor design study so far are to establish a conceptual basis for a reactor which:

(1) is self-sustaining on natural or depleted uranium feed only once it has been charged with an initial feed of enriched fuel. This concept would also employ a once-through fuel cycle.

(2) would utilize for the most part existing fast breeder technology.

(3) has improved characteristics vis-a-vis proliferation.

(4) offers good economics.

To achieve the above objectives, the concept requires a high breeding gain and good neutron economy. These requirements have led to a design based on the use of metal fuel. This one feature almost doubles the number of fertile atoms per unit volume of the fuel, significantly increases the fast

fission contribution of ^{238}U and ^{240}Pu , reduces the capture-to-fission ratio of fissile isotopes, and because of the relatively harder neutron spectrum, leads to a smaller reactivity loss due to fission product absorption.

The fuel, clad, and duct designs are being developed in cooperation with Argonne National Laboratory. The fuel design follows as closely as possible the design features of the successful Mark II⁽⁴⁾ metal driver fuel, which has been operated in EBR-II to burnups in excess of 13 atom percent.

The physics analysis to date indicates that designs can utilize either sodium or helium as the coolant. For engineering reasons, we have somewhat higher confidence at present in the design based on helium cooling.

The reactor can be made to operate with any of several fuel shuffling strategies. In each case, however, it appears highly desirable to surround the central hard spectrum region of the core by a moderator zone consisting of graphite or beryllium. The role of the moderator differs somewhat depending on fuel shuffling strategies; its principal purpose is for power flattening and reactivity management. Its presence outside the active core region provides some boost in reactivity, thereby reducing the reactivity swing during the intervals between fuel shuffling. It should be pointed out that the reactivity swing is a net gain in reactivity because of the large internal conversion ratio.

The FMSR is compatible in most respects with balance of plant designs corresponding to conventional fast breeders. The one significant departure affecting the nonnuclear part of the helium-cooled version is the current choice of high pressure (about 140 bars) for the helium-cooled version. The reason for this tentative choice is the desire to increase the effective heat capacity of the gas flowing through the core, to take maximum advantage of the better heat transfer characteristics of metal fuel and to reduce total pumping power requirements.

An example of the design layout of such a reactor concept will be discussed for both the helium-cooled and sodium-cooled versions in the next section. This will be followed by analyses of one of the potential fuel management strategies.

2.2 CORE LAYOUT

Figure 2.1 is a cross-sectional diagram of the FMSR core. The schematic core layout should be considered as generic rather than a representation of a specific design layout. In the several loading and shuffling strategies that have been explored the same general features have been used. The hexagonal subassemblies contain fuel or moderator and steel. The nonshaded hexagons could contain steel which could be used as a thermal shield. The hexagons with dots in the center contain moderator. Hexagons marked "F" are locations of fuel-bearing control rods, while those marked "S" are representative locations of shutdown rods. All other hexagons represent fuel-bearing subassemblies. Those marked with a "1" are in the moderated zone; those marked with a "2" are in the hard spectrum region, and those marked "3" are in a transition region where spectrum softening occurs because of the presence of the surrounding moderator.

Figure 2.2 shows a cross-sectional view of the proposed subassemblies. Fueled subassemblies would contain nominally 271 fuel pin locations in a hexagonal array within the hexagonal duct walls that confine the coolant flow. The distance between duct wall flats is approximately 20 cm. The external dimension of moderator-filled subassemblies would be essentially identical to those of the fueled subassemblies. The dashed circle in the center designates space to be allocated for cooling.

FAST-MIXED SPECTRUM REACTOR CONCEPT

FIGURE 2.1: Cross-Sectional View of the FMSR.

FUELED
SUBASSEMBLY

MODERATOR-FILLED
SUBASSEMBLY

FIGURE 2.2: Cross-Sectional View of the FMSR Subassembly.

2.3 FUEL SHUFFLING STRATEGIES

Several fuel shuffling strategies have been investigated. In one of these, fresh fuel as natural or depleted uranium is inserted first into locations near the center of the fast spectrum zone. Here it displaces fuel which in turn is moved to a new position farther out radially. In turn, fuel from that position is moved to a next position farther out, etc. Thus, fuel moves in successive shuffling steps from the center of the fast spectrum region to the transition zone, and then into the moderated region, from which it is discharged when its irradiation limit has been reached. Calculations indicate that this strategy would satisfy neutronic requirements of breeding and criticality in the steady-state mode of operation, but it would have the disadvantage that the variation in plutonium content and therefore power generation would be high for fuel in its initial location at the center of the fast spectrum zone.

In a second strategy the fresh fuel would be placed first in the transition zone at the outside of the fast spectrum region. Its low initial plutonium content would offset the tendency toward power peaking in this location. After some period of residence, the fuel would then be moved to the central zone of the fast region. From this point on, it would be moved in succession through several locations in the fast spectrum region in a sequence designed to flatten the power generation rate. It would then skip over the transition zone, and would be shuffled outward through the two radial zones of the moderated region before discharge. This version of the reactor would also satisfy the neutron physics requirements.

A third strategy is based on loading fresh fuel first into the outermost ring of the moderated region. The fresh fuel (depleted or natural uranium)

acts as a strong absorber of radially leaking neutrons. After a period of residence, it is moved to the inner ring of the moderator region. The fuel remains in this position until the bred plutonium enrichment reaches approximately 3%, at which time the fuel is shuffled to the fast region. This strategy results in a lower power swing for the fuel during its first cycle in the fast zone. In addition, this strategy also yields an acceptably flat radial power distribution.

Studies to date have focused on the third strategy. The primary basis for emphasizing this strategy was to reduce potential local hot spot effects in the moderated region, which would result if more enriched fuel were placed there. The strategy has the disadvantage that the power density in the outer moderated region is low, requiring the fast core to carry a higher power load, and leading to a higher net fluence damage to clad. Other shuffling strategies may very well turn out to be more advantageous, and these will be studied later.

In order to permit as careful a reactor physics evaluation as possible, the simple diagram of Figure 2.1 has been expanded in such a way as to treat the details more adequately. This will be described in the next section on reactor physics.

2.4 GAS-COOLED FMSR DESIGN PARAMETERS

In order to take maximum advantage of current fast reactor development, the helium-cooled FMSR parameters were selected to be reasonably close to the design parameters used in the GA 300 MW(e) GCFR Demonstration Plant, as described in Reference 5.

Table 2.1 lists a number of basic parameters of the FMSR design, relative to those found in Reference 5. The rationale behind the preference of metal fuel over oxide fuel for the FMSR has already been mentioned. Venting of

TABLE 2.1
General Design Parameters of FMSR
and Comparison to GCFR Demo Design

Parameter	FMSR	GCFR DEMO
Reactor Power, MWe	1000	300
Active Core Height, cm	160	100
Cladding OD, cm	0.8804	0.7400
Cladding Thickness, cm	0.0432	0.0483
Fuel Pellet OD, cm	0.7940	0.6333
Fuel-Cladding Gap, cm	0.0	0.0051
Duct Wall Thickness, cm	0.254	0.254
Hexcan Size (Inner), cm	18.69	16.55
Fuel Volume Fraction	0.39	0.277
Flowing Coolant Volume Fraction	0.40	0.40
Pitch, cm	1.1354	1.0000
P/D	1.29	1.35
No. of Pins/Subassembly	271	271
No. of Fuel Subassemblies	408	118
Number of Spacer Grids	15	10

the metal fuel has the same two major advantages for the FMSR as for the oxide-fueled GCFR: collapse of the fuel pins is prevented early in fuel lifetime, and pressure-driven creep of the clad toward the end of life caused by fission gas pressure is also avoided. The ability of metal fuels to release fission gas over the entire fuel pin in a process which is essentially independent of local fuel temperature tends to significantly reduce the expected axial expansion of the metal fuel with burnup. This has been observed in operation of the ANL Mark-II driver fuel.

The fuel preferred for the gas-cooled FMSR is vibratory-compacted metal powder, packed to a density of 0.75 times the theoretical value. This leaves a gas space into which fuel can grow as fission products accumulate. The Mark II driver fuel developed by Argonne, a sodium-bonded fuel, is undesirable for the gas-cooled reactor, because of possible effects of sodium that might be released as a result of cladding failure.

The larger pin diameter shown for the metal fuel reflects the advantages to be gained from the significantly higher thermal conductivity of metal fuel, even after accounting for the porosity of the metal fuel and a reduced maximum centerline temperature of 850°C.

Core volume fractions selected for the FMSR were a result of both heat transfer and metallurgical studies. The principal changes represent a recognition that the 1974 GA design study allowed considerable space for dilation and bowing of the then current best steel, 20% cold-worked stainless type 316 steel.

If newer advanced alloys, which have shown excellent irradiation resistance to fluences of about 2×10^{23} (nvt > 0.1 MeV) are used, the replacement of some heretofore stagnant helium between the ducts with fuel alloy was assumed to be possible with only a slight reduction in the flowing helium

volume fraction. The validity of these very important initial assumptions could only be partially examined in the time scale leading to this report; More intensive studies will be made in FY 1979.

The FMSR core height of 1.6 meters was selected as a means to reduce axial leakage of neutrons. While longer than the core height of the General Atomic's 300 MW(e) Demo, core heights of this magnitude range are not unusual for breeder reactors in the 1000 MW(e) size range.

The balance of the FMSR reactor plant was intended to be as close as possible to that of a conventional GCFR. This would greatly reduce R & D requirements. Reduction of time and costs of development in a FMSR concept could lead to an earlier possible construction date. One significant change from current US design of the GCFR has been adopted at the suggestion of General Atomic. The base gas pressure of the FMSR has been taken as 2000 psi rather than the nominal 1230 psi preferred by GA. This change is attractive because of the high thermal conductivity of the metal fuel, and it tends to reduce required pumping power. Future GCFR designs which seek to employ carbide fuels might also find the higher pressures advantageous.

2.5 SODIUM-COOLED FMSR DESIGN PARAMETERS

The sodium-cooled FMSR has been studied as a limited parallel effort to the gas-cooled FMSR. Essentially the same fuel shuffling strategy has been used for the sake of expediency. A metal fuel was again selected, but in this case the fuel pin design could be very similar to that of the sodium-bonded Mark-II driver fuel of EBR-II. Experience from EBR-II irradiations would be directly relevant for the irradiation levels desired, thereby reducing the amount of additional irradiation testing. Preliminary heat transfer calculations indicate that the reactor could be sufficiently cooled with a 0.50

volume fraction of fuel, a 0.30 volume fraction of coolant and a 0.20 volume fraction of steel.

The core height is 1.6 m, identical to that of the helium-cooled FMSR. Since the metal fuel is compatible with sodium, in contrast to oxide fuel at high burnups, it may be possible to use a vented fuel for the sodium-cooled FMSR, with venting to the sodium.

It is intended to utilize as far as possible conventional balance of plant technology for the LMFBR. One difference is the requirement of higher coolant pressure, which follows from the higher fuel volume fraction, the lower coolant volume fraction and, to the 1.6 m height of the FMSR core. This pumping requirement may more readily lend itself to a pot-type LMFBR so that the design constraints of higher pressures would only apply over short pipe lengths and at the inlet plenum. The higher pressures would also enhance the tendency for hexagonally-shaped subassembly walls to bulge under the combined influence of irradiation and temperature.

3. REACTOR PHYSICS

In this section, the results of the reactor physics analyses are presented for the helium-cooled and sodium-cooled systems. Primary attention has been directed toward the helium-cooled version of the FMSR. Detailed results are therefore given only for the helium system, and preliminary, less detailed information is given for the sodium-cooled system. In all cases, the reactor physics analyses were closely interfaced with thermal-hydraulic and material requirements discussed in Sections 4 and 5 respectively.

3.1 METHODS AND THEIR VERIFICATION

The reactor physics calculations for this study are based on ENDF/B-IV nuclear data and one- and two-dimensional diffusion theory codes. Figure 3.1 shows the calculational sequence used in the procedure.

The 50-group LIB-IV⁽⁶⁾ microscopic cross-section library, processed by the MINX⁽⁷⁾ code, is used as the starting point for the nuclear data input. The library contains 101 nuclides with cross-section values generated at zero degrees Kelvin and infinite dilution and at three different additional temperatures (300°K, 900°K and 2100°K). Fission product files for ²³³U, ²³⁵U and ²³⁹Pu are given in three groups. These groups correspond to the non-saturating, slowly saturating, and rapidly saturating fission products. Delayed neutron yields and spectra are provided for seven isotopes (²³²Th, ²³³U, ²³⁵U, ²³⁹Pu, ²⁴⁰Pu and ²⁴¹Pu). These data were further processed by the CINX⁽⁸⁾ code to make the format compatible with the one-dimensional diffusion theory code, 1-DX⁽⁹⁾. The 1-DX code is used primarily to generate a 50-group cross-section library which is characteristic of a specific composition depending on plutonium and fission product concentration

FIGURE 3.1: Sequence of Calculations.

and temperature. In this way, three composition- and temperature-dependent effective cross-section sets were created for all subsequent two-dimensional reactor calculations. The fission product cross section used in these calculations was represented by a single nonsaturating nuclide, corresponding to ^{239}Pu fission products, with its cross section doubled (yield = 2) and a mass of 237 (to conserve mass). The 50-group structure, fission spectrum, and group velocities are given in Table 3.1.

The cross sections generated by the above procedure were then used in the 2-DB code⁽¹⁰⁾. This code solves the two-dimensional diffusion theory equation, and thus provides a two-dimensional representation of the reactor. (R-Z), (X-Y) and various hexagonal geometries are allowed; however, for all the calculations of interest, only the (R-Z) representation was used. This code was used to carry out both static k_e (multiplication constant) calculations and fuel cycle studies. Besides the value of k_e , the code provides information on breeding ratio, doubling time, fluence, composition at the end of every burnup step, peak/average power, flux and power distributions.

In order to determine the spatial distribution of the various material worths, perturbation theory calculations were carried out using the PERT-V code⁽¹¹⁾ using energy dependent real and adjoint flux distributions obtained from the 2-DB code. In addition to worth distributions, it is desirable to determine the Doppler coefficient and its spatial variation for ^{238}U . For this purpose, ^{238}U cross sections were generated at three different temperatures (300°K, 600°K and 900°K) and from these the Doppler coefficient could be determined. The value of β_{eff} (effective delayed neutron fraction) was also determined using this code and the delayed neutron data due to Tuttle⁽¹²⁾. Delayed neutron spectra by energy group and precursor group were obtained from the LIB-IV library.

TABLE 3.1

GROUP STRUCTURE, FISSION SOURCES, AND VELOCITIES

<u>Group</u>	<u>Energy Range (eV)</u>		<u>Lethargy</u>	<u>Chi for Pu Fueled</u>	<u>Chi for U Fueled</u>	<u>Velocity (cm/s)</u>
1	1.9971+7	1.0000+7	0.692	2.66500-3	1.98200-3	5.27745+9
2	1.0000+7	6.0653+6	0.5	3.23930-2	2.75000-2	3.89009+9
3	6.0653+6	3.6788+6	0.5	1.21445-1	1.12174-1	3.02960+9
4	3.6788+6	2.2313+6	0.5	2.10381-1	2.05204-1	2.35946+9
5	2.2313+6	1.3534+6	0.5	2.22367-1	2.24516-1	1.83755+9
6	1.3534+6	8.2085+5	0.5	1.72323-1	1.77776-1	1.43108+9
7	8.2085+5	4.9787+5	0.5	1.10173-1	1.15178-1	1.11453+9
8	4.9787+5	3.8774+5	0.25	3.60350-2	3.79300-2	9.18596+8
9	3.8774+5	3.0197+5	0.25	2.65500-2	2.80330-2	8.10638+8
10	3.0197+5	2.3518+5	0.25	1.92630-2	2.03880-2	7.15404+8
11	2.3518+5	1.8316+5	0.25	1.38100-2	1.46440-2	6.31341+8
12	1.8316+5	1.4264+5	0.25	9.80900-3	1.04160-2	5.57157+8
13	1.4264+5	1.1109+5	0.25	6.91600-3	7.35300-3	4.91689+8
14	1.1109+5	8.6517+4	0.25	4.84900-3	5.16000-3	4.33914+8
15	8.6517+4	6.7379+4	0.25	3.38500-3	3.60500-3	3.82928+8
16	6.7379+4	5.2475+4	0.25	2.35500-3	2.50900-3	3.37933+8
17	5.2475+4	4.0868+4	0.25	1.63400-3	1.74100-3	2.98225+8
18	4.0868+4	3.1828+4	0.25	1.13100-3	1.20600-3	2.63182+8
19	3.1828+4	2.4788+4	0.25	7.82000-4	8.34000-4	2.32258+8
20	2.4788+4	1.9305+4	0.25	5.45000-4	5.76000-4	2.04967+8
21	1.9305+4	1.5034+4	0.25	3.72000-4	3.97000-4	1.80882+8
22	1.5034+4	1.1709+4	0.25	2.57000-4	2.74000-4	1.59628+8
23	1.1709+4	9.1188+3	0.25	1.77000-4	1.89000-4	1.40871+8
24	9.1188+3	7.1017+3	0.25	1.22000-4	1.30000-4	1.24319+8
25	7.1017+3	5.5308+3	0.25	8.40000-5	8.90000-5	1.09711+8
26	5.5308+3	4.3074+3	0.25	5.80000-5	6.10000-5	9.68193+7
27	4.3074+3	3.3546+3	0.25	4.00000-5	4.20000-5	8.54428+7
28	3.3546+3	2.6126+3	0.25	2.70000-5	2.90000-5	7.54030+7
29	2.6126+3	2.0347+3	0.25	1.90000-5	2.00000-5	6.65429+7
30	2.0347+3	1.5846+3	0.25	1.30000-5	1.40000-5	5.87239+7
31	1.5846+3	1.2341+3	0.25	9.00000-6	9.00000-6	5.18237+7
32	1.2341+3	9.6112+2	0.25	6.00000-6	6.00000-6	4.57342+7
33	9.6112+2	7.4852+2	0.25	4.00000-6	4.00000-6	4.03603+7
34	7.4852+2	5.8295+2	0.25	3.00000-6	3.00000-6	3.56178+7
35	5.8295+2	4.5400+2	0.25	2.00000-6	2.00000-6	3.14326+7
36	4.5400+2	3.5358+2	0.25	1.00000-6	1.00000-6	2.77392+7
37	3.5358+2	2.7536+2	0.25	1.00000-6	1.00000-6	2.44798+7
38	2.7536+2	1.6702+2	0.5	1.00000-6	1.00000-6	2.04133+7
39	1.6702+2	1.0130+2	0.5	0.	1.00000-6	1.58979+7
40	1.0130+2	6.1442+1	0.5	0.	0.	1.23813+7
41	6.1442+1	3.7267+1	0.5	0.	0.	9.64257+6
42	3.7267+1	2.2603+1	0.5	0.	0.	7.50964+6
43	2.2603+1	1.3710+1	0.5	0.	0.	5.84851+6
44	1.3710+1	8.3153+0	0.5	0.	0.	4.55483+6
45	8.3153+0	5.0435+0	0.5	0.	0.	3.54730+6
46	5.0435+0	3.0590+0	0.5	0.	0.	2.76264+6
47	3.0590+0	1.8554+0	0.5	0.	0.	2.15155+6
48	1.8554+0	1.1254+0	0.5	0.	0.	1.67563+6
49	1.1254+0	6.8256-1	0.5	0.	0.	1.30498+6
50	6.8256-1	1.0000-5	11.31	0.	0.	5.73538+5

The LIB-IV cross-section library has been checked against standard fast reactor benchmark experiments⁽¹³⁾. The range of experiments covered core sizes from Jezebel (solid unreflected sphere of plutonium metal) to ZPPR-2 (zero power critical experiment of a CRBR-sized core). In general, the results of these intercomparisons indicate satisfactory agreement with measured quantities ($.97 \leq C/E \leq 1.03$). In some cases, such as certain central worth measurements and isolated values of k_e , the values of C/E were outside an acceptable range. However, since the reactor under consideration is neither a purely fast reactor, nor a purely intermediate spectrum reactor, but a combination of the two, these benchmarks may not be adequate tests for the FMSR methods and data. Fortunately a coupled fast-thermal experiment performed on a zero power assembly incorporating this dual spectrum character was available. Details of the analysis are given in Appendix A. This experiment consisted of a fast core surrounded by a natural uranium blanket, which in turn was surrounded by a beryllium reflector; the entire assembly was surrounded by a depleted uranium blanket. Although this experiment did not mock-up the FMSR in either size or composition, the mixed spectrum character was preserved. Therefore the test was considered to be a good one. As pointed out in the Appendix, the computed and measured values of k_e are very close ($C/E = .99$). In addition, the computed and measured spatial distributions of ^{235}U fission agreed very well over the entire range, including the steep peak at the natural uranium/beryllium interface. From these results, it is concluded that the methods and data used are adequate for this initial study of FMSR. As more detailed analyses become necessary, more sophisticated methods utilizing hexagonal geometry and transport theory may be warranted for some applications.

3.2 PRELIMINARY REACTOR PHYSICS RESULTS FOR THE HELIUM-COOLED FMSR

The R-Z model used in the 2-DB diffusion theory burnup code is shown in Figure 3.2. Zone 1 represents the fuel in the outer moderated region, zone 2 represents fuel in the inner moderated region, and zones 3, 4, 5 and 6 represent the fast core regions. Zones 7 through 12 represent the axial blankets. All zones in each horizontal cut through the core are further subdivided in the fuel-management scheme into a total of 34 subzones in order to approximate the required fuel shuffling. The relevant zone compositions are shown in Tables 3.2 and 3.3.

3.2.1 Physics Characterization of the FMSR

(a) Neutron Spectrum

Because of gas cooling and metal fuel, the neutron spectrum in the fast core region is very hard. The nascent fission spectrum is modified in this region by inelastic scattering of neutrons with the uranium and the steel constituents. Therefore, there is little degradation of the fission spectrum. The hard spectrum enhances the neutron economy, resulting in a high central conversion ratio. Another consequence of the hard neutron spectrum is a substantial fast fission bonus from ^{238}U ; in fact, approximately 25% of the power in the fast core is a result of fissioning of ^{238}U .

The neutron flux spectrum at the core center and at a point within the fuel in the moderated region is shown in Figure 3.3. It can be seen that although the spectrum in the moderated region is considerably softer, it is still "hard," in that a considerable fraction of the neutrons are above 1 keV.

The gauge for measuring neutron worth as a function of energy is the adjoint spectrum, or the "importance" of a neutron at a given energy and position. Figure 3.4 shows the adjoint spectrum at the center of the fast core and

FIGURE 3.2: R-Z Model of the He-Cooled FMSR.

TABLE 3.2: FUEL ASSEMBLY VOLUME FRACTIONS

	ASSEMBLY	CONTROL ASSEMBLY (CONTROL IN)
Fuel	.39	
Coolant	.45	NOT YET DEFINED
Structure	.16	
Control		
TOTAL	1.00	

TABLE 3.3: CORE REGION VOLUME FRACTIONS

	ZONE 1	ZONE 2	ZONE 3	ZONE 4	ZONE 5	ZONE 6	MODERATOR ZONES
Fuel	.39	.39	.39	.39	.39	.39	.39
Coolant	.45	.45	.45	.45	.45	.45	.45
Structure	.16	.16	.16	.16	.16	.16	.16
Control	-	-	-	-	-	-	-
Moderator	-	-	-	-	-	-	-
TOTAL	1.00	1.00	1.00	1.00	1.00	1.00	1.00

NEUTRON SPECTRUM

FIGURE 3.3

ADJOINT SPECTRUM

FIGURE 3.4

a point within the fuel in the moderated region. It can be seen that the adjoint spectrum is quite steep above 10 keV, showing the effect of the hard spectrum. The ^{238}U fission threshold is clearly seen, as well as the effect of capture in the low energy range. The adjoint flux is of considerable importance in design and safety evaluations. The neutronic response of the system to perturbations around a design point can be readily predicted, thereby eliminating the need for a large number of repetitive calculations.

(b) Flux Calculations

The one-group spatial flux distribution for the helium-cooled FMSR is shown in Figure 3.5; the corresponding adjoint flux distribution is shown in Figure 3.6. From the figures, it can be seen that neither the flux nor the adjoint peak at the center. Both peak approximately midway between the center of the core and the inner reflector region. This behavior is a consequence of the radial fuel zoning resulting from the fuel shuffling strategy selected for this initial study. Additional shuffling strategies are under study which will result in modified flux and adjoint distributions. A design objective is to flatten the flux and power as much as possible to reduce hot spot effects, minimize orificing requirements, and equalize mean outlet temperatures.

(c) Doppler Coefficient

The calculated neutron flux and adjoint spectra were used in a series of first-order perturbation calculations, to obtain estimates of the Doppler coefficient, fuel and boron worths, and the delayed neutron fraction.

A mapping of the Doppler coefficient is shown in Figure 3.7. The Doppler coefficient is defined as

$$\alpha_D = \frac{Tdk}{dT} \quad (T = \text{temperature in } ^\circ\text{K})$$

TOTAL FLUX (B.O.C.)

FIGURE 3.5

TOTAL ADJOINT (B.O.C.)

FIGURE 3.6

40 cm		40 cm		40 cm	
162.7 cm	-9.177E-5	-3.430E-5	2.363E-5		
	-9.540E-5	-3.620E-5			
	-8.862E-5	-3.682E-5	6.363E-5		
	-9.839E-5	-3.810E-5			
	-1.011E-4	-3.903E-5	1.991E-5		
	-1.026E-4	-4.122E-5			
	-1.004E-4	-4.165E-5	2.688E-5		
	-1.117E-4	-4.425E-5			
	-1.070E-4	-4.432E-5			
	-1.026E-4	-4.430E-5	2.688E-5		
	-9.758E-5	-4.315E-5			
	-9.857E-5	-4.355E-5			
	-8.632E-5	-4.062E-5			
	-8.085E-5	-3.939E-5			
	-8.806E-5	-4.209E-5			
	-9.144E-5	-4.481E-5	6.363E-5		
	-1.159E-4	-5.592E-5			
	-1.459E-4	-7.219E-5			
	-2.188E-4	-1.084E-4			
	-3.045E-4	-1.757E-4			
Beryllium					
-2.492E-4	-1.074E-4				
-1.536E-4	-6.592E-5	3.257E-5			
-1.330E-4	-5.603E-5				
Beryllium					
-3.453E-5	-1.421E-5				
-2.074E-5	-8.546E-6	3.257E-5			
-1.687E-5	-6.739E-6				
Beryllium					
-3.302E-6	-1.357E-6				
-1.894E-6	-7.922E-7	4.842E-7			
-1.292E-6	-5.467E-7				
-1.072E-6	-4.568E-7				
Beryllium					
-2.736E-7	-1.201E-7				
-1.714E-7	2.559E-8	4.842E-7			
-1.277E-7	-5.669E-8				
-1.148E-7	-5.133E-8				
Beryllium					

292.7 cm	Beryllium		
	-2.492E-4	-1.074E-4	3.257E-5
	-1.536E-4	-6.592E-5	
	-1.330E-4	-5.603E-5	
	Beryllium		
	-3.453E-5	-1.421E-5	3.257E-5
	-2.074E-5	-8.546E-6	
	-1.687E-5	-6.739E-6	
	Beryllium		
	-3.302E-6	-1.357E-6	4.842E-7
-1.894E-6	-7.922E-7		
-1.292E-6	-5.467E-7		
-1.072E-6	-4.568E-7		
Beryllium			
-2.736E-7	-1.201E-7	4.842E-7	
-1.714E-7	2.559E-8		
-1.277E-7	-5.669E-8		
-1.148E-7	-5.133E-8		
Beryllium			

FIGURE 3.7: Distribution of the Local Doppler Coefficient.

The overall reactivity Doppler coefficient is

$$\alpha_D = - 0.0045$$

over the range 300° to 900°K. From Figure 3.7, it can be seen that the largest contribution to the Doppler coefficient comes from the zones closest to the internal reflector. This is due to the local spectral softening and corresponding larger capture rate in the resonance region. Axially, the Doppler effect drops off quite rapidly because of the corresponding drop-off of the flux and adjoint. The overall Doppler coefficient is in a range representative of gas-cooled breeders.

(d) Delayed Neutron Fraction

The calculated delayed neutron fraction, $\beta_{eff} = 0.0041$ is approximately twenty-five percent higher than β_{eff} for an oxide-fueled breeder. This is a result of the direct burning of ^{238}U .

(e) Material Worths

Material worth calculations were made for the fissile isotopes ^{235}U and ^{239}Pu in order to obtain an estimate of enrichments required for fueled control rods. The calculated worths at core mid-height and as a function of radius are shown in Figure 3.8 for ^{235}U and ^{239}Pu . The worth of plutonium is approximately 1.4 times the worth of ^{235}U . These worth curves will provide some help in determining the composition and location of fuel control rods.

(f) Breeding Ratio

As stated earlier, the breeding or conversion ratio of the FMSR is higher than that of conventional breeders primarily as a result of the use of metal fuel. In addition, the relatively high packing of natural uranium contributes significantly to the breeding ratio. A map of the spatial dependence of the conversion ratio is shown in Figure 3.9. The variation reflects the local neutron spectrum and distribution of fissile materials. In the fast core region,

FISSILE MATERIAL WORTH DISTRIBUTION

FIGURE 3.8

	40 cm	40 cm	40 cm
162.7 cm	1.612	2.206	3.425
	1.763	2.425	
	1.157	1.457	2.136
	2.793	3.838	4.960
	1.987	2.742	
	1.395	1.866	2.704
	1.214	1.566	
	2.299	3.091	4.960
	1.447	1.958	2.704
	1.226	1.600	
	1.127	1.436	
	1.250	1.650	
	1.019	1.245	2.136
	0.961	1.132	
	1.036	1.272	
	0.985	1.170	
	1.085	1.337	
	1.052	1.241	
	1.106	1.295	
	1.048	1.163	
	Beryllium		
	2.874	3.499	4.547
	2.563	3.181	
	3.527	4.202	
	Beryllium		
	3.958	4.497	4.547
	3.697	4.300	
	4.886	5.396	
	Beryllium		
	5.595	5.850	6.122
	5.569	5.996	
	5.356	5.890	
	6.661	6.840	
	Beryllium		
	6.531	6.510	6.122
	6.254	6.350	
	5.238	5.410	
	4.625	4.584	
	Beryllium		

FIGURE 3.9: Spatial Distribution of the Conversion Ratio.

the core conversion ratio is significantly greater than one, a feature which leads to a net increase in reactivity over a cycle. This is a principal design feature of the FMSR. The spatially-averaged breeding ratio for the helium-cooled FMSR is 1.65.

3.2.2 Static Power Distributions

The power distribution in a reactor is time-dependent. Determination of the time behavior of the spatial power distribution is especially important in the FMSR because of the fuel shuffling strategy employed, whereby a subassembly starts its residence at low power and completes its residence at relatively high power. However, as an equilibrium cycle is achieved, the global power distribution over the cycle should be reasonably constant even though some subassemblies experience larger changes. The beginning of cycle radial power distribution is shown in Figure 3.10. The valleys in the fast zone are the result of introducing low enriched fuel shuffled from the moderator region. The remainder of the power distribution in the fast region is reasonably flat radially as can be seen in Figure 3.10, but exhibits a quasi-cosine shape axially as shown in Figure 3.11. The treatment of the larger gradients at the interface between the fast core and moderator region will require some care, especially with regard to orificing; power peaking near the moderator region will be a major design consideration. Figure 3.12 gives a two-dimensional picture of the overall power distribution.

Figure 3.13 gives a detailed description of the power mapping for each zone used in the analysis. The average power in MWt/liter and the peak-to-average power ratio are given. The highest value of the peak-to-average power ratio (1.41) occurs at the interface between the moderator region and hard spectrum core. The variations in power density in any individual subassembly over the time of its full residence in a given zone are discussed in Section 3.3 which follows.

RADIAL POWER DISTRIBUTION CORE MIDPLANE

FIGURE 3.10

AXIAL POWER DISTRIBUTION
CORE CENTER LINE

FIGURE 3.11

POWER DENSITY (B.O.C.)

FIGURE 3.12

		40 cm		40 cm		40 cm	
162.7 cm	292.1 cm	.225	(1.086)	.108	(1.326)	.028	(1.97)
		.216	(1.092)	.104	(1.222)		
		.277	(1.083)	.148	(1.307)	.035	(2.26)
		.162	(1.085)	.078	(1.319)	.012	(2.11)
		.206	(1.093)	.099	(1.326)		
		.264	(1.089)	.133	(1.318)	.032	(2.01)
		.289	(1.080)	.152	(1.305)		
		.204	(1.081)	.099	(1.312)	.028	(1.97)
		.267	(1.082)	.134	(1.309)		
		.294	(1.077)	.155	(1.301)	.032	(2.01)
		.303	(1.076)	.165	(1.294)		
		.285	(1.078)	.151	(1.295)		
		.300	(1.079)	.174	(1.293)		
		.292	(1.081)	.177	(1.295)		
		.277	(1.083)	.161	(1.298)		
		.266	(1.083)	.161	(1.297)	.035	(2.26)
		.248	(1.083)	.144	(1.297)		
		.239	(1.078)	.145	(1.289)		
		.234	(1.071)	.143	(1.280)		
		.263	(1.136)	.169	(1.352)		
		Beryllium					
		.092	(1.201)	.051	(1.411)		
		.078	(1.109)	.043	(1.310)	.008	(3.46)
		.057	(1.117)	.032	(1.310)		
		Beryllium					
		.028	(1.200)	.016	(1.400)		
		.022	(1.124)	.013	(1.320)	.0008	(3.46)
		.016	(1.094)	.010	(1.280)		
		Beryllium					
		.007	(1.066)	.005	(1.250)		
		.005	(1.066)	.003	(1.250)	.0007	(3.63)
		.005	(1.066)	.003	(1.250)		
		.004	(1.007)	.002	(1.250)		
		Beryllium					
		.002	(1.065)	.001	(1.240)		
		.0016	(1.065)	.001	(1.240)	.0007	(3.63)
		.0016	(1.064)	.001	(1.240)		
		.0018	(1.064)	.001	(1.240)		
		Beryllium					

FIGURE 3.13: Spatial Distribution of Average Power (MWT/L) and Zonal (Peak/Average).

3.3 FUEL MANAGEMENT DESIGN STUDIES

This section provides a description of the burnup calculations performed with the 2DB diffusion theory code. The fuel shuffling scheme selected for this initial study involves loading fresh natural uranium into the outer moderated region and then moving it into the central fast region, from where it is discharged. The plutonium enrichment increases from about 2.7% in the moderated regions to above 7% at time of discharge from the fast region.

3.3.1 Fuel Management Strategy

The fuel management strategy selected for this initial study is shown schematically in Figure 3.14. Basically, natural or depleted uranium is fed into reactor zone 1 which is located in the outer moderator region. After a prescribed residence time, it is successively shuffled toward the fast zone from which it is discharged. All fuel resides in one of six zones.

Figure 3.15 shows the R-Z zone designation for the fuel depletion model. The reactor was represented by 34 radial fuel subzones, each containing 12 fueled subassemblies. In the axial direction, the reactor was represented by three zones, two in the core region and one representing the axial blanket. (In the calculations only one quadrant of the cylindrical reactor is calculated.) The adequacy of using only 2 zones to represent the reactor core axially as well as only one axial zone for the axial blanket will need to be investigated.

The number designation of a radial fuel sub-zone in the first axial layer is a measure of the zone residence time in the reactor, e.g., at the end of any given cycle zone 1 has been exposed for one cycle, zone 20 for twenty cycles etc. In the second axial layer zones 36 and 55 are physical extensions of zones 1 and 20, respectively, and have corresponding exposure times. The subassemblies are discharged after 34 cycles, of which 20 cycles were spent in the fast zones.

FIGURE 3.14: Fuel Shuffling Strategy.

FIGURE 3.15: A Schematic Representation of FMSR for the Fuel Management Strategy.

The problem of transition from the startup core to an equilibrium core has not yet been fully addressed. For the present work the equilibrium compositions for the various zones were obtained in the following manner. An initial estimate of the uranium, plutonium, and fission fragment isotope number densities was made for the various zones. These number densities were determined from earlier preliminary calculations and as such were reasonably accurate. A search on concentration of the heavy nuclides was then performed to obtain a critical reactor. Reactor burnup calculations were then made for 34 refueling cycles to obtain equilibrium cycle compositions with natural uranium feed. Hence, at the end of 34 cycles, all the initial fuel has been discharged from the reactor. Each burnup cycle then becomes a duplicate of the previous one in which all thirty-four zones are shuffled at the end of each cycle (i.e., zone 34 is discharged and replaced by zone 33, 33 by 32, etc. until zone 1 is replaced by the natural uranium feed). However, since in the present shuffling scheme only six zones are shuffled per cycle, each burnup cycle will be slightly different from the previous cycle. The equilibrium cycle results quoted are for the fuel arrangement conforming to that shown in Figure 3.15.

During each burnup cycle, the effective multiplication factor of the reactor increases by about two percent. Reactivity control of the reactor is accomplished by means of fueled control rods. It is well within the capability of a judiciously chosen distribution of fueled control elements to add approximately two percent in k_e ($\$4.5$) reactivity to the reactor. A key design problem associated with such a scheme will be the necessity to provide sufficient cooling for the resulting higher power densities in the control element and its surroundings. A cycle length of 185 days was selected which results in a k_e of between 0.98 and 0.99 at the beginning of the cycle. The extra reactivity needed for criticality can be obtained by the insertion of appropriate

fueled control rods. As plutonium is bred during the cycle, the fueled control rods are progressively withdrawn to maintain criticality. Detailed analyses of the FMSR reactor incorporating control rods are currently underway.

The value of k_e at the beginning of the cycle can be increased to, say 0.99 or even 1.0 if a longer cycle length is adopted. From past calculations, it is estimated that the k_e at the beginning of the cycle can be increased by one percent if the cycle length is increased by ten days. Increasing the cycle length by 10 days increases the subassembly residence time in the moderated region by 140 days. The plutonium concentration at the time of insertion into the fast zone is therefore increased with a corresponding higher beginning of cycle k_e . This would mean that the reactor would be super-critical at the end of the cycle and would require neutron poison shutdown rods in addition to control rods. The longer residence period would also result in higher fluence damage to the clad, unless the number of cycles is correspondingly reduced.

The geometrical placement of a fueled subassembly in the core and its subsequent shuffling must take into account the core radial power distribution and its variation during the subassembly residence time. When the subassembly is shuffled from the moderated region into the fast region, the average plutonium enrichment is 2.7%. After introduction into the fast region has taken place, the plutonium content increases rapidly with each cycle until it reaches a saturation level of approximately 7%.

In order to minimize the number of shuffles at the end of each cycle, a fuel subassembly is kept in the same geometrical position in the core for many cycles. However, the number of cycles will vary depending on the position within the core. Generally, an element resides in the moderated region for a comparatively long time. After it has been moved to the fast region, the residence times at first are short, since plutonium builds up rapidly. Once a

saturation concentration of plutonium is approached, the residence times in the zones are increased. During this time, the amount of plutonium in the subassembly increases, with a corresponding increase in power density requiring suitable reorificing. It is estimated that less than 20% of the subassemblies in the fast zone would need to have their orificing changed during their residence time in that zone. However, a primary objective of the fuel management strategy is to minimize this power swing. A considerable degree of success has been achieved in the current design, as will be discussed later.

Fuel management proceeds as follows: Two fuel rings are used in the moderated region of the reactor. The outermost ring has eight sub-zones consisting of 12 subassemblies each; the inner ring consists of six sub-zones. Fresh subassemblies are fed into the outermost ring. After eight cycles in the same position, the fuel is shuffled into the inner ring for a residence time of six more cycles in one position before it is shuffled into the fast region.

The subassemblies are shuffled a total of four times within the fast zone before being discharged from the reactor. The periods of residence between these shuffles vary. A residence time of 3 cycles occurs between the time of introduction into the fast core and the first shuffle, 2 cycles between the first and second shuffles, 6 cycles between the second and third shuffles and 9 cycles between the third shuffle and of discharge from the reactor. In all, the subassemblies reside for 14 cycles in the moderated region and 20 cycles in the fast region. The procedure is summarized in Table 3.4.

3.3.2 Results of Fuel Management Studies

The performance of the helium-cooled FMSR has been studied for the fuel shuffling strategy discussed in the previous section. It should be emphasized that this shuffling strategy represents only one of many possible alternatives. For this preliminary initial study, it was essential to select one as a

TABLE 3.4

Fuel Loading for the Fast-Mixed Spectrum Reactor
408 Elements, Pu/U Metal Core and Axial Blankets

	<u>Zone 1</u>	<u>Zone 2</u>	<u>Zone 3</u>	<u>Zone 4</u>	<u>Zone 5</u>	<u>Zone 6</u>
Fuel S/A	96	72	36	24	72	108
Fuel S/A Loaded Per Cycle*	12 (loaded)	12 (shuffled)	12 (shuffled)	12 (shuffled)	12 (shuffled)	12 (shuffled)
43 Fissile Loaded Per Cycle (Kg)	38.6 (²³⁵ U in Nat.U)	0	0	0	0	0
HM Loaded Per Cycle (Kg)	5,510	0	0	0	0	0
Fuel Residence Time (EFPD)	1480	1110	555	370	1110	1665

* All equilibrium cycle

reference. Other strategies may perform better with regard to power peaking and residence time. Several are presently under study and will form the basis of a later report. No analysis of control over the cycle has been made. Control requirements are expected to have an effect on performance, particularly on local power gradients; these problems are presently being studied.

In the present design, the moderator regions contribute relatively little power and reactivity. Therefore much of the discussion to follow will focus on results for the fast region. The primary function of the moderator region is to breed plutonium to a 2.7% enrichment. However, since the effective fission cross section in the moderator region is about four times its value in the fast region, power generated in the moderated region is sufficient to help sustain a reasonable radial power distribution.

Figure 3.16 shows the buildup of the plutonium concentration during each shuffle in the fast core. The sub-zones labeled "3" in Figure 3.16 refer to sub-zones into which the subassemblies are shuffled after removal from the moderated zone. Only 12 subassemblies are shuffled at any time. Therefore, subassemblies shuffled to zone 3, designated "3", would reside in these positions for three cycles, after which they would be moved to zone 4 locations designated as "4". The fuel would remain in these locations for two cycles and then be moved to locations designated "5". The fuel would reside in a "zone 5" location for six cycles, after which it would be moved to a "zone 6" location. After a residence of nine cycles in a zone 6 location, the fuel would be discharged from the reactor.

Fuel with low Pu content is placed first near the center of the reactor in order to flatten the power distribution. Shifting the fuel to the outer regions of the fast spectrum core as plutonium is built up also helps to

FIGURE 3.16

flatten the radial power distribution. As seen in Figure 3.16 the Pu content increases with exposure with the largest increase per cycle occurring in the "zone 3" or entry location. The enrichment approaches an equilibrium value of approximately 7 percent. The zone 6 plutonium concentration changes very little for that reason.

The enrichment shown in Figure 3.16 is combined with the local fluxes to form the power densities depicted in Figure 3.17. In these figures, the enrichment and power swings correspond to averages over the central axial zone which is 80 cm in height. Due to computer storage limitations, the number of axial sub-zones had to be limited to 6. The effect of this approximation on the results needs to be evaluated, especially with regard to reactivity. The effect of smearing the plutonium over 80 cm in the central fast zone on reactivity is especially important. The power variation shown in the figure corresponds to the full residence time in that zone. For example, the power swing shown for location "3" occurs over 3 cycles. Orificing could be used to compensate for cycle-to-cycle variations which might be desirable for about 20% of the subassemblies. Power peaking near the beryllium can also be seen in Figure 3.17.

Figure 3.18 shows the peak concentration of fission products in fuel as a function of shuffling and residence time. The pattern of fission product buildup in each zone is clearly evident. The final peak concentration of fission products on discharge is reasonably uniform.

a. Heavy Metal Inventory

Tables 3.5 and 3.6 provide inventories of the uranium and plutonium isotopes, fission products, and the fuel and discharge rates for the beginning and end of equilibrium cycle.

POWER DENSITY VARIATION
OVER FUEL RESIDENCE IN SUB-ZONES,
HE-COOLED FMSR

FIGURE 3.17

FISSION PRODUCT CONCENTRATION VARIATION
OVER FUEL RESIDENCE IN SUB-ZONES,
HE-COOLED FMSR

FIGURE 3.18

TABLE 3.5

FUEL INVENTORY AT BEGINNING OF EQUILIBRIUM CYCLE (KILOGRAMS)
Helium-Cooled FMSR

CORE ZONE	U-235	U-236	U-238	Pu-239	Pu-240	Pu-241	Pu-242	FISSION PRODUCTS
1	201.3	1.7	29406.4	42.1	0.4	0.1	0.0	7.0
2	123.6	8.9	21766.2	265.6	12.3	3.5	0.1	64.6
3	41.6	9.1	10533.6	369.8	27.5	5.9	0.4	150.8
4	22.0	6.8	6813.6	345.8	26.2	3.7	0.4	198.0
5	46.8	22.3	19541.8	1330.8	126.5	12.0	1.8	1168.4
6	38.3	34.0	27028.6	2375.5	330.6	28.0	4.2	3541.4
6 ^a CORE TOTAL	473.6	82.8	115090.2	4729.6	523.5	53.2	6.9	5130.6
AXIAL BLANKET TOTAL	337.8	21.4	59497.0	1133.2	45.7	4.8	0.2	415.5
TOTAL, REACTOR	811.4	104.2	174576.2	5862.8	569.2	58.1	7.1	5546.1
FEED, KG/YR (75% LOAD FACTOR)	57.2	0.0	8109.3	0.0	0.0	0.0	0.0	0.0
DISCHARGE, KG/YR (75% LOAD FACTOR)	14.7	7.1	6717.9	509.1	81.2	9.1	1.0	827.7

TABLE 3.6
FUEL INVENTORY AT END OF EQUILIBRIUM CYCLE (KILOGRAMS)
Helium-Cooled FMSR

CORE ZONE	U-235	U-236	U-238	Pu-239	Pu-240	Pu-241	Pu-242	FISSION PRODUCTS
1	199.0	2.3	29388.4	58.8	.70	0.1	0.0	9.8
2	115.0	11.1	21648.6	349.9	19.9	5.6	0.2	95.0
3	38.0	9.6	10400.4	433.2	31.8	5.7	0.5	205.0
4	20.2	7.0	6740.6	375.7	29.9	3.7	0.4	239.2
5	42.9	22.5	19317.9	1385.7	139.0	12.5	1.9	1329.0
6	35.4	33.8	26746.7	2395.0	353.0	31.4	4.5	3776.6
5 CORE TOTAL	450.5	86.3	114242.6	4998.3	574.3	59.0	7.5	5654.6
AXIAL BLANKET TOTAL	332.0	22.6	59382.6	1209.9	50.2	5.3	0.3	453.4
TOTAL, REACTOR	782.5	108.9	173625.2	6208.2	624.5	64.3	7.8	6108.0
FEED, KG/YR (75% LOAD FACTOR)	57.2	0.0	8109.3	0.0	0.0	0.0	0.0	0.0
DISCHARGE, KG/YR (75% LOAD FACTOR)	14.7	7.1	6717.9	509.1	81.2	9.1	1.0	827.7

b. Plutonium Inventory

Figure 3.19 shows the mapping of the buildup of plutonium inventory as a function of exposure time for the two axial layers. From the figure, it can be seen that the inventory approaches an equilibrium concentration of approximately 7.5% at the end of 31 cycles, for the central axial 80 centimeters of the core.

Table 3.7 gives an overall summary of the fuel management calculations. As stated previously the helium-cooled FMSR has excellent breeding characteristics. However, the high peak burnup of $160,000 \frac{\text{MWD}}{\text{t}}$ and the high peak fluence of 8.5×10^{23} (nvt > 0.1 MeV) constitute major points of concern; the impact on material considerations will be discussed in Section 4.

3.3.3 Conclusions from the Fuel Management Studies

The preliminary fuel cycle strategy studied so far has indicated the following:

1. A reasonably flat radial power distribution can be obtained with only limited fuel shuffling.
2. The power swings over the cycle should be manageable by means of suitable orificing changes which could be made at mid-residence for a limited number of subassemblies (20%).
3. Manageable and quite conventional reactivity swings are obtainable.
4. Effect of control (fuel or absorber) rods on the radial power distribution needs to be investigated.
5. Other fuel management strategies which can mitigate some of the effects of such factors as high burnup and high fluence need to be investigated. Several design options are under consideration.

PU-239 BUILDUP WITH IRRADIATION

FIGURE 3.19

TABLE 3.7
Summary of the Fuel Cycle Studies
Helium-Cooled FMSR

Number of Fuel Subassemblies		408
Number of Fuel Subassemblies Shuffled Per Cycle		12
Cycle Duration (Full Power Day)		185
Reactivity Swing Over the Cycle, ΔK		0.022
Breeding Ratio		1.65
<u>Inventory (Tonnes)</u>	<u>Pu</u>	<u>Heavy Metal</u>
Core BOC	5.3	126.09
Discharge/Yr*	0.6	8.2
Average Enrichment (%)	7.3	
Burnup (MWD/T)		
Average		141,500
Peak		183,000
Peak Fluence (E > 0.1 Mev)		8.5×10^{23}

*75% Load Factor

3.4 PRELIMINARY RESULTS FOR THE SODIUM-COOLED LMFBR

As stated previously, the sodium-cooled FMSR has been studied to a far lesser extent than the helium-cooled system. From very preliminary heat transfer calculations, it is tentatively concluded that a sodium-cooled FMSR could be cooled with a 0.30 coolant volume fraction, allowing for a 0.50 fuel volume fraction, the remaining 0.20 volume fraction being assigned to steel. The effects of such a design on core pressure drop and pumping power have not yet been investigated. Very preliminary estimates indicate that a relatively high Δp (pressure drop) across the subassembly is one of the consequences. The impact of this value of Δp on the pressure-driven duct dilation problem needs to be assessed.

The methodology and much of the design developed for the helium-cooled FMSR were used in a series of reactor physics calculations for the sodium-cooled FMSR. The fuel management strategy was essentially identical for both systems.

The results of calculations for the sodium-cooled FMSR are summarized in Table 3.8. The relatively high breeding ratio is in large part a consequence of the high fuel volume fraction, and is probably optimistic since more accurate heat transfer calculations, duct dilation analyses, or other engineering design considerations could indicate the need for opening up the core, thereby increasing the coolant volume fraction at the expense of the fuel volume fraction.

TABLE 3.8
Summary of the Fuel Cycle Studies
Sodium-Cooled FMSR

Number of Fuel Subassemblies	408
Number of Fuel Subassemblies Shuffled Per Cycle	12
Cycle Duration (Full Power Day)	185
Reactivity Swing Over the Cycle, ΔK	.026
Breeding Ratio	1.7
<u>Inventory</u> (Tonnes)	<u>Pu</u> <u>Heavy Metal</u>
Core (BOC)	6.33 173
Discharge/Yr*	0.69 10.57
Average Enrichment (%)	6.5
Burnup (MWD/T)	
Average	110,000
Peak	163,000
Peak Fluence ($E > 0.1$ Mev)	8.0×10^{23}

*80% Load Factor

4. FUELS AND MATERIALS

The studies of fuels and materials reported here are the result of a short program of only four months duration. Most of the data used for this evaluation were obtained from Argonne National Laboratory. Important contributions to the metal fuel and cladding evaluations were made by the staffs of the Materials Science Division and the EBR-II Project; the Components Technology Division provided the evaluations of duct designs.

This section will summarize the results to date as received from Argonne. The full evaluations, as reported by Argonne, are given in Appendix B.

Although the material requirements have focused on the helium-cooled system, it is believed that most of the conclusions will apply to the sodium-cooled system. An emerging conclusion from the studies to date, however, indicates that in both systems the fuel, clad and duct materials will be subjected to peak fast fluences of up to 8×10^{23} (nvt > 0.1 MeV) and the fuel exposed to peak burnups of up to 17 atom percent. The coupling of FMSR feasibility to the ability of these materials to meet these requirements makes the fuel and materials evaluations the central issue in determining the future of the FMSR concept. This conclusion should be qualified to some degree because of the preliminary nature of the evaluations to date. Also, the evaluations were based on very preliminary designs which have not been optimized. For example, more refined analysis may yield designs in which the residence time and therefore fluence damage as well as fuel burnup could be reduced by the judicious tailoring of the fast and moderated spectrum zones and through use of alternate fuel shuffling patterns.

4.1 FUEL FOR THE HELIUM-COOLED FMSR

The central question regarding the use of metal fuel in the helium-cooled

FMSR relates to the adequacy of extrapolating experience with Mark-II metal fuel to the helium-cooled FMSR. For example, the Mark II fuel is a sodium-bonded fuel which has operated successfully to high burnup levels in the sodium-cooled EBR-II reactor. The sodium inside the clad provides excellent heat transfer to the clad during the early, rapid swelling of the metal fuel, which occurs at burnups to about 2.5 to 3.0 atom percent. After that point the fission gas, which tends to migrate to grain boundaries, generates a state of interconnectedness of these fission gas pores. Approximately seventy to eighty percent of the fission gas generated after that point is released, and subsequent swelling is greatly reduced. R. Seidel of the EBR-II Division has observed in a recent irradiation a metal fuel pin which had scarcely grown in the axial direction after 8.4 atom percent burnup.

There are two favored candidates for the helium-cooled metal fuel which could provide the desired initial low smear density (about 75% of theoretical density) and good fuel-to-clad thermal conductivity. One is to use a sintered fuel of the correct smear density, the other is the use of the VIPAC fuel loading procedure. The VIPAC procedure consists of simply filling the clad of the fuel pin with natural uranium metal powder. Vibrating the fuel pin during loading assures the correct settling of the fuel in the pin. The VIPAC procedure may become the preferred fuel "fabrication" technique of the future. For oxide fuels it offers simple remote handling advantages.

The metal fuel would be vented to release fission gases, using the same procedure as is currently proposed by GA for oxide fuel. However, some added precautions may be needed to protect the hot metal fuel within the cladding from impurities in the internal gas system such as H_2 , O_2 , and H_2O . A larger pin diameter may be desired for economy of fabrication and reduction of pumping power losses. The pin diameter choice must be consistent with the ANL

recommendation that 850°C should be considered as a tentative maximum fuel centerline operating temperature. ANL also recommends that the uncertainties in estimating this number must be considered, and that a confirmatory instrumented experiment is also very important. A light coating of oxide film on the metal particles is recommended as an aid for prevention of excessive sintering.

Metallic interdiffusion between the uranium-plutonium fuel and steel clad must also be considered. An immediate advantage results from the use of the recommended HT-9 alloy for the clad. Since HT-9 contains hardly any nickel, the preferential diffusion of nickel into the uranium metal is essentially eliminated. A second recommendation is made by ANL in which it is proposed that a very thin barrier of vanadium be placed between the fuel and clad to greatly inhibit interdiffusion. For heat transfer purposes, a maximum fuel-clad interface temperature limit of 570°C is tentatively imposed. An incidental major safety advantage is achieved if a vanadium barrier is used. In the case of an abnormal transient, the fuel and clad temperatures may rise well beyond normal operating conditions. Destruction of the cladding through formation of a eutectic between the fuel and the vanadium barrier would not occur until the temperature exceeded 1040°C, well above the operating maximum of 570°C.

ANL feels sufficiently confident about the potential of the fuel to say that "The U-Fs* EBR-II fuel has demonstrated the potential for metal fuels to successfully achieve the burnup desired in the FMSR." (Section 4 of the ANL Review, Appendix B). Nevertheless, much work remains to be done to assure a definite design. This is discussed in considerable detail in Appendix B.

An alternate and highly tentative fuel design concept is being considered by BNL. This fuel also employs VIPAC uranium metal and a vanadium barrier, and it may be explored in the future. In this design, the fuel resides within the

*Fs = "fissium," or fission product equivalent in the metal.

duct walls while the helium coolant passes through a series of straight tubes through the duct, somewhat as in a straight-through heat exchanger. The major advantage of this design stems from the fact that use of the conventional fission gas venting procedure employed in GCFR design results in a small pressure difference between the inside and the outside of the duct wall. The pressure-driven creep which causes the subassembly walls to interfere, requiring large gaps between subassembly walls would be greatly reduced or eliminated. This is a major design advantage and is discussed in Part II of the ANL review.

4.2 FUEL FOR THE SODIUM-COOLED FMSR

Essentially all of the work performed by ANL in support of the FMSR program has been directed toward the helium-cooled version. It has tentatively been assumed that the sodium-cooled FMSR would have a major advantage in the fuels area, in that the fuel could now be very close in design to the sodium-bonded Mark-II fuel which has been irradiated to very high burnup in EBR-II. This fuel design will be considered in the future. However, a question which must be examined is the impact of the larger proposed pin diameter, with a centerline temperature of about 850°C. This temperature is greater than that used in previous irradiations; it also brings the metal alloy into a new metallic phase. Another question relates to the possible need for physical support of the spongy metal fuel under the load of its own weight. It may be possible to vent the sodium-bonded metal fuel directly to the sodium cooling, something not achievable with oxide fuel. This would remove the need for a large fission gas plenum and alleviated stress-related creep dilation of the clad during irradiation.

4.3 CLAD FOR FMSR FUEL

The ANL review and recommendations for cladding selection are contained in Part I of Appendix B. Considerations in making such a choice are also discussed. Some of the information contained therein has been summarized in the previous section on FMSR fuel. Table 4.1, extracted from the ANL summary, gives a synopsis of the cladding study. The ANL commentary on this table identifies ordinary 316 stainless steel and D-9 steel as unacceptable and concludes that D-21 is marginal for FMSR clad. The last four alloys in the table, however, appear to be acceptable as far as can be determined from irradiation to 2×10^{23} nvt > 0.1 MeV. ANL prefers the HT-9 ferritic alloy over the D-66 and D-68 austenitic alloys for the cladding. This is based on current information which indicated that this alloy retains a greater ductility after irradiation and exhibits minimal irradiation swelling. The ferritic steels might also be more compatible with sodium, although this difference might not be of particular importance, at least for the helium-cooled FMSR.

The possibility of venting both the helium- and sodium-cooled fuels for the FMSR could be of considerable importance toward controlling the clad dimensions under conditions of extensive irradiation.

4.4 DUCT WALL CONSIDERATIONS FOR THE FMSR

The evaluation by ANL of the behavior of the subassembly ducts proposed for the FMSR reactor is contained in Part II of Appendix B. As a result of analyses made for the first FMSR design, this study has resulted in two major conclusions (page 22):

"1. The core restraint aspects of the design studied are adequate to prevent excessive free bow due to irradiation creep and swelling. The associated assembly displacements and forces during operation are acceptable.

TABLE 4.1
Extrapolated Cladding Properties at $5 \times 10^{23} \text{ n/cm}^2$

Alloy	% $\Delta V/V$ ¹	In-Reactor Creep Ductility, % $\Delta D/D$	Postirradiation Total Elongation, % $\Delta L/L$ ²	Wt. % Ni
316	110	4	1.5	10
D-9	77.9	3.5	1.6	15.5
D-21	15.6	0.4	0	25
D-66	0.2	0.6	0	40
D-68	7.2	1.3	0	34
D-57	0.8	2	3	0
HT-9	3.1	2	6	0.5

¹Maximum calculated swelling in the temperature range 530-575°C.

²Projected to $2 \times 10^{23} \text{ n/cm}^2$, tested at 110°C above the irradiation temperature.

2. The duct design will result in excessive duct dilation to the point where the ability to remove assemblies for fuel transfer is doubtful."

The second conclusion had been predicted in early conferences, and was confirmed by these preliminary calculations.

The Components Technology Division of ANL has recommended a series of design changes by which this problem might be resolved. These changes will be studied in the future, and one or more of the most promising design options may emerge as a duct design option for the FMSR. For the purpose of this report, the options will only be listed in summary form, along with BNL comments on their possible effectiveness.

1. Free Dilation Approach. This is simply a restatement of the conventional design approach: give enough space for dilation and make the duct stronger. The free space required for expansion and the increased steel fraction required would most likely result in a severe neutronic penalty for the FMSR concept. This would probably not be an acceptable solution.

2. Constrained Dilation Approach. This suggestion would be very interesting for both the FMSR concept and as a fundamental approach to LMFBR and GCFR designs. The basic idea is to provide much less space between the ducts and allow the duct faces to rest against each other over considerable areas. The implementation of this idea would require considerable care with regard to its full significance, but the payoff could be significant. Its feasibility needs to be demonstrated.

3. Round Duct Design Approach. There is a factor of approximately sixty in the advantage of a round duct over that of a conventional hexagonal duct having the same wall thickness, in regard to resistance to pressure-driven creep under irradiation. Reference 4 of Part II in the ANL report describes a simple triangular pitch of fuel pins within circular ducts of reasonable

spacing and wall thickness. The net fuel volume fraction in this new configuration is only slightly less than the corresponding fraction of a hexagonal lattice having the same pitch.

This option may be significant for the FMSR; evaluation of its potential could be relatively rapid and dependable. A major incidental advantage of the round duct design is elimination of the tendency of the walls to grow away from the fuel pins at the edge of the subassembly as in the hexagonal geometry, causing excessive local coolant bypass.

4. Duct Replacement. ANL suggests that it might be possible to remove and replace the ducts as often as necessary in order to preserve geometrical limits. This might become more and more difficult as the fuel and clad continue through the full irradiation program.

5. Vented and Ductless Designs. Duct designs which have varying strategies and degrees of venting are frequently proposed. Any design which reduces the pressure differential between the inside and the outside of the duct wall will correspondingly reduce the pressure-driven dilation. BNL has been discussing with ANL a concept it has begun to consider in which the VIPAC fuel is compacted into the duct itself and the helium coolant flows through many coolant channels passing through the fuel. If the fuel in this new geometry is vented as usual, there will be almost no pressure differential on the duct wall. The coolant flow tubes, if they were cylindrical, would again be very resistant to creep and would take the pressure of the flowing helium. Such a design would resemble in part that used in cooling the HTGR fuel, but it would require careful examination before adoption for the FMSR. This concept was mentioned in the last paragraph of Section 4.1.

4.5 MODERATOR

Both beryllium and graphite have been employed in FMSR design studies. Although beryllium has been used in most of the design studies to date, graphite may be more suitable in the neutronic sense. A third alternative, which has received only little attention, is zirconium hydride. The ANL review to date has concentrated on the feasibility of beryllium.

Successful irradiation experience with beryllium has been shown to irradiations up to 3.52×10^{22} n/cm² ($E > 0.1$ MeV), much less than required for FMSR application. A recent correlation, however, predicts that for a fluence of 7×10^{23} n/cm² ($E > 0.1$ MeV), the dilation, $(\Delta L/L)$, would be approximately 15%. If this correlation is valid over the extended irradiation range, it is anticipated that a suitable moderator design could be determined to accommodate such a dilation.

The summary in Appendix B also discusses irradiation damage to the beryllium, as well as considerations relevant to helium gas release in beryllium as a result of the $(n, 2n)$ reaction in the metal. The compatibility of beryllium metal with its environment in the helium-cooled FMSR is found to be quite satisfactory.

A considerable amount of work must be done to evaluate the beryllium-moderator subassembly design as far as adequacy of encapsulation and geometry for appropriate heat removal. For the sodium-cooled version, the compatibility questions must also be considered.

4.6 SUMMARY FOR FUELS AND MATERIALS RECOMMENDATIONS

An accurate summary of the current ANL recommendation of the feasibility of satisfactory design of fuel, clad, moderator and fuel ducts for the FMSR reactor is provided in one paragraph of the cover letter for Appendix B, which states:

"As we have pointed out to BNL, the proposed operating requirements of the FMSR push fuel, cladding, moderator, ducts and other core components into fuel burnups and fluences that far exceed any other applications we are aware of, and even further beyond existing experimental test data. Nevertheless, we believe that there is a reasonable chance that judicious design of the core components may enable the performance goals of the FMSR to be realized."

BNL concurs with ANL in this conclusion and in consideration of the fuel and materials as the most sensitive areas for FMSR feasibility.

If the fuels and materials constraints lead to infeasibility of operation in the mode described, then an alternate mode of FMSR operation would have to be considered. There are several other modes of FMSR operation which should still have a significant resource utilization advantage. However, these concepts would generally require a partial feed of fuel which has a limited enrichment in ^{235}U or Pu. These possibilities have only been touched on, and will be explored further in the near future.

5. THERMAL-HYDRAULIC DESIGN STUDIES

A realistic reactor design must take into account the often conflicting requirements of reactor physics and thermal-hydraulics. In addition, the materials which make up the reactor fuel, cladding, and structure all have operating limits which provide constraints on the design. Finally, state of the art balance-of-plant requirements must be considered for pumping power and operating pressures, which determine the amount of energy which can be extracted from the reactor.

The thermal-hydraulic design of a gas-cooled fast reactor is somewhat simple, because of the single phase nature and chemical inertness of the coolant. Hence, no intermediate coolant loop is needed. However, the inferior heat transfer qualities of helium as opposed to liquid metals require operation at high pressure, and consequently, high pumping power. In order to enhance the heat transfer between the fuel and the coolant, the fuel pins are surface roughened over most of their length.

The thermal-hydraulic design of the FMSR must satisfy the additional constraints arising from materials requirements, as discussed in Section 4. Specifically, the temperature of the fuel centerline and the inner surface of the cladding must be below 850°C and 600°C, respectively. The large thermal conductivity of the metal fuel is an advantage in this regard, but the clad-to-coolant heat transfer coefficients must be dealt with carefully.

The thermal-hydraulic design studies reported here relied heavily on the data base assembled for the General Atomic 300 MW(e) GCFR, with one important difference. The gas-cooled FMSR operates at 2000 psi as opposed to 1320 psi for the GCFR. This was done in order to exploit the superior heat transfer characteristics of the metal fuel as well as to reduce pumping power requirements.

In order to provide as realistic an FMSR design as possible, thermal-hydraulic calculations were iterated with the reactor physics calculations. The present design represents only one stage in the iterative process of achieving a final FMSR design.

5.1 DESCRIPTION OF THE METHODS

Heat transfer, pressure drop, and pumping power calculations for the FMSR design were performed with the JUPITER code⁽¹⁴⁾ developed at Brookhaven for this purpose. The FMSR design evolved from several iterations interfacing JUPITER calculations with physics and materials requirements.

JUPITER is a flexible, one-dimensional, multi-channel, steady-state code for thermal-hydraulic analysis of gas-cooled fast reactors. Flexibility was essential for the code in order to carry out the large number of parametric FMSR studies required before achieving an acceptable design. For example, in JUPITER, the degree of cladding roughness that is available can be tailored to the cladding temperature requirements. To determine the amount of roughening required, the core is divided into five axial segments so that five different types of cladding zones may be used. In addition to the active core region, a lower and upper plenum and a lower and upper axial blanket can also be modeled. The code can compute coolant flow rates required by each channel to achieve a consistent desired temperature rise across all channels. The orifice pressure drops are also computed for each of the channels such that the pressure drops across all the fuel elements are constant. The user can also specify flow rates and orifice loss coefficients as input for each channel. Figure 5.1 gives a schematic description of a JUPITER channel. Each channel is divided into several axial nodes. Power generated at each node (obtained from 2-DB reactor physics results) can be given in either kw/ft or W/cm³.

FIGURE 5.1: Schematic Diagram of a JUPITER Channel.

JUPITER can employ two radial nodes and a central hole in the fuel with axially-dependent radii. Two values of thermal conductivity of the fuel can be used for the two fuel nodes. For the FMSR design, there is no central hole. Although JUPITER allows a fuel-cladding gap, the present FMSR design using VIPAC fuel does not have any gap. The temperature and pressure dependence of the thermophysical properties of the coolant have been taken into account and are given in Appendix C.

Several options are available in JUPITER to model the heat transfer and friction factor at the cladding and coolant interface. As in the GCFR, the roughness multiplication factors for the friction and heat transfer coefficients, which multiply the corresponding coefficients for smooth lattice rods, have been incorporated in JUPITER. The roughness multipliers can be specified as input or calculated by the code from built-in correlations.⁽¹⁵⁾ Because of uncertainties in these values for FMSR conditions, the input parameters have been varied in the parametric studies from 2.0 to 2.3 for the heat transfer multipliers, and 4.0 to 4.4 for the friction factor multipliers for full roughness. Another option available in JUPITER is the use of the subroutine TOMSON, which explicitly treats the cladding roughness design and determines the heat transfer and friction factor for each axial node from the corresponding temperature and velocity profiles. This has been obtained from the BASIC subroutine of CALIOP⁽¹⁴⁾ developed at General Atomic and made available to BNL.⁽¹⁶⁾ Details of the heat transfer and friction factor coefficients are given in Appendix C.

Pressure drop calculations include contributions from friction, momentum change, orifice loss and spacer grids. The spacer grid pressure drop calculation assumes an empirical loss coefficient of 1.373 and a design with 17.9% of the flow area blocked by the grid. These values are representative of the

300 MW(e) Demo GCFR design.⁽⁵⁾ The orifice loss for the peak channel is taken to be zero. The orifice pressure drops for the other channels are adjusted such as to produce the same pressure drop across the full subassembly as in the peak channel. JUPITER predictions for the pressure drop calculations have been checked against the 300 MW(e) GCFR Demo Design and are discussed in Appendix C. The pumping power is calculated separately for each channel (and then summed for the reactor).

An average pin was used for the thermal-hydraulic studies required for this initial study. The corner pin and the wall pin effects as well as hot spot effects are currently being studied. In the present calculations, core flow rates were obtained by first specifying the highest power channel requirements obtained from the physics design. These flow rates and the corresponding orifice loss coefficients were then used to determine the thermal-hydraulic characteristics for the core for a particular burnup condition.

5.2 THERMAL-HYDRAULIC DESIGN STUDIES OF He-COOLED FMSR

5.2.1 General

The heat transport system of the FMSR design closely follows that of the 300 MW(e) GCFR Demo design.⁽⁵⁾ The main flow of helium coolant enters the reactor from the upper cold gas-plenum, passes through the grid plate and enters the core at 300°C. After mixing with the bypass flow in the lower gas plenum, the hot gas leaves the reactor cavity and flows through the outlet ducts to the steam generators. After transferring its heat to the secondary steam circuit, the coolant returns through the circulators to the cold-gas plenum above the reactor. Only processes across the reactor inlet orifices and subassemblies were calculated in the present study.

In order to balance the mixed mean coolant outlet temperature and to

mitigate the effect of the power buildup during the fuel cycle, an orificing scheme is required. In the present study, the orifice settings for fuel subassemblies are determined according to maximum power conditions that occur during the time subassemblies reside in the core. This restriction will be eliminated later, and orifices may be varied several times over the subassembly residence time if necessary, to obtain higher mixed mean core outlet temperature.

To enhance heat transfer between the cladding and helium, the fuel pins are surface roughened over all or part of their length. The degree and length of cladding roughness are important design considerations and are determined by parametric studies to satisfy the fuel and cladding temperature requirements.

5.2.2 Design Goals and Design Parameters

As discussed earlier, the metallurgical and conventional GCFR design requirements dictate the constraints to be imposed on the FMSR design. The main objective is to achieve a design consistent with a 850°C maximum fuel centerline temperature, 600°C maximum cladding-inner-surface temperature, a 36% thermodynamic efficiency, and a reasonable pumping power requirement (15 to 20%). The design goals used in this initial study are shown in Table 5.1.

The general design parameters for the FMSR, such as the fuel pin, pitch, hexcan size, etc., are derived from the information obtained from reactor physics calculations. These are given in Table 5.2; a comparison with the 300 MW(e) GCFR Demo design is also shown. The active length of the core is 160 cm. The upper and lower axial blankets are each 40 cm long. The length of the upper and lower plenums together total 30 cm. There is no central hole or a fuel-cladding gap in this design, which uses the VIPAC fuel discussed in Section 4.

TABLE 5.1
Design Goals for He-Cooled FMSR

Parameter	Design Objective
Max Fuel Centerline Temperature	850 ⁰ C
Max. Clad Inner Surface Temperature	600 ⁰ C
Coolant Temperature Rise	230 $\pm$ 10 ⁰ C
Coolant Outlet Temperature	530 $\pm$ 10 ⁰ C
Inlet Pressure	2000 psi
Maximum Reynolds Number	2 x 10 ⁵
Pumping Power	160 $\pm$ 10 MW

TABLE 5.2

General Design Parameters of FMSR
and Comparison to GCFR Demo Design

Parameter	FMSR	GCFR DEMO
Reactor Power, MWe	1000	300
Active Core Height, cm	160	100
Cladding OD, cm	0.8804	0.7400
Cladding Thickness, cm	0.0432	0.0483
Fuel Pellet OD, cm	0.7940	0.6333
Fuel-Cladding Gap, cm	0.0	0.0051
Duct Wall Thickness, cm	0.254	0.254
Hexcan Size (Inner), cm	18.69	16.55
Fuel Volume Fraction	0.39	0.277
Flowing Coolant Volume Fraction	0.40	0.40
Pitch, cm	1.1354	1.0000
P/D	1.29	1.35
No. of Pins/Subassembly	271	271
No. of Fuel Subassemblies	408	118
Number of Spacer Grids	15	10

5.2.3 JUPITER Model

A 24-channel JUPITER model was constructed for the helium-cooled FMSR thermal-hydraulic design studies. Twenty channels were used in the fast zone with 12 subassemblies in each channel corresponding to the physics model discussed in Section 3. The outer two zones containing 168 subassemblies were divided into four channels. The total power produced in the 160 cm active part of the core is 2600 MWt, 92% of which is produced in the fast zone. The average and axial peak power densities in some of the important channels shown in Table 5.3 correspond to 1110 burnup days. The maximum axial power densities in these channels during their full residence time are also shown in Table 5.3. This table is a partial representation of the information to be found in Figure 3.17. One again can see the variation in power with fuel shuffle as the plutonium builds up in the relevant zone. The maximum axial average power of 9.33 kw/ft was produced in radial zone 3 (reactor physics zone 26) at a burnup of 1480 days. The axial peak-to-average power ratio for this case was 1.35. The axial power profile and the axial nodal arrangement for the peak channel are shown in Figure 5.2. Other design data used for the core model are also shown in Table 5.2. The thermophysical properties used are given in Appendix C in Table C1. Fifteen spacer grids were used. The spacer grid design follows the 300 MW(e) GCFR design and block 17.9% of the coolant flow area.

5.2.4 FMSR Core Design and Thermal-Hydraulic Performance

The primary objective of this initial study was to optimize the performance of the plant, i.e., to maximize core outlet temperature and fuel volume fraction while minimizing core pressure drop and pumping power. The operating conditions also had to be consistent with the fuels and materials requirements discussed previously. Using these criteria as a basis, a series of parametric

TABLE 5.3
Axial Power Distribution in Different Channels of He-Cooled FMSR Core

Radial Zone No.	Reactor Physics Zone	No. of Subassemblies Shuffled	1110 Days Burnup		Maximum During Residence Time	
			Power (KW/FT)		Power (KW/FT)	
			Average	Maximum	Average	Maximum
1	19	12	6.30	9.20	6.30	9.20
2	18	12	6.14	8.90	6.62	9.60
3	26	12	8.48	11.55	8.79	11.85
4	15	12	4.48	6.50	5.69	8.28
5	17	12	5.56	8.11	5.56	8.11
6	21	12	7.13	10.26	8.01	11.30
7	24	12	7.81	11.07	8.06	11.37
8	16	12	5.25	7.62	5.82	8.45
9	20	12	6.95	9.98	8.02	11.22
.	.	.	.	.	.	.
.	.	.	.	.	.	.
.	.	.	.	.	.	.
17	27	12	6.88	9.16	6.84	9.09
18	31	12	6.37	8.63	6.69	8.86
19	30	12	6.31	8.54	6.57	8.66
20	34	12	7.44	9.89	7.44	9.89
21	13	36	2.08	2.87	2.08	2.87
22	10	36	0.83	1.12	0.83	1.12
23	6	48	0.20	0.25	0.20	0.25
24	2	48	0.06	0.07	0.06	0.07

FIGURE 5.2: Axial Power Profile and Nodal Arrangement for the Peak Channel
(Power Generated in the Upper and Lower Axial Regions Not Shown).

calculations were made including trade-off studies to determine the length and degree of fuel pin roughening required in order to meet fuel and cladding temperature requirements. Results for these studies are shown in Table 5.4 for the peak channel, i.e., for radial zone 3 after 1480 days of burnup. The inlet and outlet coolant temperatures for this study were 300°C and 540°C, respectively. It should be noted that 540°C was the maximum coolant outlet temperature allowed in any subassembly at any time in the present study. The coolant flow rates for the different cases studied shown in Table 5.4 were different; each case, therefore, produced a different core pressure drop, requiring a correspondingly different pumping power. The last three cases also show the sensitivity to the heat transfer and friction factor coefficients. The TOMSON subroutine case used a rib-height, HR, 0.01 cm and a temperature ratio, TR, 1.1. The predicted maximum fuel centerline temperature varied from 814° to 893°C. These results thus indicate that a design satisfying the fuel and cladding temperature requirements can be obtained with 100 cm of fuel pin roughening. The grid spacer pressure drop for the last three cases was 26 psi. The fourth case in which the TOMSON subroutine was used for the heat transfer and friction factor coefficients evolved as the reference design for this study. The axial variation of the fuel centerline temperature profile for the peak channel for this case is shown in Figure 5.3, along with the temperature profiles for the fuel pellet surface, mid-clad, and coolant temperatures. As seen in Figure 5.3, a sharp change in fuel and cladding temperatures occurs at the interface of the smooth and the roughened pin surfaces. The coolant flow rate through the subassemblies is 2317 kg/sec, of which 93% flows through the twenty inner fast zones. The mixed mean outlet temperature is 532°C.

TABLE 5.4

Parametric Studies for He-Cooled FMSRPeak Power Channel

	Max Fuel Centerline Temp (C)	Max Clad Inner Surface Temp (C)	<u>Pressure Drop psi</u>		Core Pumping Power (MW)
			Active Core	Total	
Smooth Fuel Pin	893	617	18	58	92
80 cm of Fuel Pin Roughened*	870	577	52	92	151
90 cm Roughened*	838	571	56	96	157
100 cm Roughened*	833	571	60	100	163
100 cm Roughened [/]	814	561	61	101	167
100 cm Roughened ^{\$}	823	566	56	96	158

* Heat Transfer and Friction Factor from TOMSON Subroutine.

[/] Heat Transfer and Friction Factor Multiplier 4.4 (2.3).

^{\$} Heat Transfer and Friction Factor Multiplier 4.0 (2.0).

JUPITER PREDICTION OF AXIAL TEMPERATURE PROFILE

FIGURE 5.3: Peak Channel Axial Temperature Profiles for Fuel Centerline, Fuel Pellet Surface, Mid-Clad and Coolant.

From Table 5.4, the maximum cladding inner surface temperature under nominal conditions is 571°C. If hot spot effects due to random and calculational uncertainties are applied, it is estimated that the inner surface cladding temperature could be as high as 607°C for the 2 σ confidence level. Since this temperature is not too far from the metallurgical temperature limit, the present nominal results are therefore considered to be reasonably representative.

The pressure drop developed across the fuel subassembly was calculated from the sum of friction and acceleration pressure drops plus pressure drops due to spacer grids and orifices. The contributions for the increased friction factors resulting from fuel pin roughening were included in the pressure drop calculations. Details of the pressure drop results are shown in Table 5.5 for the peak power channel. The total pressure drop across a fuel subassembly is 100 psi, of which 60 psi is frictional pressure drop in the active core region and 26 psi for the fifteen spacer grids used in this study. The grid spacer pressure drop accounts for a significant part of the total fuel element pressure drop. The spacer grid design used here is the same as used in the GA 300 MW(e) design. This should be investigated further for improvement. There is no orifice pressure drop for the peak channel. The orifices for the other channels are adjusted to keep the pressure drop uniform in the reactor core. The pumping power required for the core is 163 MW.

The essential thermal-hydraulic characteristics of the He-cooled FMSR core are summarized in Table 5.6, and compared to those of the 300 MW(e) GCFR design. The FMSR has certain distinct characteristics. It has a higher operating pressure and a higher pressure drop across the core. However, the $\Delta p/p$ for the FMSR (5.0%) is reasonable compared to that of the GCFR (3.4%). The coolant flow rates per unit coolant area are higher for the FMSR compared to

TABLE 5.5
Pressure Drop Description for the Peak Channel
of He-Cooled FMSR

	(psi)
Reactor Entrance to Fuel Element	0.03
Orifice	0.00
Smooth Upper Part of Fuel Pin	4.95
Active Core Region	59.76
Smooth Lower Part of Fuel Pin	7.33
Momentum Change	1.65
Spacer Grid	26.10
TOTAL	99.82

Orifice Loss Coefficient = 0.00

Total Pumping Power Needed for 12 Subassemblies = 9.925 MW

TABLE 5.6

He-Cooled FMSR Thermal-Hydraulic Characteristics
and Comparison to GCFR Values

	<u>FMSR</u>	<u>GCFR</u>
Heat Output from Core, MWt	2780	826
System Pressure, psi	2000	1230
Reactor Pressure Drop, psi	100	40
Inlet Coolant Temperature, °C	300	312
Mixed Mean Core Outlet Temperature, °C	532	540
Peak Channel Average Heating Rate, KW/ft	8.79	10.41
Coolant Flow through Fuel Element, Kg/sec		
Fast Zone	2165	
Outer Zone	152	
TOTAL	2317	~650
Maximum Inner Cladding Temperature, °C	580	~800
Maximum Fuel Centerline Temperature, °C	850	~ 2100
Core Heat Transfer Surface Area, m ²		
Fast Zone	2878	
Outer Zone	2015	
TOTAL	4893	724
Avg. Peak Channel Surface Heat-Trans. Coefficient		
Btu/hr-ft ² - °F		
Rough Surface	3290	2400 (Avg.)
Smooth Surface	1820	
Maximum Reynolds Number	1.8x10 ⁵	~1.1x10 ⁵

those of the GCFR, resulting in a higher pumping power requirement. The Reynolds numbers in the fast zones of the FMSR vary from 9.0×10^4 to 1.8×10^5 . In the design of the FMSR, the coolant flow rates were made as low as possible by reducing the power density as much as possible. Due to the low cladding temperature constraint, the FMSR required a higher cladding-to-coolant heat transfer coefficient. This was achieved by going to a higher coolant flow rate, resulting in higher Reynolds numbers, and extending the length of surface roughening. The average heat transfer coefficient used for the peak channel was $1820 \text{ Btu/hr ft}^2 \text{ } ^\circ\text{F}$ for the smooth part of the pin and $3290 \text{ Btu/hr ft}^2 \text{ } ^\circ\text{F}$ for the rough regions. Optimization studies will be made to relax these constraints.

A parametric study was made to investigate the sensitivity of the pumping power to the inlet coolant temperature variation. For this study, the temperature rise across the core was increased to maintain the same mixed mean coolant outlet temperature as in our reference design. Results for this study are shown in Table 5.7. It is seen that there is an approximate decrease of 2.4 MW of pumping power per $^\circ\text{C}$ increase in the core coolant temperature. Thus, this information would be employed in a total detailed balance-of-plant evaluation which would then be made and optimized for a minimum pumping power requirement. In this regard, the higher operating helium pressure in the FMSR would be advantageous, because it should deliver more heat to the steam generators. This will reduce the temperature of the coolant at the core inlet and thus increase the temperature drop across the core.

Finally, if the fuel centerline temperature would have to be reduced further because of the need for additional design margin, one way to achieve this and keep the same fuel volume, would be to decrease the fuel pin diameter. The reference design was therefore studied for a 331 pin subassembly design

TABLE 5.7

Sensitivity Study of Pumping Power
to Temperature Rise Across the Core

Coolant Inlet Temperature (°C)	Temperature Rise Across Core in Peak Channel (°C)	<u>Pressure Drop psi</u>		Core Pumping Power (MW)
		Active Core	total	
310	230	67	112	196
300	240*	60	100	163
290	250	56	94	147

* Reference design

using the same power profiles. The design parameters for this study are shown in Table 5.8. The results for the 271 and the 331 pin subassembly cases are shown in Table 5.9. It is found that the fuel centerline temperature can be reduced; however, the pressure drop and the pumping power will increase by about 15%. Thus, if the balance of plant would allow a further increase in the coolant temperature rise, the 331 pin design may be recommended for the helium-cooled FMSR final design provided problems associated with the edge pin channel effects are not enhanced.

5.2.5 Conclusion

The initial thermal-hydraulic studies of the helium-cooled FMSR indicate that a viable core design can be achieved which meets the following characteristics:

- . Maximum fuel centerline temperature below 850°C
- . Maximum cladding inner surface temperature below 590°C
- . Fuel pin and subassembly heat transfer characteristics utilizing conventional GCFR designs
- . Inlet and mixed mean outlet temperature consistent with GCFR designs

Further work in the design studies should address the following objectives:

- . Determine ways to reduce pumping power
- . Allow more design margin for the fuel centerline and cladding temperatures
- . Consider edge pin channel effects
- . Study the importance of hot spot effects
- . Study advantages of utilizing alternative spacer grid designs
- . Determine moderator cooling requirements

TABLE 5.8

Fuel Pin Parameters for 331 Pin He-Cooled
FMSR Subassembly

Parameter	Design Value
Cladding OD, cm	0.800
Cladding Thickness, cm	0.043
Fuel Pellet OD, cm	0.714
Fuel-Cladding Gap, cm	0.0
Duct Wall Thickness, cm	0.254
Hexcan Size (inner), cm	18.692
Fuel Volume Fraction	0.39
Flowing Coolant Volume Fraction	0.40
Pitch	1.0274
P/D	1.28
Number of Fuel Subassemblies	408
Number of Spacer Grids	15

TABLE 5.9

Thermal-Hydraulic Results for 331 Pin He-Cooled
FMSR Design and Comparison with the 271 Pin
Reference Design

Parameter	271 Pin Design	331 Pin Design
Length of Fuel Pin Roughened, cm	100	100
Inlet Coolant Temperature, °C	300	300
Mixed Mean Outlet Coolant Temp. °C	532	532
Average Heating Rate in Peak Power Channel		
W/cm ³	227.37	229.85
KW/ft	8.79	7.20
Maximum Fuel Centerline Temp. °C	833	775
Maximum Clad Inner Surface Temp. °C	571	564
Pressure Drop, psi		
Active Core	60	71
TOTAL	100	114
Core Pumping Power, MW	163	187

Several alternate designs will be considered in the future including a design with 331 pin subassemblies, design with circular duct subassemblies, design with lower fuel volume fractions, etc. Considerations will also be given to designs where the entire length of the fuel pin is roughened which could have possible advantages from the manufacturing and safety points of view. The safety aspects of mild off-normal operating conditions will also be examined.

Since only limited thermal-hydraulic analysis has been done to date, more extensive analyses for both the helium-cooled and sodium-cooled FMSR are planned.

6. SAFETY ASPECTS OF FMSR

The safety related features of the FMSR, in general, will resemble those of other fast reactor concepts with safety features of the gas-cooled version being similar to its counterpart GCFR, and safety features of the sodium cooled version being similar to its counterpart LMFBR. In each case, the principal differences will stem from differences in their respective fuels and the inclusion of a moderated region in the FMSR fuel. Although careful safety analyses have not yet been done, the important differences can still be delineated and discussed.

6.1 NEUTRON LIFETIME

Since the FMSR is in large part a fast reactor, its neutron lifetime will be short, and a relevant safety analysis must focus on its effects on reactor transients. The incorporation of a moderator zone in the FMSR will tend to lengthen the neutron lifetime thereby diminishing the violence of a prompt power excursion that might ensue from large, inadvertent reactivity additions. The value of the neutron lifetime will depend on the specific design of the reactor core, and in particular on the degree of power sharing between the hard spectrum region and the moderated region. Core designs presently under study cover a wide range of power sharing. The basis of selection of a particular core design must take into account its corresponding neutron lifetime and its effect on the severity of reactor transients. As shown in Section 3, β_{eff} for the FMSR design used in the present study is 0.0044 significantly higher than corresponding breeders. This effect will tend to mitigate the effect of inadvertent reactivity insertions.

6.2 SPATIAL TRANSIENTS

The use of two differing material regions in the FMSR raises the possibility of occurrence of decoupled transients involving only part of the core. Such a possibility always exists in a large reactor. However, the question is somewhat more significant for FMSR because of the possibility that a transient could involve neutronic characteristics of only one of the two regions. The possibility for such a transient and its implications must be explored.

6.3 FUEL SLUMPING VS. FUEL EXPANSION

Vibratory compacted fuel is proposed for use in the gas-cooled FMSR, and ANL's Mark II fuel for the sodium-cooled version. The vibratory compacted fuel will have a density well below the theoretical value. The Mark II fuel will also have a low density as soon as release of gaseous fission products begins to induce porosity. In either case, the issue must be raised and answered as to whether fuel melting could lead to slumping which might densify in the core and result in a large reactivity increase. On the other hand, the dissolved fission product gas in both kinds of fuel should cause the fuel to foam or froth on melting, inducing expansion of the reactor core rather than compaction; this could result in a substantial negative reactivity that would shut down a transient at the onset of fuel melting. Fuel frothing did occur to a substantial degree in the EBR-I meltdown.

For the sodium-bonded fuel such as used in the Mark II design, appreciable liquid sodium penetration into the interconnected porosity as burnup proceeds has been observed. In any transient that caused the fuel temperature to exceed the boiling point of sodium (880°C), fuel dispersion could occur before melting of the uranium (~1040°C).

In transients leading to fuel melting, it is presently expected that the

fuel would in fact disperse rather than slump and thereby yield beneficial negative reactivity feedback. Nevertheless, the phenomena require careful exploration, starting with experiments in TREAT and in Sandia's ACPR.

6.4 SODIUM VOID

The sodium void coefficients require further analysis. In the one calculation made so far for a particular design, the reactivity increased markedly on total sodium voiding. The spatial distribution of the sodium void coefficient and the overall void coefficient will depend strongly on the degree of power sharing between the two reactor core regions and the fuel shuffling strategy. A core design must take properly into account the resulting sodium void coefficients and their effects on transients.

6.5 DOPPLER COEFFICIENT

Although the contribution from the hard spectrum region to the negative Doppler coefficient will be small, the contribution by the moderated zone can be significant, depending on the degree of power sharing. Core design must also take into account the importance of the Doppler effect to overall negative reactivity feedback coefficient. For the one core design utilized in the present study, the Doppler coefficient is sizeable and negative ($\sim 4 \times 10^{-5} \frac{\delta k}{k} / ^\circ\text{C}$).

6.6 RECRITICALITY

The average fissile enrichment of the fuel is quite low ($\sim 5\%$ plutonium) in both versions of the reactor. It is unlikely that this fuel can be made critical at the average enrichment, without some neutron moderation. Even with the addition of moderator blocks from the moderated zone, recriticality would still be questionable.

Studies are planned to determine under what circumstances, if any, recriticality might be possible. If there are such circumstances, designs will be explored to prevent reassembly in the required geometry. In any event, present plans include provisions for the conceptual design of an in-vessel molten core retention system with active cooling. If necessary, this system would incorporate neutron poisons to prevent recriticality.

It is believed that appropriate design measures can be taken to ensure that no accident could credibly release energy with mechanical work after a first pulse. With a long neutron lifetime, the total mechanical work should be small and the effect mild.

6.7 AUXILIARY HEAT REMOVAL SYSTEM

Backup cooling for a sodium-cooled version would be supplied by auxiliary pumps in a bypass to the main coolant circuit. Such a system would be designed such that no credible rupture of the primary system could uncover the core. Backup cooling for the gas-cooled version would meet the same problems encountered in the design of the GCFR. The reliability of circulator operation and its ability to force sufficient gas through the core if a massive depressurization were to occur are specific problem areas. The implications of these questions have yet to be explored. Resolution of these questions will depend on solutions of similar questions for the GCFR.

6.8 FUEL MELT TEMPERATURE

The melting point of metal fuel is well below that of ceramic fuel. However, the thermal conductivity is high and the maximum fuel temperature will be low. The margin to fuel melting is comparable to that in an oxide fueled reactor.

6.9 FUEL-CLAD INTERACTION

Uranium and steels undergo mutual interdiffusion at higher temperatures, and eutectic formation can occur. For this reason, it has been recommended that a vanadium barrier be inserted between the uranium and the steel. Even though this barrier will be only a few thousandths of an inch thick, a metallurgical bond will be required between the steel and vanadium to eliminate interface thermal resistance. An experimental program will be required to establish the adequacy of the barrier for avoiding fuel-clad interaction over the long fuel lifetime.

6.10 FUEL BOWING

The effects of fuel bowing in altering reactivity are well known. The use of fuel assembly duct walls and spacer grids, combined with the poor structural rigidity of the fuel, should limit the effects of fuel bowing to an acceptable level. In any case, the reactivity effect of fuel bowing will be negative and thus a mitigating factor in a power transient situation.

6.11 FUEL-COOLANT INTERACTION

Clad failure during a reactivity transient could release molten fuel into the sodium of the sodium-cooled version of the reactor. The possibility of coolant boiling and consequently enhanced mechanical work in interactions between metal fuel and sodium has not yet been explored, and must therefore be the subject of experimental research. The presence of fission gas in the intergranular positions coupled to the diffusion of sodium into the pores of the fuel are expected to considerably mitigate the effect of fuel-coolant interactions.

Summary

With two important exceptions, there seems to be little difference between the safety of gas-cooled or sodium-cooled versions of FMSR and their conventional fast breeder counterparts. The same safety issues must be explored, and similar design determinations resulting from an appropriate safety analysis must be made.

The first exception relates to the behavior of the fuel under the occurrence of a severe transient. There is no a priori reason to believe that upon melting oxide and metal fuels would move spatially in the same way; their reactivity histories will therefore differ. There is, however, reason to believe that the metal fuel will readily disperse because of dissolved gas, and because of sodium vapor pressure in the sodium cooled FMSR. The contrary possibility of fuel densification must be considered, and relevant experiments should be performed to determine what really would take place.

The second exception relates to the low fissile enrichments of the fuel. This would tend to reduce the chance of recriticality should meltdown occur even though suitable designs should prevent recriticality. The tendency of metal fuel to form inseparable eutectics with steel will further reduce the effective fissile density. It could also tend to reduce the volumetric decay heat density levels because of increased neutron absorption in the fuel. Added to these effects is the tendency of fuel from the higher power density regions to be diluted by fuel from the other areas further decreasing the fissile enrichment and decreasing the chances of recriticality. When estimates of large mechanical energy release have been made in the past for LMFBR accident sequences, these have been the result of large energy development from recriticality. FMSR designs therefore offer the possibility of assured low mechanical effects of reactor accidents.

7. SUMMARY and CONCLUSIONS

An initial evaluation of the feasibility of the FMSR has been completed. This work was carried out over a four month period and as such these findings must be considered preliminary. However, the results of this first study do provide the incentives and direction for future work.

The objectives of the FMSR program are to:

Provide a reactor which can operate on a once-through fuel cycle, thereby avoiding the need for re-processing of fuel for a long time;

Greatly extend the utilization of natural uranium resources by achieving high fuel burnup;

Operate with natural or depleted uranium as the only feed material, after startup, thereby removing the need for associated re-processing plants;

Have good economics;

Employ emerging fast reactor technology.

The results of this initial feasibility study will be summarized for the three major areas studied namely, reactor physics and fuel cycle, fuels and materials, and thermal hydraulics. Due to the short time available, a single reference design was selected as the focal point of this investigation. A limited number of iterations were made in an attempt to achieve overall consistency among the three areas. Only basic aspects of the design were therefore addressed, and important questions such as startup requirements and approach to the equilibrium cycle were deferred to a later time. The major conclusions from this initial feasibility study are:

- . An FMSR design is feasible which will operate on a 6 month fuel cycle with only natural uranium for its equilibrium feed.

- . For the design selected, resulting reactivity swings are acceptable. Further design studies are needed to relax initial criticality requirements. Initial preliminary calculations indicate that fuel-bearing control rods could readily provide the excess $0.02 \Delta K$ required for startup at the beginning of the cycle.

. Acceptable power densities and profiles appear to be consistent with thermal-hydraulic and metallurgical requirements.

. For the design selected, high peak burnups (15 to 20% heavy metal) and high peak fluences of 8×10^{23} (nvt > 0.1 MeV) are necessary to sustain an equilibrium feed of natural uranium.

. Further work is needed to determine how other FMSR designs utilizing different fast-moderator configurations and different fuel shuffling strategies would relax these requirements.

. Extensive design efforts and irradiation testing are required to extend metal fuel irradiation experience beyond that available from the EBR-II Mark-II fuel experience.

. Only the advanced steel alloys proposed for the cladding may be able to meet the high fluence requirements within allowable swelling ranges. Irradiation testing will be required to confirm this.

. The design of the fuel subassembly duct may be one of the most severe problems for the FMSR. A number of possible solutions to the duct interference problem, as proposed by ANL, should be studied.

. Alternate subassembly duct designs such as circular ducts should be studied.

. An alternate fuel subassembly design proposed by BNL should also be investigated.

. Thermal-hydraulic analyses support the feasibility of the initial design.

. Since the methods used in the thermal-hydraulic analyses were extrapolations of other methods, a careful evaluation and checkout of the methods is necessary.

. Finally, this initial study bears out the validity and feasibility of the FMSR concept within the constraints imposed by the metal fuel, cladding, and duct materials. Further study and uncertainty analysis are required in order to specify an appropriate R & D program which will ensure feasibility.

7.1 REACTOR PHYSICS AND FUEL CYCLE STUDIES

The results of the reactor physics and fuel cycle studies are:

. A satisfactory design option has been evaluated. The design is essentially consistent with ANL fuel and materials recommendations. However, further design iterations will be required as more information becomes available. In particular, a significant redesign may be needed to evaluate the impact of alternate duct designs on the physics performance.

. Satisfactory power densities and profiles are attainable. Optimization studies to improve the physics performance should be made possible.

. Relatively high fuel burnup (15 to 20% of heavy metal) is required to sustain reactivity. Although EBR-II metal fuel has sustained burnups close to 15%, the high peak fluence (8×10^{23} nvt > 0.1 MeV) in the FMSR poses serious problems for the clad and ducts. Other physics designs should be explored to mitigate these effects.

. A sodium-cooled FMSR was studied, albeit at a low level effort. A design appears to be feasible and should be pursued.

. This study did not address several reactor physics areas which must be analyzed before feasibility can be determined. These are the specifications of the first core, startup, control, and shutdown requirements. The feasibility of using fuel-bearing control rods with associated problems such as the effect on local power densities needs to be investigated.

- . The fission product cross-section data base should be improved.
- . The impact of implementing the FMSR on total resource utilization needs to be studied.
- . Safety-related physics studies are needed.
- . The effect of uncertainties on the feasibility of the concept needs to be carefully studied.
- . Alternate, possibly more attractive design options need to be explored.

7.2 FUELS AND MATERIALS RESULTS

The results of the fuels and materials studies are:

- . For the helium-cooled FMSR, ANL recommends a VIPAC (vibrationally compacted powder) fuel with a vanadium barrier between the fuel and cladding.
- . A vented fuel similar to that proposed for GCFRs is recommended.
- . Expected peak burnups (15 to 20% heavy metal) are not far beyond current MARK-II fuel irradiation experience.
- . Since the proposed fuel design is different in many respects from the MARK-II fuel, caution must be exercised in extrapolating such experience.
- . A fabrication and irradiation program should be initiated as soon as possible.
- . The advanced clad steel alloy, D-57, appears to meet FMSR requirements. An inner vanadium barrier would be very advantageous, especially in sustaining off-normal operation conditions.
- . The fuel subassembly duct-wall will undergo excessive dilation if conventional duct designs are adopted. Several design options proposed by ANL are being investigated to resolve this problem. This aspect of the design will be a focal point for further study.
- . Essentially no fuel and materials evaluations were performed for the

sodium-cooled FMSR. One clear advantage of the sodium-cooled version over the helium-cooled version is the feasibility of extrapolating the sodium-bonded MARK-II fuel design to FMSR requirements.

7.3 THERMAL-HYDRAULIC RESULTS

The results of the thermal-hydraulic studies are:

- . Satisfactory thermal-hydraulic performance is feasible using state of the art gas cooling technology. Design operating limits imposed by the reactor physics and materials can be achieved.

- . The feasibility of using a higher operating pressure (2000 vs 1300 psi) needs to be assessed. The fractional pressure drop across the core (4.9%) appears to be reasonable particularly for its effect on the balance of plant.

- . The impact of uncertainties on attainable operating conditions needs to be carefully studied.

- . A thorough checkout of the BNL thermal-hydraulic methods should be made as soon as possible.

- . Hot spot effects, edge pin channel effects, grid-spacer optimization, etc., need to be carefully studied.

- . The adequacy of extrapolating the GCFR thermal-hydraulic data base to FMSR needs to be evaluated.

- . The heat transfer requirements for the moderated subassemblies has not yet been examined. While presumably not a problem because of low power generation, the design of the moderator and its characteristics must be established.

8. RECOMMENDATIONS FOR FUTURE FMSR WORK

In December, 1978, a draft proposal for an expanded FMSR program was submitted to DOE. These recommendations will repeat and expand on these suggestions. The short-term objective is to perform sufficient work to demonstrate feasibility to the point where an R&D program directed toward construction and demonstration could be specified. To that end, recommendations are made only for FY-79 and FY-80.

Remainder of FY 1979 Recommendations

The following is a summary of the most important subjects to be pursued in FY 1979:

1. Provide required FMSR information for input to the NASAP/INFCE programs on a schedule consistent with the NASAP/INFCE effort.
2. Evaluate results and implications of the present study. Develop a new set of consistent design and operating parameters closer to the optimum. Perform required uncertainty analyses in order to further define the R&D program.
3. Develop an updated R&D program for the FMSR reactor concept.
4. Provide a non-proliferation core design option which could be incorporated into the developing GCFR design (i.e. within the reactor cavity).
5. Fuels and Materials Area:
 - A. Quickly define long lead-time activities, which involve fabrication, testing, or long term irradiations. Develop a tentative irradiation program and schedule.
 - B. Select a duct engineering design which has the greatest chance for performing satisfactorily under FMSR conditions.
 - C. Provide a preliminary engineering design for the fuel pin.

Provide best estimates of physical changes which might occur over fuel lifetime; determine operating constraints and their effect on reactor physics or heat transfer calculations. Define a clad whose characteristics are consistent with predicted fuel performance.

D. Develop an engineering design for the moderator subassemblies consistent with heat transfer and reactor physics constraints.

E. Begin specification of fuels and materials required for the sodium-cooled FMSR concept.

6. Reactor Physics and Fuel Cycle Area:

Begin a second and more detailed feasibility study of the FMSR concept.

A. Define fissile needs for the first core, determine control requirements for the fuel cycle, and calculate fissile resource needs for a 30-year plant lifetime.

B. Examine uncertainties in the design and their implications. Define fallback design options for the FMSR concept in case irradiation requirements cannot be achieved.

C. Develop a limited number of alternate FMSR designs. Analyze those which offer advantages such as possible reduction of total irradiation damage, etc.

D. Continue design studies of sodium-cooled version of the FMSR.

E. Provide incidental calculations, such as hot spot effects and reactivity coefficients; examine effects of hexagonal geometry, etc.

7. Thermal-Hydraulics Area:

A. Provide a thorough checkout of BNL thermal-hydraulics method against those employed by GA.

B. Iterate with fuels and materials program, in particular, to be sure that proposed designs can accommodate uncertainties.

C. Improve methods, or adopt GA methods, to analyze problems of detail, such as hot channel effects, edge pin heat transfer, etc.

D. Reduce as far as possible the uncertainties associated with extrapolating GCFR performance data to FMSR requirements.

Fiscal Year 1980

1. Development in the fuels and materials area should include fabrication of a fuel pin designed for the helium-cooled FMSR. Short-term irradiations should be started in EBR-II. Sodium cooling of the pins should be satisfactory for this early work. Required irradiations of steels may simply be extensions of irradiations DOE already plans for favored steels to higher levels. Preparations for sodium-cooled irradiations of full length fuel pins in FFTF should begin. Other duct design options and their impact on performance should be studied experimentally, with duct studies converging on an optimum design. Calculations and uncertainty analyses should continue.

2. In the reactor physics area, an optimum design should emerge. The sodium-cooled version should be designed in greater detail. Uncertainties should be evaluated. Evaluation of the safety of the FMSR reactor should be developed. A zero-power reactor critical program should be defined to provide a check of the validity of the analyses. Design should begin on a version of the FMSR which would operate successfully as a second core in the 300 MW(e) GCFR Demonstration Plant.

3. The analysis of thermal hydraulics should incorporate information on fuel pin, fuel assembly, and duct design as these are developed. A study should be conducted of effects on power peaking factors of control element

motion and changes in power profile. Moderator subassembly cooling will be studied to support the design of these subassemblies. The effect on nonfission heating will be determined.

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APPENDIX A

Analysis of the Coupled Core Critical Experiment

Outlined in this Appendix are the results of a preliminary analysis of a coupled core reactor experiment.¹ The purpose of this analysis was to gain confidence in the ability of the 2-DB code coupled with the LIB-IV cross-section library to predict coupled core reactor measurements.

The coupled core experiment is illustrated in Figure 1; this is an (R-Z) representation of the actual reactor. The nuclide number densities in the various regions are shown in Table A1. Region 1 is the fast core, region 2 a natural uranium inner blanket, region 3 is a beryllium moderator, region 4 is a high density depleted uranium blanket and, finally, region 5 is a low density depleted uranium blanket. In the interest of reducing parasitic absorptions, the structural material used was aluminum.

In order to use the LIB-IV library, it had to be processed through the 1-DX code first. Output from the code is in the form of a macroscopic cross-section file compatible with the two-dimensional reactor codes. Both the diffusion theory code 2-DB and the transport theory code TWOTRAN were used to determine the multiplication factor (k_e). This intercomparison of methods was only carried out using all 50 groups available from the LIB-IV library. In addition, 2-DB calculations were carried out using 35 groups and 20 groups. In this way the sensitivity of k_e to the number of groups could also be estimated. Table 2 shows the lethargy widths for the 3-group structures used. The 35-group structure has 1/2 lethargy units, and in the 20-group structure, groups of lower importance in the fast core are grouped together. This grouping is clearly not optimum for the beryllium moderator region.

Values of k_e and values of peak/average power for the various calculations are shown in Table 3. From these comparisons, it is seen that the transport theory calculation overpredicts the experimental value (1.0) by approximately 2%, while the diffusion theory calculations are less than 1% low. In the case of the transport theory calculation, the inclusion of higher order scattering (P_1 scattering matrix) should lower the value of k_e . These, however, are not available from the LIB-IV library. Also shown in Table 3 are the peak/average power ratios for the diffusion theory calculations. It is seen that the values of peak/average in the fast core and natural uranium inner blanket hardly vary with group structure. However, in the high density uranium blankets, the variation could be approximately 1%.

Finally, the ^{235}U fission distribution was calculated for the case of 20 groups. This is shown in Figure A2. Also shown in this figure are the experimentally determined results. It is seen that the agreement is acceptably close.

TABLE A1
Zone Number Densities

Nuclide	Zone Number				
	Zone 1 Fast Core	Zone 2 Natural U	Zone 3 Beryllium	Zone 4 DU High Density	Zone 5 DU Low Density
²³⁵ U	6.78037E-3	1.45294E-4	-----	9.20194E-5	3.87449E-5
²³⁸ U	7.60330E-3	1.88409E-2	-----	3.98336E-2	1.89843E-2
Al	2.49430E-2	1.87374E-2	4.39817E-3	4.39817E-3	1.90386E-2
Be	-----	-----	.1032384	-----	-----

TABLE A2
Lethargy Widths

Group Number	50 Group Structure	35 Group Structure	20 Group Structure
1	.692	.692	.692
2	.5	.5	.5
3	.5	.5	.5
4	.5	.5	.5
5	.5	.5	.5
6	.5	.5	.5
7	.5	.5	.5
8	.25	.5	.5
9	.25	.5	.5
10	.25	.5	.5
11	.25	.5	.5
12	.25	.5	.5
13	.25	.5	.5
14	.25	.5	1.0
15	.25	.5	1.0
16	.25	.5	1.5
17	.25	.5	2.0
18	.25	.5	3.0
19	.25	.5	2.0
20	.25	.5	11.31
21	.25	.5	
22	.25	.5	
23	.25	.5	
24	.25	.5	
25	.25	.5	
26	.25	.5	
27	.25	.5	
28	.25	.5	
29	.25	.5	
30	.25	.5	
31	.25	.5	
32	.25	.5	
33	.25	.5	
34	.25	.5	
35	.25	11.31	
36	.25		
37	.25		
38	.5		
39	.5		
40	.5		
41	.5		
42	.5		
43	.5		
44	.5		
45	.5		
46	.5		
47	.5		
48	.5		
49	.5		
50	11.31		

TABLE A3

Calculated Results

Calculation	k_e	Peak/Average Power Zone				
		1	2	3	4	5
Transport Theory (S_4 50 groups)	1.0205	-----	-----	-----	-----	-----
Diffusion Theory						
50 groups	.9905	1.314	1.649	-----	7.71	4.002
35 groups	.9910	1.314	1.646	-----	7.706	4.011
20 groups	.9920	1.313	1.659	-----	7.013	4.029

Figure A1: R-Z Representation of Reactor.

CALCULATED AND EXPERIMENTAL
URANIUM-235 FISSION DETECTOR
RESPONSE

FIGURE A2

APPENDIX B

Material Evaluation Studies and Core Mechanic Considerations for the Fast-Mixed Spectrum Reactor

Prepared by the Argonne National Laboratory with contributions from the Material Sciences Division, the EBR-II Project and the Components Technology Division.

FUEL, CLADDING, AND MODERATOR CONSIDERATIONS

FOR THE

FAST MIXED SPECTRUM REACTOR (FMSR)

by

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FUEL, CLADDING AND MODERATOR CONSIDERATIONS FOR THE FAST MIXED SPECTRUM REACTOR (FMSR)

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I. Fuel Pin

The performance requirements of the FMSR fuel pins lie outside current experience or projected LMFBR needs for either metal or ceramic fuel pins. The following sections attempt, after a brief restatement of the performance requirements, to apply or extrapolate present knowledge to FMSR conditions and point out major considerations and uncertainties that need to be addressed. Recommendations are made to satisfy the major concerns.

A. Performance Requirements

The current BNL design for the FMSR requires a reactor coolant outlet temperature of 530°C. The corresponding peak cladding temperature is nominally 570°C. Current practice, however, calls for pin design and performance predictions to be based on the 2 σ temperature rather than the nominal temperature. This temperature has not yet been determined for the FMSR; however, based on LMFBR experience, it may be expected to be ~625°C. According to BNL representatives, the fuel pins will operate for 4800 days to a peak fuel burnup of 17-20 at. % and a peak fast fluence of $5-7 \times 10^{23}$ n/cm² (E > 0.1 MeV). Assuming the fuel is metal, the nominal peak fuel center temperature achieved is expected to be near 850°C. The nominal peak linear heat rating is expected to be ~43 kW/m in the 1.6 m high core. The desired initial fuel is natural metallic uranium, although alloying additions will also be considered. At end of life, the fuel will contain ~9 wt.% plutonium.

The design transient events for the FMSR have not yet been determined. These events, once defined, will have to be considered in evaluating fuel pin performance.

The nature of the helium-cooled reactor and the fuel pin Pressure Equalization System (PES) requires that the fuel pin materials be compatible with the PES environment as well as with each other.

B. Materials Considerations

1. Fuel Swelling and Gas Release

Fuel swelling is one of the primary design concerns because of its potential for introducing significant stress on the cladding. For long cladding lifetimes, the fuel pressure on the cladding must be kept to a minimum. The following discussion addresses the swelling phenomenon and accommodation of it. The related issues of interconnected porosity and gas release are also discussed.

Metallic uranium exhibits substantial swelling upon irradiation.⁽¹⁻¹⁰⁾ The low temperature alpha phase is orthorhombic and exhibits anisotropic swelling.⁽²⁻⁶⁾ Preferred orientation of the grain structure results in anisotropic growth upon irradiation.⁽¹¹⁻¹²⁾ Cavitation swelling, that resulting from intergranular tearing due to anisotropic swelling of neighboring grains, peaks in the temperature range of 400-500°C for high purity or dilute uranium alloys.⁽⁵⁾ Such swelling has not been observed in EBR-II driver fuel. Irradiation of uranium and uranium alloys to low burnups has shown that the temperature dependence of unrestrained fuel swelling is strongly a function of composition and preparation. The fuel swelling results primarily from accumulation of gaseous and solid fission products. The solid fission products alone contribute about 2% volume increase per atom percent burnup. The gaseous fission products generate internal porosity resulting in substantial fuel swelling, which, under some conditions, is easily restrained. Further, there is evidence that the high hydrostatic pressure in the FMSR (14 MPa) may significantly reduce the fuel swelling rate.^(5,13) The restrained fuel swelling behavior at high burnups and high temperatures is currently an area of uncertainty.

The EBR-II Mark-II driver fuel elements have been designed to accommodate the swelling of metallic fuel.⁽⁹⁻¹⁰⁾ Irradiation performance has proven that the elements can reliably survive 10 at. % burnup.⁽¹⁴⁾ Small scale testing of Mark-II fuel elements has demonstrated that a burnup of 16.4 a/o is possible without cladding breach.⁽¹⁵⁾ The extended lifetime in the Mark-II design results from allowing 33% fuel swelling before the fuel contacts the cladding. Earlier Mark-I and Mark-IA designs have substantially shorter lifetimes because only 17% fuel swelling could be accommodated before cladding strain. The key result of the 33% fuel swelling is that the

fission gas porosity becomes interconnected and the fission gas is released to the element plenum as predicted.⁽¹⁶⁾ The resultant fuel structure is weak, and subsequent swelling is restrained by the cladding and partially accommodated by the internal porosity. By having a sufficiently large plenum, the internal pressure is also reduced. These two design enhancements result in minimum cladding hoop stresses. The initial smear density for metallic fuel should therefore be 75% or less with sufficient accommodation of released fission gas.

The fuel swelling rate is only secondarily significant. It primarily determines the point in life when the fuel contacts the cladding and could stress the cladding. However, limited fission-gas release before, and substantial release subsequent to, that event greatly reduces the fuel pressure on the cladding. Since the cladding properties degrade only slightly during that initial interval, little cladding damage is sustained.

Interconnected porosity must be maintained to assure continued gas release and minimal fuel pressure on the cladding. Within the solid Mark-II fuel pin, the porosity is nucleated throughout the matrix and grains by Ostwald ripening. Agglomeration results in interconnected porosity. The process is dynamic with long, narrow channels "pinching off" and others opening by agglomeration. Irradiation tests of subdivided fuel to low burnups indicate increased gas release and reduced fuel swelling because of the increased open porosity.⁽⁷⁾

The distribution of open and closed porosity is thought to be a function of operating temperature. Recent Mark-II elements which operated at higher than normal temperature ($\sim 720^{\circ}\text{C}$ centerline compared to the normal 635°C centerline) exhibited 50 rather than 70% gas release at 8.4 at. % burnup. The higher temperature increases the bulk diffusivity of uranium and also transforms a substantial part of the fuel into the gamma phase. Uranium self-diffusion in the gamma phase is several hundred times greater than in the alpha phase at 700°C . Under these conditions the most stable configuration will be obtained more quickly, and since large spherical pores are the most stable, less connectivity results. The increase in retained fission gas may result in additional fuel pressure on the cladding.

The success of the EBR-II Mark-II driver fuel in achieving a burnup of 16.4 at.% near the goal for the FMSR, has demonstrated the viability of low fuel smear density, i.e., 75% or less, in accommodating metal fuel swelling. However, because sodium-bonded pins are not feasible in a helium-cooled reactor, FMSR pins must be either helium or contact bonded to the cladding. Large helium gaps are not feasible for heat transfer reasons, and contact bonding would require the building in of the necessary porosity into the fuel. A particulate fuel form would satisfy the conditions of helium-bonding, good initial heat transfer, and available porosity for swelling accommodation. A smear density of 75% is easily obtained by blending two particle sizes in appropriate fractions. Particulate fuel also has potential advantages in fuel preparation and element fabrication as well as fabrication costs.

Although the vibratorily-compacted particulate fuel structure is similar to the Mark-II design at fuel-cladding contact, the particulate fuel structure may not be stable at elevated temperature. The metallic fuel particles may sinter together resulting in bulk densification and shortening of the fuel column. The increase in smear density and reduced burnup capability would be unacceptable. Densification may be greatly retarded by addition of an oxide phase to the particle surfaces. Since it is likely that a surface oxide phase would form during fabrication, a specified thickness could be obtained under proper conditions. The design objective would be to balance the fuel swelling rate with the sintering densification rates.

If the thin oxide coating is employed to avoid sintering, additional concerns are raised by its presence. Effects on thermal performance must be analyzed. Differential thermal expansion and fuel swelling will generate stresses in the oxide layer and potentially cause spalling. Loss of the protective layer and redistribution of the oxide must be evaluated. Regeneration of the oxide layer due to contaminants in the coolant will depend on system getters such as traps, axial blanket, and other components in the element as well as the oxygen potential, which is expected to be low. The very limited oxygen available will partition among the various fuel pin components.

An out-of-reactor experimental program is necessary to determine the required or optimum oxide coating thickness and particle sizes to minimize densification of the fuel by beginning-of-life sintering. This concern would tend to vanish relatively early in life because fission-gas swelling would maintain the porous structure. Proof-testing of the concept would be necessary in follow-on irradiation tests.

2. Fuel-Cladding Compatibility

Fuel-cladding metallurgical interaction is a significant consideration in the design of a fuel pin that has an intended lifetime of about 13 years. Two aspects of cladding degradation must be considered: cladding wastage due to interdiffusion and fuel-cladding eutectic formation.

Interdiffusion is a function of the component composition. Early studies reported results on the compatibility of uranium with several metal transition elements and alloys with respect to reaction and solubility.^(17,18) Subsequent investigation characterized interdiffusion between uranium or uranium alloys with several potential cladding choices.⁽¹⁹⁻²¹⁾

The results have been aptly categorized into three types.⁽²⁰⁾ For those materials which form intermetallic compounds with uranium such as stainless steel, penetration into the cladding can only occur by growth through those compound layers. However, because the nickel and iron in stainless steel diffuse much more readily than uranium, penetration into the fuel is much more extensive. For those materials which exhibit extended solubility with uranium such as niobium and molybdenum, large penetrations into the cladding result. For those materials which exhibit only limited solid solubility in uranium such as vanadium, very little penetration can be expected. Measured interpenetration in EBR-II driver fuel elements was consistent with out-of-reactor rates. Total interpenetration of 76 μm with only 13 μm penetration into the Type 316 stainless steel cladding at 10.3 at. % burnup has been observed.⁽¹⁴⁾ The addition of plutonium to uranium or to uranium alloys generally lowers fuel melting temperatures and fuel-cladding eutectic temperatures and increases fuel-cladding reaction rates. For example, data

for U-10Pu-10Fz vs Type 304 stainless steel, extrapolated to the FMSR lifetime, indicate a cladding wastage of 0.3 mm, or about 80% of an initial cladding thickness of 0.38 mm.⁽²⁰⁾ For the U-10Pu-10Fz/V-20Ti system, only 6 μm of the cladding was penetrated in one year at 600°C in diffusion couple studies.⁽²⁰⁾ The rates for vanadium were dependent upon the oxygen content of the vanadium. A (U,Pu)O₂ layer was formed by diffusion of the oxygen from the vanadium to the fuel. This layer prevented penetration of the metallic elements. Inter-diffusion between vanadium and Type 304 stainless steel was found to be less than or equivalent to the penetration by uranium so that little cladding wastage can be expected.⁽²⁰⁾

Eutectic penetration is a concern because it limits the operating temperature, particularly for off-normal events. Uranium and uranium-fissium in iron and stainless steel exhibit eutectic temperatures in the range of 705°C to 725°C. The vanadium-uranium eutectic temperature, on the other hand, is 1040°C. Although the uranium/steel eutectic temperatures are low, high temperatures are necessary to obtain appreciable rates. The peak penetration rate of uranium through iron was found to be 760 $\mu\text{m}/\text{sec}$ at 1150°C.⁽²²⁾ The peak rate in Type 304 stainless steel was half as great.⁽²³⁾ Transient tests in TREAT confirmed the rates and also the maximum in penetration rate at 1150°C.⁽²⁴⁾ Resistance to attack of vanadium by uranium is reportedly "small above the eutectic temperature and negligible at temperatures above the melting point of uranium".⁽¹⁸⁾

Refractory metal barriers, e.g., niobium or vanadium, would be an effective way to achieve the required thermal performance by eliminating the possibility of the formation of relatively low-melting eutectics between uranium (and plutonium) and iron and nickel. Vanadium alloys have been, in fact, a candidate for consideration as cladding materials for metal-fueled sodium-cooled fast breeder reactors.^(25,26)

EBR-II irradiations⁽²⁵⁾ indicated there were no deleterious reactions between vanadium and Type 304 SS. More recently, in the fast reactor oxide fuel program, niobium and vanadium have been studied as buffer/getter materials between oxide fuel and Type 316 SS cladding to prevent fission-product attack of the cladding.⁽²⁷⁾ Although the fuel system being

studied is markedly different from that proposed for the FMSR, the behavior of the vanadium-stainless steel interface in these experiments is relevant to the FMSR proposal. Two fuel pins (one with vapor deposited vanadium on the fuel and the other with vapor deposited vanadium on the cladding) were irradiated in EBR-II to a burnup of 30,000 MWd/MTM. Clad temperatures varied from $\sim 450^\circ$ to $\sim 610^\circ\text{C}$. There was no evidence of vanadium-stainless steel reaction.

The available data thus appear to support the concept of using vanadium to prevent eutectic reactions between uranium and stainless steel at relatively low temperatures. However, the integrity of the vanadium barrier must be assured for the desired lifetime of the pins. Otherwise, reactions could take place between fuel and cladding at or above the fuel-cladding eutectic temperature.⁽²⁵⁾

An alternative to the barrier concept is the addition of an alloying constituent to the fuel. The reaction rates of unalloyed uranium or binary uranium-plutonium alloys with stainless-steel type cladding materials (iron-nickel based alloys) can be significantly reduced by adding specific alloying elements. Suitable additions can further improve the fuel by raising both the fuel melting temperature and the temperature at which a fuel-cladding eutectic reaction occurs. In the early development work on metal fuels conducted at ANL, the three principal alloying additions studied were niobium (discussed earlier), titanium, and zirconium. Of these, zirconium was the most beneficial. Nineteen pins containing U-15Pu-10Zr alloy fuel and clad with a number of different alloys were fabricated and irradiated in EBR-II. The performance of this fuel was similar to that of the current EBR-II driver fuel.⁽²⁸⁾ The extrapolated cladding wastage for U-15Pu-10Zr in 316 stainless steel at FMSR goal conditions appears acceptable.

The plutonium alloys that were irradiated contained at least 10 wt.% zirconium. The FMSR fuel, however, builds up to no more than 9 wt.% Pu at end of life. This lower plutonium content may reduce the net amount of zirconium that might be required to maintain good fuel-cladding compatibility, and it may be possible to take advantage of the ~ 2 -2.5 wt.% fission product zirconium that is generated in the fuel to further enhance compatibility. Compatibility studies would be necessary to determine the minimum required alloying additions.

Fission-product attack of the cladding must also be considered. Some of the volatile and gaseous fission products will be released from the fuel and be either plated out on the cladding surface or in the upper blanket, trapped in the fission-product trap in the pin, or will enter the pressure equalization system. Many of the fission products, however, are expected to be much less mobile than in mixed-oxide fuel. Evidence for this has been seen in EBR-II Mark-II driver fuel. There is evidence, however, that some cesium redistribution does occur. Significant amounts of cesium have been found in bond sodium above the fuel column and concentrated in voids in the fuel. The cesium itself, in the oxygen-free environment inside a Mark-II pin, has posed no particular problems. Although the environment in the FMSR fuel pin is not known at this time, the oxygen potential is expected to be quite low. The potential for cladding attack by cesium in the presence of the expected oxygen is unlikely but requires verification. Concerning the fission products entering the pressure equalization system, they are not expected to be significantly different, either in quantity or composition, from those in the GCFR.

In summary, the compatibility of unalloyed uranium fuel containing bred-in plutonium with an iron-based cladding appears to be unacceptable, particularly due to the potential for eutectic penetration. Alloying additions, such as zirconium, may result in acceptable compatibility but may not be neutronically acceptable. Within this constraint, the use of a vanadium barrier designed and fabricated to prevent fuel-cladding contact throughout the element lifetime is recommended. With a vanadium barrier, a nominal cladding temperature of 570°C, with an expected 2 σ temperature of 625°C, is feasible, based on the uranium-vanadium eutectic temperature of 1040°C. Neither fission-product attack of the cladding nor fission-product loading of the PES is expected to pose significant problems; experimental verification, however, is needed. Should further neutronics calculations establish the feasibility of using a minimal zirconium addition, this avenue should be pursued in parallel with the barrier concept.

3. Fuel/Blanket-Coolant Compatibility

Contaminants in the primary helium, such as hydrogen, oxygen, and water vapor, could have significant effects on the fuel and blanket materials in the reactor system if present in appreciable concentrations, i.e., >10 ppm. However, such concentrations are not expected in this reactor, nor have they been experienced in other helium-cooled reactor systems or tests. But they must be considered to cover low probability events.

The importance of hydrogen is in the possible formation of UH_3 (density $\sim 11 \text{ g/cm}^3$) and the resultant large increase in volume, $\sim 60\%$. The rate of reaction of uranium with hydrogen is greatest at temperatures near 225°C (2-3 mm/hour), declines rapidly with increases of temperature above 250°C , and becomes zero when the dissociation pressure equals the external hydrogen pressure.⁽²⁹⁾ At equilibrium, the former is equal to one torr at 212°C and one atmosphere at 432°C . Although the hydrogen concentration in the helium is expected to be very low, ~ 10 ppm, the uranium could act as a sink at temperatures below 212°C , the temperature at which the hydrogen equilibrium pressure over UH_3 corresponds to the partial pressure of hydrogen in helium containing 10 ppm hydrogen. The solubility of hydrogen at one atmosphere in alpha and gamma uranium is 2.2 ppm at 662°C and 14.7 ppm at 769°C , respectively.⁽²⁹⁾ However, since the hydrogen partial pressure would be about one torr at 10 ppm and 2000 psi system pressure, the hydride would not form and the solubility would be correspondingly low at normal operating temperatures. The potential for hydride formation would therefore be limited to low-temperature modes of operation. To preclude reaction even at low temperatures, consideration should be given to a sacrificial hydrogen getter in the system.

Under normal FMSR operating conditions, the uranium is expected to be exposed to only minute quantities of oxygen from the PES. Significant oxidation of the uranium is thus not expected. For example, conservatively assuming that during a reactor shutdown, all the void space in the fuel column were filled with 14 MPa (135 atm) helium coolant containing 10 ppm O_2 , the quantity of oxygen present would be sufficient to oxidize (to UO_2) only $\sim 0.24 \text{ mg U}$, out of $\sim 850 \text{ g U}$ in the element. Assuming that oxygen enters the fuel element during each reactor shutdown, to oxidize 0.1% of the

uranium in the element would require an average of almost one shut-down per day for the thirteen-year life of the element. Oxide-forming fission products, such as cesium, that will accumulate in the pin fission-product trap during the irradiation, the carbon in the trap, and any other oxygen getters above the fuel should further reduce the potential for fuel oxidation.

Oxygen and water vapor could be of concern to the integrity of the vanadium barrier. However, out-of-pile compatibility tests of U-10Pu-10Fs with unalloyed vanadium of various oxygen contents, indicated that the oxygen diffused from vanadium to fuel and formed a $(U,Pu)O_2$ reaction layer at the interface.⁽²⁰⁾ The free energies of formation of the uranium oxides are more negative than those of vanadium.⁽³⁰⁾ Thus, the uranium fuel, or perhaps blanket, should prove to be an effective getter for protection of the vanadium barrier.

In the event of cladding breach, fuel will be exposed to coolant containing the hydrogen, oxygen, and water vapor impurities. Because the operating temperatures are above the equilibrium temperature, it is not expected that hydriding will occur. However, since there is an essentially unlimited supply of oxygen and water vapor, relatively rapid oxidation will probably occur. Studies of irradiated uranium with about 50% swelling showed an oxidation enhancement factor of about 100 over unirradiated material.⁽³¹⁾ If the plastic fuel should extrude through a cladding breach, the coolant may become contaminated with powdery oxidation products (uranium, plutonium, and fission products) and potentially with fuel and fission products as well. The design of the reactor should incorporate features to cope with this consequence of breached fuel elements.

4. Cladding

Perhaps most critical to the FMSR concept is the identification of a satisfactory cladding alloy. The U-Fs EBR-II fuel has demonstrated the potential for metal fuels to successfully achieve the burnup desired in the FMSR. However, no current fast reactor designs will result in cladding fluences as great as those in the FMSR.

The important cladding properties, in addition to compatibility with the fuel and coolant, which are discussed elsewhere, are the swelling behavior and the mechanical properties, e.g., yield strength, ultimate strength, stress-to-rupture, ductility, and creep strength.

The FMSR fuel pins require a low-swelling cladding alloy in order to achieve the goal exposure of $5-7 \times 10^{23} \text{ n/cm}^2$. An alloy such as 20% CW Type 316 stainless steel, the current LMFBR reference cladding, is virtually eliminated from consideration as FMSR cladding because, at goal exposures, its swelling is great enough to significantly degrade the thermal-hydraulics behavior of the reactor.

Six alloys are currently under investigation in the National Cladding and Duct Materials Development Program. These are: D-9, a modified Type 316 stainless steel; D-21, D-66, and D-68, precipitation-strengthened alloys; and D-57 and HT-9, ferritic alloys. The effects of preirradiation thermo-mechanical treatments, irradiation temperature, and fluence on swelling and other properties are being determined. However, the effects of micro-chemistry, temperature history, and stress history are also significant and not easily separated. Further, direct reliance on the data base being generated for the LMFBR becomes difficult because of the differences in thermal and stress histories and operating environments between the FMSR and LMFBR claddings.

Based on present knowledge, the swelling, creep ductility, total elongation and fuel compatibility (assumed to improve with decreasing nickel content) of the six alloys, together with the LMFBR reference Type 316 cladding, have been extrapolated to FMSR conditions. Based on cladding properties measured at $2 \times 10^{23} \text{ n/cm}^2$, the extrapolated properties are given in Table 1.

Table 1. Extrapolated Cladding Properties at $5 \times 10^{23} \text{ n/cm}^2$

Alloy	$\% \Delta V/V^1$	In-Reactor Creep Ductility, $\% \Delta D/D$	Postirradiation Total Elongation, $\% \Delta L/L^2$	Wt.% Ni
316	110	4	1.5	10
D-9	77.9	3.5	1.6	15.5
D-21	15.6	0.4	0	25
D-66	0.2	0.6	0	40
D-68	7.2	1.3	0	34
D-57	0.8	2	3	0
HT-9	3.1	2	6	0.5

¹Maximum calculated swelling in the temperature range 530-575°C.

²Projected to $2 \times 10^{23} \text{ n/cm}^2$, tested at 110°C above the irradiation temperature.

The high swelling of 316 and D-9 is unacceptable. The swelling of alloy D-21 may be acceptable. The swelling of alloys D-66, D-68, D-57, and HT-9 is acceptable. All the alloys appear to have significant creep ductility to avoid excessive stress buildup during operation, although no mechanism for significant cladding stress has been identified. Postirradiation testing indicates the precipitation-strengthened alloys, D-21, D-66, and D-68, have little or no ductility. This brittleness, particularly during a transient event, could lead to cladding failure. These alloys, based on the high nickel content, are probably less compatible with uranium-based metal fuels than are the other alloys. If a vanadium barrier is used, the nickel content becomes less important. It can be concluded that, although significant areas of uncertainty exist, the ferritic alloys show sufficient promise to appear feasible for FMSR application with HT-9 being the most attractive.

The vented-pin design of the BNL FMSR results in very low cladding stresses. Normally, the major contributors to cladding stress are plenum pressure and fuel-clad mechanical interaction (FCMI). Thermal stresses, produced by restraining thermal bow due to temperature gradients across the cladding wall, relax out under irradiation and do not contribute. In a vented pin design, there is no plenum-pressure-induced stress. Therefore, the only possible source of stress in the FMSR is FCMI. Experience with EBR-II Mark-II driver fuel suggests that, at cladding temperatures below 590°C, FCMI does not exist at steady-state. Limited experience with high-temperature subassemblies (~756°C peak cladding temperature) indicates that FCMI may exist at these temperatures although the high temperature behavior may as well be due to increased cladding swelling and creep rates from carbide precipitation, and reduced cladding strength from the five-fold increase in depth of cladding attack. Because of the uncertainty as to both the existence and the magnitude of FCMI in the FMSR fuel pin, a cumulative damage fraction (CDF) calculation was performed to estimate the stresses (amount of FCMI) which could be tolerated by a 0.38 mm thick cladding.

The CDF technique uses a time-to-rupture correlation to estimate the rupture time for a materials subjected to particular stress and temperature conditions. The time at these conditions vary, the various frac-

tions are summed and failure is assumed to occur when the CDF equals 1. The rules established for the application of the technique require that unirradiated stress-rupture correlations be used to calculate damage during steady-state irradiation.

Stress-rupture correlations for HT-9⁽³²⁾ were used to estimate the cladding stress required to produce a CDF value of 1 for a 13-year reactor residence time. The cladding OD (root diameter) and ID were assumed to be 7.87 mm and 7.11 mm, respectively. At a cladding temperature of 570°C, the maximum tolerable stress is 76.5 MPa (11.1 ksi). If a steady-state CDF of 0.5 is used as the criterion (to allow a safety factor), the maximum allowable stress would be 64.1 MPa (9.3 ksi). This hoop stress is equivalent to an internal hydrostatic pressure of 6.55 MPa (950 psi). Hot hardness and limited tensile data for uranium near FMSR operating temperatures suggest the fuel is quite weak and plastic and may flow or hot press readily below this pressure.⁽³³⁾ Experimental studies, i.e., hot pressing tests of irradiated fuel, could determine the maximum FCMI stresses expected.

In order to improve the heat transfer between the cladding and helium coolant, the cladding will have to be ribbed as in the GCFR design. However, this improvement is obtained at the cost of several actual or potential disadvantages.

Ribbing the tubing results in increased pumping power at a given coolant flow, and the final design (i.e., ribbed length and rib characteristics) will, to a large extent, be based on economic considerations. The preferred design would have the ribbing over the entire fueled length of the pin. This is to eliminate the transition from ribbed to smooth tubing in a region of relatively high power generation that would result in a discontinuity in the axial temperature profile. Such a discontinuity could give rise to severe mechanical stresses resulting from temperature-induced differential irradiation swelling.

Experimental programs in support of the GCFR have demonstrated that properly ribbed unirradiated, i.e., ductile Type 316 20% CW SS cladding is at least as "strong" as smooth tubing under static pressure loading conditions. However, there is very little information as to the performance (including lifetime) of ribbed tubing under the cyclic (temperature and pressure) conditions expected to be encountered in power reactors.

The mechanical properties of the ribbed tubing, as well as the corrosion resistance in impure helium, have been shown to be strongly affected by the nature of the ribbing process. It is expected that work done in support of the GCFR will define the optimum manufacturing process.

C. Pin Design Considerations

Key considerations affecting the FMSR pin design have been discussed in the preceding sections. These include the accommodation of expected fuel swelling, a protective barrier for the cladding inner surface, and the potential effects of ribbing the outside of the cladding. Two other considerations in the pin design are the axial blanket/reflector support system and active fission-product and impurity traps within the pin.

One of the fuel-related concerns in the FMSR fuel pins is the possibility of sintering and densification of the particulate fuel very early in life. One of the factors tending to promote sintering is axial loading the fuel. To minimize such loading on the fuel column, the upper axial blanket or reflector should not be supported by the fuel column. The upper blanket/reflector should have the following characteristics:

1. The blanket/reflector support system should not be a built-in "weak-link" or stress riser in the cladding, as the dimples in the EBR-II Mark-II cladding have been found to be.
2. The blanket/reflector should be located far enough above the top of the fuel column to allow for expected axial fuel swelling. Insufficient allowance may cause an increase in fuel-cladding mechanical interaction.
3. The blanket/reflector should remain permeable to fission gases throughout the life of the pin. Clogging, either by fuel, blanket, or fission products, may lead to unacceptably high pressures in the pin, accelerated cladding deformation, and premature cladding breach.

Designs that satisfy these criteria for the upper axial blanket have been considered and appear to be feasible from functional and fabrication considerations.

The lower axial blanket presents a somewhat different situation. The blanket column must support the weight of the fuel column yet must not itself be compressed or not be allowed to expand due to swelling. A particulate lower blanket might be feasible: the as-fabricated porosity would accommodate most fuel swelling, and the blanket column might be cool enough, and hence strong enough, to support the fuel column. However, the fabrication would require two similar lines for producing natural uranium for the fuel and depleted uranium for the blanket, thereby introducing an inventory problem. An alternative would be a solid depleted uranium slug separated from the fuel region by an independently supported pedestal to support the fuel column. The design of such a system appears to be feasible.

The PES, as proposed for the GCFR includes carbonaceous traps in the individual fuel elements and for each subassembly, i.e., the elements are manifolded to a common header which in turn is connected to the permanent part of the structure. These traps prevent volatile fission products, e.g., cesium from reaching the PES and delay the entry of gaseous fission products to the PES. The fission-product trapping systems and technology developed for the GCFR would be applicable to the FMSR.

The previous discussion on the potential oxidation of the pin components indicated that the expected low oxygen levels in the system virtually preclude the concern over fuel oxidation. Should the oxygen levels be higher than those now anticipated (~ 10 ppm), the carbonaceous fission-product trap, the cesium trapped on it, and the uranium upper blanket would act as effective getters to protect the vanadium barrier. Replenishment of the oxide layer on the fuel particles would be welcomed, as this might assure open passages for fission-gas release at high burnup. In summary, intentional oxygen getters in the pin do not appear necessary at this time.

Hydrogen, however, presents a potential problem (low-temperature hydride formation) that may require an intentional trap in the upper portion of the pin. Materials for such traps have been used effectively in LWR fuel rods.⁽³⁴⁾ Placement of such a trap in the pin just above the uranium upper blanket should present no problem.

II. Moderator

A. Background

The currently assumed moderator material in the Fast Mixed Spectrum Reactor (FMSR) concept is beryllium metal spheres in two size distributions, packed to 80% smeared density.⁽³⁵⁾

Based on the available literature, three areas that are considered important are addressed here. These are (1) the gas release behavior, (2) the dimensional stability, and (3) the coolant-moderator compatibility. Emphasis has been placed on evaluating the effects of irradiation, since such effects are crucial in affecting all the above items.

Before each of the specific items are discussed, it is useful to summarize the overall effects of irradiation on metals. The first of the two main effects of irradiation on metals is the displacement damage caused by energetic neutron bombardment. This damage is related to the continuous production of Frenkel defects (vacancy and interstitial pairs) and the subsequent defect clustering into dislocation loops. The behavior of metal can be significantly modified by the presence of these defects, with the radiation enhanced creep and the change in mechanical properties being the most pronounced phenomena during and after irradiation respectively. Displacement damage can be annealed out, however, when the annealing conditions (temperature and time at temperature) are appropriate.

The second main effect of irradiation on metals is the production of inert gas atoms from nuclear transmutations. Because of the low solubility in and the low chemical reactivity with the metal matrix, these gas atoms tend to precipitate out and form gas bubbles. The bubbles can migrate under the influence of the temperature and stress gradients to dislocations and grain boundaries; they can also coalesce to form bigger bubbles. The gas bubble formation is the main contributor to the swelling of certain classes of metals which possess a considerable reaction cross-section for the nuclear production of inert gases. This swelling mechanism should be distinguished from that due to void formation which generally does not require the presence of inert gas atoms. Conventionally, the two swelling mechanisms (bubble and void) have been treated as separate phenomenon, but a clear distinction between the two probably cannot be ascertained.

For beryllium metal, the predominant swelling mechanism is that of the formation of helium gas bubbles. Since the swelling is related to the number of gas atoms retained in metal, useful information can be deduced from the gas release behavior and the total amount of gas atom production. In the following, the available experimental data are reviewed with regard to the gas release behavior and extrapolations are made to the expected FMSR operating conditions. The inherent danger in extrapolation must be recognized, and the results should be interpreted with caution.

B. Gas Release Behavior

Table 2 gives the helium gas release data measured from three capsule specimens irradiated at different temperatures (600-750°C) and to different exposures ($0.75-1.08 \times 10^{21}$ n/cm², E > 1 MeV).⁽³⁶⁾ The gas release is seen to increase by an order of magnitude when the irradiation temperature changes from 600°C to 750°C. Comparatively, the exposure is seen to affect the gas release less than the irradiation temperature.

Since there was no significant temperature gradient in the capsule experiments, bubble movement from biased migration is not likely. The contribution to the total gas release from the recoil and knockout processes was estimated to be less than 1%, with the exception of one specimen which had ~6.5%. An estimate of the diffusion coefficient, D, of the helium gas atoms in beryllium has been obtained from Fick's law

$$D = D_0 \exp\left(-\frac{Q}{RT}\right) \quad (1)$$

where D_0 is 7.1×10^{19} cm²/sec, Q is the activation energy equal to 115,000 cal/mole, R is the universal gas constant, 1.987 cal/mole°K, and T is the temperature in °K. At 550°C and 600°C, the values of D are 2.04×10^{-11} and 1.15×10^{-9} cm²/sec respectively. Notice that the diffusion coefficient in Eq. (1) was obtained under the assumption that the gas release is due to gas atom diffusion. No consideration has been given to gas bubble behavior. Such a diffusion coefficient is consistent with the Booth diffusion model, but should not be applied as such in bubble mechanics calculations. In addition, the data base for Eq. (1) is too thin to substantiate its validity over a wide range of conditions.

Table 2. Helium Gas Release from Irradiated Beryllium⁽³⁶⁾

Capsule No.	Exposure n/cm ² > 1 MeV	T _{irrad.} (°C)	Volume Be, cm ³	Total He Produced, cm ³	Annulus He cm ³ STP	% of Total He in Annulus
6	1.08 x 10 ²¹	600	14.40	34.2	0.093	0.27
7	0.75 x 10 ²¹	750	14.38	23.9	3.213	13.43
8	0.88 x 10 ²¹	600	14.34	27.7	0.0097	0.035

C. Dimensional Stability

Table 3 gives the data on volumetric swelling of beryllium irradiated to different exposures. Although the volume changes are relatively small, it should be noted that the exposures are well below those expected in the FMSR.

A correlation for the low temperature swelling behavior of beryllium is given by⁽³⁷⁾

$$\frac{\Delta L}{L} (\%) = 0.1829 (\phi t)^{1.035} \quad (2)$$

where $\Delta L/L$ is the percent of length increase and the fluence, ϕt , is in units of 10^{22} n/cm². At the expected end-of-life fluence of 5 to 7 x 10²³ n/cm² (E > 0.1 MeV) in the FMSR, Eq. (2) predicts 10.5% and 14.9% in $\Delta L/L$ respectively. The data base for this correlation contains 66 data points with values of ϕt between 1.3 x 10²² to 3.52 x 10²² n/cm² (E > 1 MeV). This is still an order of magnitude lower than expected fluence levels in the FMSR. The irradiation temperature of ~100°C in these experiments is also much lower than that expected in the FMSR.

An area of some interest from the data on the low temperature swelling behavior is that the swelling appeared to be macroscopically isotropic. This is confirmed from the measurements of the changes in both the length and the diameter of the specimens. In contrast to the pronounced anisotropic behavior

Table 3. Volumetric Swelling of Irradiated Beryllium⁽³⁶⁾

Exposure (n/cm ² > 1 MeV)	T _{irrad.} (°C)	Initial Density (g/cm ³)	Final Density (g/cm ³)	ΔV/V (%)
0.75 x 10 ²¹	750	1.854	1.834	1
1.08 x 10 ²¹	600	1.854	1.826	1.5

in beryllium single crystals, the lack of anisotropy in the polycrystalline beryllium samples during irradiation is probably due to the averaging out of the random orientation of basal poles in the single crystal. Such a possibility can only exist when there is no texture development and the texture is known to depend strongly on the fabrication process.

D. Coolant-Moderator Compatibility

In the event of canning breach, the beryllium moderator will be exposed to coolant, which is assumed to be helium in the FMSR. The likelihood of continuous high temperature oxidation must therefore be considered. The oxidation of irradiated beryllium in CO₂ has been studied with reaction temperatures ranging from 600 to 1000°C.⁽³⁸⁾ Two specimen types were used with each fabricated from a different fabrication process. Table 4 gives the characteristics of the as-irradiated, pre-oxidation beryllium samples. Notice that the Brush beryllium actually has a negative volumetric swelling which is not seen in any of the other reported experiments. This discrepancy is difficult to explain unless there is a temperature dependence which favors the thermal densification somewhere between 280°C and 480°C.

Oxidation experiments were performed on the irradiated beryllium samples held at constant temperatures for a prolonged period of time, generally on the order of 2000 hours. Sample weight gains were measured from time-to-time such that a plot of the weight gain as a function of time can be constructed. Typically, the curve increases sharply during the initial stage of oxidation, levels somewhat and takes off again if the condition for "break-away" oxidation is fulfilled. For Brush beryllium, breakaway occurred at

850°C and above and the percentage swelling at this temperature under helium environment (100 mm pressure) after 350 hrs had reached 24%. The percentage swelling during oxidation in CO₂ at 850°C reached a similar level, but only much later at 1,200 hrs. At 700°C, the percentage swelling during oxidation after 1,300 hrs at temperature is 2-3%.

The weight gain increases as the oxidation temperature increases. For Pechiney samples, the weight gains of the unirradiated samples are consistently higher than the irradiated samples, with the exception at 700°C. For Brush samples, such a trend is true only at 600°C and at longer time. The trend is reversed at higher oxidation temperatures and the irradiated samples can have 25 times larger weight gain than the unirradiated samples at 1000°C. Generally, the Brush samples exhibit greater extent of oxidation than the Pechiney samples. This indicates that the fabrication process of the beryllium metal can be important in determining the oxidation behavior. The Brush samples were fabricated from thermally reduced powder whereas the Pechiney samples were made from electrolytic flake.

In order to assess the role of swelling on oxidation, the irradiated Brush samples were annealed to bring out various amounts of pre-swelling. Two general trends in the oxidation behavior of the pre-swollen samples are of particular interest. The first one is related to the high temperature breakaway phenomenon and that a sudden increase in the magnitude of oxidation (weight gain) coincides with the massive increase in swelling. This shows that the two phenomena are closely associated and one may contribute to the other and vice versa. The second trend observed is that below the temperature for breakaway, the weight gain increases with the amount of pre-swelling. For example, the 0% pre-swelling sample at 600°C after 1,000 hrs gained 0.05 mg/cm²; whereas the sample pre-swollen to the maximum swelling of 33.6% gained 4 mg/cm², an increase of almost two orders of magnitude. Since the FMSR coolant temperature is near 600°C, a canning breach near the end of life is likely to result in substantial oxidation. Furthermore, the presence of water vapor is known to decrease the breakaway oxidation temperature of beryllium.

Table 4. Characteristics of the As-Irradiated, Pre-oxidation Beryllium Samples

Manufacturer	Exposure $n/cm^2 > 1 \text{ MeV}$	Initial Density (g/cm^3)	Final Density (g/cm^3)	$\Delta V/V$ (%)
Brush	2.75×10^{21}	1.846	1.848	-0.1
Pechiney	1.23×10^{20}	1.846	1.844	0.11

E. Summary and Conclusions

Based on the available literature, the experimental data on the irradiation behavior of beryllium has been surveyed with regard to the effects of irradiation. However, the experiments performed to date do not satisfy the fluence requirements expected in the FMSR. If simple extrapolations from the existing data are applicable, then the use of beryllium metal as the moderator material in FMSR may be acceptable. More detailed assessments in each of the three areas discussed is warranted. The assessments should include experimental analyses as well as theoretical analyses using a bubble mechanics computer program such as GRASS-SST,⁽³⁹⁾ and a general purpose finite element code for structural and heat transfer analysis such as ADINA⁽⁴⁰⁾ and ADINAT.⁽⁴¹⁾

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CORE MECHANICS CONSIDERATIONS
FOR A
FAST MIXED SPECTRUM REACTOR (FMSR)

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I. INTRODUCTION

A preliminary study of the core mechanics of a helium coolant Fast Mixed Spectrum Reactor (FMSR) design was conducted addressing core restraint and duct dilation phenomena and using the experimental advanced alloy D57 as the duct material. The features chosen for this preliminary design study were based on data specified by Brookhaven National Laboratory and on past GCFR designs. The design was not specifically chosen to alleviate the duct dilation problems of a high pressure long life, steep power gradient reactor.

The study used irradiation properties of the D57 alloy based on extensive extrapolation of minimal experimental data. For this reason the validity of D57 as an engineering material at the fluences anticipated in this design remains questionable. While the preliminary correlations for D57 shows significantly less irradiation swelling than AISI-316(20% CW), the irradiation creep is of the same magnitude and this is the primary cause of difficulties in using this alloy at very high fluence. Sensitivity studies to changes in the creep and swelling correlations have not been conducted and irradiation embrittlement has not been addressed.

Based on this preliminary study of a single conventional core design, and on our present accepted concepts of satisfactory performance, it was concluded that this core mechanical design was not viable for an FMSR. This observation is not unexpected because of the large differential pressure and fluences inherent in the design. These factors allow some high pressure and high fluence ducts to dilate beyond the envelope of the load pads on neighboring assemblies because of smaller dilation in adjacent low pressure, low fluence assemblies. This would cause severe problems in withdrawal and insertion of assemblies during fuel transfer

operations. No difficulties in core restraint of assembly bow were identified for the conditions studied.

In the course of this preliminary study, the various phenomena pertinent to the core mechanics of the FMSR were identified and quantified, and a computer model for the NUBOW-2D (Inelastic) code was developed and run. This report details the major features of the particular design which was studied and describes these results.

Based on previous general design studies and on the data derived herein, several alternate core designs have been identified that could lead to a satisfactory concept. It is recommended that these alternatives be studied in detail in the very near future.

II. RELEVANT CORE MECHANICS PHENOMENA

A brief scenerio of the major core restraint and duct dilation phenomena might go as follows: As the reactor comes to power duct wall temperature increases cause a uniform dilation and differential coolant pressures cause a "ballooning" dilation. Radial temperature gradients cause assembly bowing and resulting interactions between assemblies at the load pads. Inlet plenum pressure causes deformation of the support plate and splaying of the assemblies. As the core ages, irradiation swelling causes uniform dilation of the hexagonal duct and irradiation creep causes ballooning dilation. Radial temperature and fluence gradients cause differential swelling inducing assembly bow while irradiation creep relaxes the assembly bending stresses. Eventually contact may occur at dilation points and the resulting forces inhibit further ballooning dilation growth. As the reactor is brought down to refueling conditions, the thermal bowing, thermal dilation, pressure dilation and support plate deformation are removed changing the assembly interaction state. In this condition the assembly interaction forces and appropriate clearances must be such as to allow fuel handling operations. This section quantifies some of these phenomena in the context of the preliminary FMSR core mechanics study.

A. Thermal Expansion

The duct wall temperature is assumed to rise linearly through the core region causing a uniform (hexagonal) dilation of the duct (Fig. 1) peaking at the bottom (downstream) end of the core and remaining constant below that. The grid plate also expands, and therefore the pitch increases, by the temperature of the inlet plenum above the manufacturing temperature. Since we are concerned primarily with relative growth it is convenient to assume the nominal dimensions are given at inlet temperature, 312°C, and measure changes from that

value. Thus for this model the maximum assembly temperature rise is

$$\Delta D(\max) = \alpha D \Delta T(\max) = 1.2 \times 10^{-5} \times 0.192 \times 230 = 0.548 \text{ mm}$$

$$\Delta D(\text{midcore}, \max) = \frac{1}{2} \Delta D(\max) = 0.274 \text{ mm.}$$

B. Elastic Pressure Dilation

Coolant pressure in the duct causes a "ballooning" dilation of the hexagonal duct with the hexagon midflat moving out and the hexagon corners moving in (Fig. 1). This effect is greatest at the top (inlet) of the duct. The dilation is linear with pressure, for small deformations, and can be expressed using a pressure compliance factor, G_{1H} , for a single flat of a hexagon.

$$G_{1H} = \frac{.0284}{9E} \frac{(D-t)^4}{t^3} \text{ m/Pa} \quad (\text{Ref. 1, p. 392})$$

$$\Delta D = 2G_1 \Delta p$$

$$\Delta p(\max) = 8.23 \times 10^5 \text{ Pa}$$

$$\Delta p(\text{midcore}, \max) = 3.80 \times 10^5 \text{ Pa}$$

$$\Delta D(\max) = 2.395 \text{ mm}$$

$$\Delta D(\text{midcore}, \max) = 1.106 \text{ mm.}$$

C. D57 Creep and Swelling Correlations

The irradiation-induced creep and swelling were calculated from available correlations (Ref. 2). Over the range 375-525°C, the creep predictions are almost independent of temperature. Representative values are shown

in Figure 2. These creep strains are on the order of the minimum predictions for AISI 316 (20% CW). The swelling correlation is exemplified by Figure 3 with a peak swelling prediction of just over 3% volumetric at 425°C and 7×10^{27} n/m² fast fluence. These are of course greatly diminished from AISI 316 (20% CW) predictions. However it must be emphasized that these curves represent very large extrapolations beyond experimental data and the uncertainties are correspondingly unsettling.

D. Swelling Dilation

Irradiation swelling causes uniform duct dilation of the same form as thermal expansion (Fig. 1). The linear dimensions change is 1/3 the volumetric rate so that:

$$\Delta D = 1/3 D \frac{\Delta V}{V} = 0.64 \text{ mm/\% swelling.}$$

E. Creep Dilation

Provided that the shape of the hexagonal duct has not changed sufficiently to change the stresses in the duct walls, a correspondence principle can be used for the creep dilation of the duct based on the elastic compliance of a hexagon. The creep dilation shape is then the same as the pressure "ballooning" dilation (Fig. 1). The correspondence principle is expressed as:

$$\Delta D = 2G_{III} \Delta P \Delta J E.$$

The estimated irradiation creep modulus at 420°C (Fig. 2) is:

$$J = \bar{B} \phi t \quad \text{Pa}^{-1}$$

$$\bar{B} = 4.055 \times 10^{-38} \text{ Pa}^{-1} (\text{n/m}^2)^{-1}$$

The peak midcore flux for this FMSR design is approximately:

$$\phi = 2.07 \times 10^{27} \text{ n/m}^2/\text{cycle} \quad 1 \text{ cycle} = 800 \text{ days}$$

so that

$$\Delta D(\text{midcore, max}) = 7.91 \text{ mm/cycle}$$

F. Rounding Limits on Dilation

The stress state in the duct wall changes from dominant bending stresses to dominant loop stresses as the bending stresses at the corners of the hexagon relax and the duct approaches round. The loop stresses are substantially lower. In ducts that are allowed to balloon to near round a correction must be made for this large deformation effect. The dilation at which the hexagon becomes round, assuming no increase in the perimeter is (Fig. 4):

$$\Delta D = \left(\frac{2\sqrt{3}}{\pi} - 1 \right) D = 19.71 \text{ mm.}$$

For a round duct the elastic pressure compliance is:

$$G_{1C} = \frac{3 (D-t)^2}{\pi^2 E t} = 2.5194 \times 10^{-11} \text{ m/Pa}$$

The ratio of hexagonal compliance to circular compliance is

$$G_{1H} / G_{1C} = 57.76$$

G. Force Compliance

When the hexagonal duct contacts its neighbor, a force arises between

the two ducts. This force limits dilation. Assuming that the area of contact is small and located at the midflat, this dilation effect can also be represented using a compliance factor (Ref. 1, p. 393):

$$G_{2H} = \frac{-0.791}{3E} \frac{(D-t)^2}{t^3} = -3.3874 \times 10^{-7} \text{ m/N} .$$

The elastic portion of the force compliance is

$$\frac{\Delta D}{2} = G_{2H} F$$

and the creep contribution is

$$\frac{\Delta D}{2} = G_{2H} F \Delta J E .$$

There is also a duct rounding effect on the force compliance but this is much smaller in magnitude:

$$G_{2C} = 1/3 G_{2H} .$$

H. No Growth Condition

In order that the duct not dilate, within a first order effect:

$$\Delta D = 2 G_1 \Delta p + 2 G_2 F_o = 0 \quad F_o = \frac{-G_1}{G_2} p .$$

For the hexagon:

$$- \frac{G_{1H}}{G_{2H}} = 4.296 \times 10^{-3} \text{ m}^2$$

and for the circle:

$$- \frac{G_{1C}}{G_{2C}} = 2.231 \times 10^{-4} \text{ m}^2 .$$

For the maximum midcore pressure of $3.8 \times 10^5 \text{ Pa}$:

$$F_o = 1632 \text{ N for the hexagon and}$$

$$F_o = 84 \text{ N for the circle.}$$

I. Thermal Bow

Within the assumptions of a linear rise in the radial temperature difference through the core and a constant temperature difference below the core, the bottom end deflection of an assembly is

$$U = \frac{767 \alpha DT}{D} \left(\frac{1}{6} \ell_1^2 - \frac{1}{2} \ell_1 \ell + \frac{1}{2} \ell^2 \right)$$

for this design $U = 5.64 \times 10^{-5} \text{ DT m} .$

The maximum DT for this design is in row 3

$$DT(\text{max}) = -82^\circ\text{C}$$

$$U(\text{max}) = 4.63 \text{ mm.}$$

J. Swelling Bow

Swelling bow is caused by differential swelling on opposite walls of the duct. Because the D57 swelling correlation is essentially temperature independent at the duct wall temperatures considered, the differential swelling

is caused primarily by flux gradients in the reactor. For this FMSR design, the bottom end deflections can be estimated by

$$U = \frac{\Delta\phi}{\phi} S \left(\frac{2\ell_1^2}{3\pi D} \right) \left(\frac{\ell}{\ell_1} - \frac{2}{\pi} \right)$$

$$U(\max) = \left(\frac{\Delta\phi}{\phi} S \right)_{\max} .477 \text{ m.}$$

K. Creep Bow

The magnitude of creep bow depends on the bending stresses in the core region which in turn depend, primarily, on the net interaction forces at the bottom of the assembly. These forces depend on the overall structural interaction of the core and cannot be readily determined without a computer code such as NUBOW. For this design, however, the thermal bows are small, swelling gradients are small, and the gap at the TLP is large so that creep bow is expected to be moderate even for the long exposures of the FMSR.

L. Support Plate Deformation

Pressure in the inlet plenum, in conjunction with the weight of the assemblies, causes the support plate of the FMSR to bulge down. This in turn causes the assemblies to splay out due to the rotation of their cantilevered base. No calculation of this effect was done for the FMSR but previous results for the GCFR (Ref. 3) indicate a maximum rotation of 5.347×10^{-4} rad. This would correspond to a maximum top end radial deflection of

$$U(\max) = 1.66 \text{ mm.}$$

III. NUBOW-2D (INELASTIC) CALCULATIONS

A. Model Description

The model described here is based on a description of the FMSR obtained from BNL in October 1978 and modified to fit the limitations of the code. Certainly the design concept has been modified since that date but probably not in a way that would significantly change the major observations made here. However the modifications made for the code are mostly in the conservative direction.

The duct dimensions are shown in Fig. 5. The below-core-load-pad (BCLP) plane location was chosen somewhat arbitrarily and the across-pad dimension set to cause a small mean interference at temperature. The same geometry is used for all ducts. A 15 row model was used with the refueling zone assignments shown in Fig. 6. The usual R-Z model was found not suitable and an X-Z ("spoke") approach was used instead. The assembly pitch is 200 mm.

The core outlet duct wall mean temperatures, temperature gradients, and resulting top-end deflections, are listed in table one. These temperatures were derived from the BNL-calculated power densities shown in Fig. 7. By assuming the temperature rise at each point in the given zone to be proportional to the power density and that the mean zone outlet temperature was approximately 528°C. The temperatures were assumed to increase linearly along the core. The extreme temperature gradients in rows 11 and 13 are an artifact of these assumptions. However the row 3 gradient points out the need for careful fuel shuffling to minimize potential problems.

The reactor is orificed to give the mean outlet temperature. With gas cooling and the long core this results in some very high pressure differentials. As shown in Fig. 8, the maximum values occur in rows 7 to 9 which also

TABLE ONE
FMSR ROW OUTLET TEMPERATURES

Duct Row Number	Mean Outlet Temperature °C	Temp. Diff. (Inboard Wall -Outboard Wall) °C	Unrestrained Duct End Deflection (Thermal), mm*
1	479	0	0
2	492	-25	-1.4
3	550	-82	-4.6
4	509	-35	-2.0
5	526	32	1.8
6	530	-30	-1.7
7	531	-03	-0.2
8	516	26	1.5
9	543	-16	-0.9
10	312	0	0
11	545	112	6.3
12	312	0	0
13	534	141	7.9
14	312	0	0
15	312	0	0

* Positive value means a radially-outward movement.

happen to be the regions of maximum fast flux, as shown in Figs. 9 and 10. This congruence turns out to be the crucial difficulty with the design. An ad-hoc recognition of the stiffness variation inherent in large deformations of hexagonal ducts (Section II.F) was programmed into NUBOW-2D (Inelastic). A simple "double-knee" curve was assumed for the variation in stiffness between the hexagon and the circle as shown in Fig. 11.

The refueling pattern is to move the fuel progressively to higher-numbered zones with virgin fuel being placed in Zone 1 and spent fuel discharged from Zone 6. The actual pattern used was as follows:

<u>ZONE</u>	<u>ROW</u>		<u>ROW</u>	<u>ZONE</u>
1	13	moved to	11	2
2	11	" "	1	3
2	11	" "	2	3
2	11	" "	3	3
2	11	" "	6	3
3	3	" "	4	4
3	6	" "	5	4
4	5	" "	9	5
5	9	" "	7	6
5	9	" "	8	6

(Rows 10, 12, 13, 14, and 15 were left in place)

The refueling time interval was 800 days (EFPD). This is probably harsher than the actual partial batch scheme of moving 1/5 of each zone each 160 EFP days. The NUBOW-2D (Inelastic) code was used to simulate this model for the first 4800 EFP days, i.e., 6 refueling cycles.

B. NUBOW Results

The NUBOW code gives extensive output describing the force, displacement and deformation state of the core model at 80 day increments through the 4800 day simulation. In addition, a brief force summary is available at 10 day increments. In this section we summarize some of those results which are pertinent to our conclusions.

Table Two gives equilibrium deflections of the bottom end and forces at the midcore dilation point and BCLP for each assembly in the virgin core at full power. The forces are interactions between the numbered assembly and the radially inward neighbor. Table Three gives the same quantities at the end of the first refueling cycle, 800 days, and Table Four gives these quantities at the end of 6 fuel cycles, 4800 days. These displacements and forces are judged to be acceptable within our present understanding of good design practice, although there is little direct evidence of the magnitude of contact forces in operating fast reactors. Table Five lists the free bow bottom end displacements of each assembly at the end of each fuel cycle. This is a measure of duct creep and swelling bow and is relevant to fuel handling analysis. The maximum bottom end bow of 14.8 mm is not judged to be a major difficulty with this design. Alternate fuel management schemes including rotation of assemblies at transfer times could be utilized to further reduce bottom end bow if necessary. This would probably be required for the row 10 beryllium assemblies at about 4800 days since these are not transferred and the bow would be expected to continue to grow beyond acceptable limits.

TABLE TWO
EQUILIBRIUM DEFLECTIONS AND FORCES AT ZERO DAYS

Row	Bottom End Deflection (mm)	Assembly Inward Interaction Force (N)	
		Midcore	BCLP
1	0.0	-	-
2	-1.3	0	11
3	-3.2	0	10
4	-2.0	0	0
5	1.1	0	0
6	-0.9	0	6
7	-0.2	0	0
8	1.1	0	0
9	-0.5	0	3
10	0.0	0	0
11	4.9	0	0
12	1.4	0	13
13	5.8	0	0
14	1.7	0	19
15	0.5	0	4

TABLE THREE
EQUILIBRIUM DEFLECTIONS AND FORCES AT 800 DAYS

Row	Bottom End Deflection (mm)	Assembly Inward Interaction Force (N)	
		Midcore	BCLP
1	0.0	-	-
2	-1.6	0	806
3	-3.1	0	797
4	-3.2	559	418
5	2.6	1183	0
6	2.9	1151	0
7	5.9	1105	0
8	14.8	1035	0
9	15.4	916	0
10	13.7	0	512
11	15.2	0	405
12	11.6	0	328
13	14.1	0	224
14	9.9	0	108
15	8.7	0	79

TABLE FOUR
EQUILIBRIUM DEFLECTIONS AND FORCES AT 4800 DAYS

Row	Bottom End Deflection (mm)	Assembly Inward Interaction Force (N)	
		Midcore	BCLP
1	0.0	-	-
2	-1.3	0.0	895
3	-2.6	0.0	892
4	-2.4	828	351
5	4.5	1360	0
6	3.8	1053	177
7	9.1	1323	0
8	20.9	1325	0
9	20.3	1241	0
10	21.4	0	680
11	20.1	0	622
12	16.6	0	501
13	19.0	0	355
14	14.8	0	256
15	13.6	0	123

TABLE FIVE
FREEBOW BOTTOM END DISPLACEMENTS AT END OF FUEL CYCLE (mm)

Row	Cycle One	Cycle Two	Cycle Three	Cycle Four	Cycle Five	Cycle Six
1	0.0	1.2	1.4	1.4	1.4	1.4
2	-1.7	-0.5	-0.4	-0.4	-0.4	-0.4
3	-1.3	0.4	0.7	0.8	0.8	0.8
4	-3.4	-3.6	-1.9	-1.7	-1.7	-1.7
5	-1.7	-1.1	-1.0	0.2	0.6	0.5
6	1.1	3.0	4.7	5.0	4.9	4.9
7	0.9	9.7	7.6	7.4	7.8	9.4
8	4.7	12.9	11.6	11.3	11.7	13.3
9	8.0	6.1	5.7	6.1	7.8	8.5
10	1.7	4.6	7.6	10.3	12.7	14.8
11	0.2	0.3	0.3	0.3	0.3	0.3
12	0.0	0.1	0.1	0.1	0.2	0.2
13	0.0	0.0	0.0	0.0	0.0	0.0
14	0.0	0.0	0.0	0.0	0.0	0.0
15	0.0	0.0	0.0	0.0	0.0	0.0

Figure 12 depicts four representative duct diameters at midcore for the first three fuel cycles; these diameters are at approximately refueling conditions: zero pressure and room temperature. Figure 13 depicts two representative interaction forces at the midcore dilation point for the first three fuel cycles. Later cycles follow approximately the same pattern as the third cycle with the largest force, 1374 N, reached between ducts 4 and 5 at 3920 days, 720 days of the fifth cycle.

The forces rise rapidly after contact to a level appropriate for restraining further dilation of the duct. The duct diameters stop growing at this force level and in some cases recede when the interaction force becomes too large. The forces at power are not disconcerting, however. It is the equilibrium duct diameters of about 202-205 mm that are of concern. Table Six gives the duct midcore refueling diameters at the end of the third cycle. If the assemblies were straight at refueling (they are not because of creep and swelling bow and bending caused by interactions at the midcore and BCLP), the available space for removing an assembly would be 201 mm at the BCLP. Removal would then require "drawing" the dilated core of 204 mm through this 201 mm "die." Spreading of adjacent assemblies might be possible and the actual equilibrium gap might be somewhat larger than 201 mm, but the duct is probably quite brittle at this high exposure and our judgement is that this is unacceptable.

The problem is essentially that the reactor contains adjacent assemblies with different exposures and different pressures and the design allows about 8 mm of dilation growth per duct. The differing conditions cause differential dilation with the result that some ducts dilate well beyond the pitch dimension while others are substantially smaller. The raised load pad below the core,

TABLE SIX

DUCT MIDCORE DIAMETERS AT THE END OF CYCLE THREE (2400 DAYS)

Duct	Midcore Diameter
1	.1975
2	.1978
3	.1979
4	.2016
5	.2008
6	.1961
7	.2041
8	.2041
9	.2034
10	.1921
11	.1920
12	.1920
13	.1920
14	.1920
15	.1920

BCLP, complicates this by constricting the area available for removing the dilated core region. A more detailed analysis using a three-dimensional code, NUBOW-3D, and a more prototypical refueling pattern could be made, but it is unlikely that it would give significantly different results for this design.

IV. CONCLUSIONS AND FUTURE OPTIONS

A. Conclusions

Three limitations in this preliminary core mechanics study of the FMSR must be recognized in any conclusions. They are:

1. The material correlations for the advanced D57 alloy represent extensive extrapolation of correlations based on minimal experimental evidence at low fluences. No direct consideration of irradiation embrittlement or uncertainties in creep and swelling were made.
2. A two-dimensional "spoke" model was used which represents only a single radial row of assemblies. No consideration of three-dimensional effects such as circumferential bridging, added stiffness of outer rows due to more assemblies, and different resident times of adjacent ducts in the same row was made. In addition, the duct dilation contact model was originally conceived with only light contact in mind. While a correction was made for rounding effects, the model is still quite crude. In particular, contact was considered to occur over a small region at the midcore while in reality, this design should contact over an enlarging region.
3. The duct diameters, thickness, and load pad dimensions were chosen to be representative of current fast reactor practice and were not chosen to mitigate the duct dilation problems inherent in the FMSR. In particular, thin-walled ducts with large gaps between assemblies were chosen and raised load pads were placed downstream of the core.

These limitations notwithstanding, and within the context of our current understanding of acceptable core restraint design, the following conclusions can be drawn. They are based on the core mechanics correlations for the FMSR discussed in Section II and the NUBOW-2D (Inelastic) calculations discussed in Section III.

1. The core restraint aspects of the design studied are adequate to prevent excessive free bow due to irradiation creep and swelling. The associated assembly displacements and forces during operation are acceptable.
2. The duct design will result in excessive duct dilation to the point where the ability to remove assemblies for fuel transfer is doubtful.

B. Options

Five alternate design strategies to accomodate duct dilation in the FMSR have been identified. The design strategies are described and briefly discussed in the following paragraphs. The object of each strategy is to limit duct dilation to within the envelope of the portions of adjacent assemblies lying below (downstream from) the core midplane. This design objective, in conjunction with a refueling technique which allows spreading of neighboring assemblies, should allow refueling of the reactor within the limitations of current refueling technology.

1. Free Dilation Approach

In this strategy the duct is allowed to dilate freely throughout its lifetime. This is essentially the design strategy of current LMFBR and GCFR designs. In order for this to work for the FMSR, the diameter to pitch ratio would have to decrease, the wall thickness would have to increase, the pressure would have to decrease and the load pad gaps would have to increase

in some combination. Similarly, a major reduction in fluence would achieve the same end, though this is probably unacceptable. Alloys showing significantly less in-reactor creep strain could also alleviate the problem.

2. The Constrained Dilation Approach

In this design strategy the adjacent duct walls are initially in or near contact at power. Forces between ducts are used to inhibit growth. Duct walls could be thinner, diameter to pitch ratio increased, and load pad gaps could be kept small. However, midcore pressures in adjacent assemblies would have to be equalized, perhaps by inlet and outlet orificing. The approach has obvious neutronic advantages in reduced structural steel and increased fuel volume fraction. However, difficulties might be encountered in equalizing pressures and since the design philosophy is opposite to that currently in use for fast reactors, it may be considered to involve more risk.

3. Round Ducts

The use of round ducts for fast reactors has been previously investigated, Ref. 4, and considered viable for materials in which creep dominates swelling. This is the case for DBI. Dilation is severely retarded and flow geometry is retained with substantially thinner walls. These thinner walls result in a lower steel volume fraction, improve the spectrum, and allow, through adroit choice of pin locations, the fuel volume fraction to remain relatively unchanged. Round ducts could be used with either a free dilation philosophy or a constrained dilation philosophy although the latter is probably more promising because of increased fuel volume fractions.

4. Duct Replacement

A possible alternative to accommodating duct dilation at high fluences is to replace the ducts at each fuel transfer cycle. The peak fluence

for a 800 day cycle is about 2×10^{23} n/cm² and the design studied is viable at that fluence.

5. Vented and Ductless Designs

LMFBR fuel assembly designers are studying and evaluating ductless and vented duct assembly designs. Ductless designs for a gas cooled reactor with steep power gradients are probably not viable, but a vented duct might be feasible and if so, the greatly reduced pressures would decrease duct dilation to manageable proportions. Other alternatives which would reduce duct wall pressures, including channeling coolant flow through the fuel rather than containing the fuel within the coolant, might be considered.

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FIG. 1 KINEMATICS OF DUCT DILATION

FIG. 2 D57 IRRADIATION CREEP MODULUS VS FLUENCE AT 450°C

FIG. 3 D57 FRACTION IRRADIATION SWELLING VS FLUENCE

FIG. 4

EQUAL CIRCUMFERENCE HEXAGON AND CIRCLE

FIG. 5 TYPICAL FMSR DUCT

FIG. 6 FMSR ZONE REPRESENTATION FOR NUBOW-2D(INELASTIC)

FIG. 7 POWER DENSITY DISTRIBUTION

FIG. 8 MID-CORE ASSEMBLY INTERNAL PRESSURE

FIG. 9 FAST FLUX DISTRIBUTION

FIG. 10

AXIAL FAST FLUX DISTRIBUTION

NORMALIZED FAST FLUX (>0.1 MEV)

FIG. 11 ASSUMED VARIATION IN STIFFNESS AS SHAPE CHANGES

FIG. 12 DUCT DILATION DIAMETERS AT REFUELING CONDITIONS.

FIG. 13 MIDCORE DILATION INTERACTION FORCES.

APPENDIX C

Thermal-Hydraulics Description of the JUPITER Code

As stated in the main text, JUPITER is a one-dimensional, multichannel, steady-state code for core thermohydraulic analysis. This appendix describes the relevant details of the helium-cooled version of the code. The data base for JUPITER was obtained from technical information assembled by the General Atomic Company for their GCFR work.

C.1 Thermophysical Properties Used in JUPITER

The following expressions for the pressure- and temperature-dependent properties of helium have been used.

$$\text{Density of Helium} = 6.95 \times 10^{-3} \left(\frac{P}{1230.0} \right) \left(\frac{585.0}{T + 273.0} \right) \text{ gm/cm}^3$$

$$\text{Viscosity of Helium} = 2.77 \times 10^{-6} (T \times 1.8 + 492.0)^{0.674} \text{ gm/cm/sec}$$

$$\text{Thermal Conductivity of Helium} = 0.0173 \left\{ 1.29 \times 10^{-3} (T \times 1.8 + 492.0)^{0.674} + 8.15 \times 10^{-4} (P - 14.7)^{0.28} \right\} \text{ watt/cm/}^{\circ}\text{C}$$

T = Gas Temperature in $^{\circ}\text{C}$

P = Gas Pressure in psi

Helium properties at approximately the inlet and outlet coolant temperatures and other physical properties used as input in the JUPITER model of the FMSR core are given in Table C1.

TABLE C1
Thermophysical Properties Used in FMSR Studies

Density of He at 2000 psi		
300°C	11.537×10^{-3}	gm/cm ³
530°C	8.233×10^{-3}	
Viscosity of He		
300°C	2.976×10^{-4}	gm/cm/sec
530°C	3.736×10^{-4}	
Thermal Conductivity of He at 2000 psi		
300°C	2.516×10^{-3}	watt/cm/°C
530°C	3.128×10^{-3}	
Specific Heat of He	5.2	joules/gm/°C
Thermal Conductivity of Fuel		
inner zone	0.16	watt/cm/°C
outer zone	0.14	watt/cm/°C
Thermal Conductivity of Cladding	0.23	watt/cm/°C
Bond Conductance at Zero Gap	2.0	watt/cm ² /°C
Thermal Emissivity	0.5	

C.2 Heat Transfer and Friction Factor Evaluation

The temperature distribution within the fuel rod was obtained by solving the conduction equation analytically. Two constant thermal conductivity inputs for the two possible radial fuel nodes were used in this calculation. This was considered adequate for this initial study because the thermal conductivity of metal fuel does not vary widely in the temperature range of interest.

The heat transfer performance of the rod cluster was computed by two different methods, one using a modified Dittus-Boelter equation⁽¹⁵⁾ and the other using a turbulent convection heat transfer developed by gas-cooled fast reactor workers^(17,18) specifically for GCFR fuel pins with two-dimensional rectangular ribs. The second method is utilized in the TOMSON subroutine obtained from GA. The equations employed in these two methods are given below.

The steady-state cladding-to-coolant heat transfer and friction factor coefficients⁽¹⁵⁾ programmed in JUPITER are:

$$h = 0.021 * \frac{\text{Th. Cond. Coolant}}{\text{Hydr. Diam.}} * \text{Re}^{0.8} * \text{Pr}^{0.4}$$

$$* \sqrt{\frac{T_{\text{cool}}}{T_{\text{surf}}}} * \text{Lattice Correction} * \text{Roughness Multiplier (RMH)}$$

$$f = 0.079 * \text{Re}^{-0.25} * \text{Roughness Multiplier (RMF)}$$

$$\text{Lattice Correction} = 1 + (1.332 - 2.751 * e^{-D_h/d_r}) * \text{Re}^{-0.1389}$$

$$\text{Re} = \frac{\text{Hydr. Diam.} * \text{density} * \text{velocity}}{\text{viscosity}}$$

$$\text{Pr} = 0.675$$

T_{cool} and T_{surf} are coolant and rod surface temperatures, respectively, and D_h/d = rod hydraulic diameter/rod diameter. The roughness multipliers RMH and RMF can be given as inputs, or the code can evaluate them from built-in correlations, which vary from 1.0 (for $Re = 10^4$) to 2.3 (for $Re > 1.283 \times 10^5$) for RMH, and 1.0 (for $Re = 10^4$) to 3.63 (for $Re > 1.283 \times 10^5$) for RMF.

For Reynolds numbers less than 2000, the following expressions⁽¹⁷⁾ are used:

$$h = \frac{\text{Th. Cond. Coolant}}{\text{Hydr. Diam.}} * \left(-33.6 + 52.2 * (p/d) - 14.8 * (p/d)^2 \right)$$

and

$$f = \left(-32.0 + 65.0 * (p/d) - 14.0 * (p/d)^2 \right) / Re,$$

where the pitch-to-diameter ratio, p/d , must lie between 1.1 and 1.5.

The subroutine TOMSON calculates the friction factor and Stanton number by integration of the logarithmic velocity and temperature profiles^(16,18). The following expression for heat transfer is used:

$$ST = \frac{h}{GC_p} = \frac{F}{8.0} * \frac{1}{1.0 + \sqrt{F/8.0} * (GH - RH)} * Z$$

where $(GH-RH)$ is a function of temperature and velocity profiles and depends on the fuel pin geometry, pitch and clad surface roughening; Z is a "fin-effectiveness" correction given by

$$Z = \left(1.0 - 0.01416 * ST * Re * PR * \frac{\text{Rib Height}}{\text{Hydr. Diam.}} \right) * ST.$$

The friction factor F in this subroutine also depends on the explicit fuel pin geometry, pitch, and clad surface roughening. The subroutine solves the non-linear function of F by an iterative procedure. The friction factor is approximately given by

$$F = \frac{8.0}{C4 - C5 + RH}$$

where

$$C4 = 2.5 * \log_e \left((0.525 * \text{Pitch} - \text{Rod Radius}) / \text{Rib Height} \right)$$

$$C5 = \frac{3.75 + 1.25 * 0.525 * \text{Pitch} / \text{Rod Radius}}{1.0 + 0.525 * \text{Pitch} / \text{Rod Radius}}$$

RH = Function in the Velocity Profile

Both the options available in JUPITER for heat transfer and friction factor have been utilized in the parametric studies leading to the present FMSR design.

C.3 Pressure Drop Calculation

The frictional pressure drop in a channel is calculated from the following equation:

$$\Delta P_f = 4 f \left(\frac{L}{D} \right) \left(\frac{G^2}{2\rho} \right) \times 145.07 \times 10^{-7} \text{ psi}$$

where

f = friction factor

L = length of channel, cm

D = hydraulic diameter, cm

G = coolant flow rate, gms/cm²/sec

ρ = average density of coolant, gm/cm³

The friction factor F takes into account the effect of the roughness of the fuel pin. The pressure drop is therefore computed separately for each axial node.

The acceleration or the momentum change pressure drop is determined from

$$\Delta P_{a.} = \frac{G^2}{2\rho} \left(\frac{\Delta T}{T} \right) \times 145.07 \times 10^{-7} \text{ psi}$$

where

ΔT = temperature rise of coolant in the channel, °C

and

$\bar{T}$ = axial average coolant temperature in °K.

The formula used to determine the pressure drop due to spacer grids in a channel is given by(17):

$$\Delta P_s = N * \lambda * \frac{G^2}{2\rho} * \frac{S}{(1-S)^2} * 145.07 \times 10^{-7} \text{ psi}$$

where

N = number of spacer grids

λ = empirical loss of coefficient = 1.373, and

S = fraction of free flow area blocked by spacer grid.

This formulation of the spacer grid pressure drop was selected for JUPITER because it correctly reproduces the expected results for $S = 0$ (no flow blockage) and for $S = 1$ (complete flow blockage).(19)

Because of its importance in the FMSR design, the ΔP_s expected for one grid is plotted in Figure C1 for several G , λ and S values. Present FMSR design studies indicate a value of $G \sim 80 \text{ gms/cm}^2/\text{sec}$ compared to a value of $G \sim 50 \text{ gms/cm}^2/\text{sec}$ for the 300 MW(e) GCFR. There is some uncertainty as to the value of λ to be used. The CALIOP manual suggests a value of 1.373 for λ , and that is presently used in JUPITER. The 300 MW(e) GCFR design suggested a value of 0.179 for S . These values of G , λ and S indicate a pressure drop between ~ 0.8 and 2.0 psi per grid for the FMSR. Thus, a rather large pressure drop may be produced from the spacer grids. The spacer grid design must, therefore, be optimized to reduce this pressure drop.

FIGURE C1: Spacer Grid Pressure Drop
Versus Flow Area Blocked.

C.4 Intercomparison of Pressure Drop Calculations

To check the pressure drop calculation of JUPITER, the hot channel of the 300 MW(e) GCFR design was modeled and its predictions are compared with two GCFR design analyses which occurred at different times. One is from the designs published in Nuclear Engineering and Design⁽²⁰⁾ and the other is from the PSID for the GCFR⁽¹⁷⁾. The results are shown in Table C2. The data used in JUPITER for the GCFR study are given below.

Active core height = 100 cm (75 cm roughened)

Upper and lower axial blanket = 90 cm

Fuel pellet OD = 0.6333 cm

Fuel volume fraction = 0.277

Flowing coolant volume fraction = 0.446

Inlet pressure = 1230 psi

Hot channel coolant temperature rise = 250°C

Average heating rate = 10.14 KW/ft

Friction multiplication factor = 4.4

Comparisons of the data in Table C2 indicate the computational adequacy of JUPITER calculations. The influences in the values of these predictions will be resolved by checking JUPITER against a specific CALOP⁽²¹⁾ prediction or against the BR2 experimental⁽²²⁾ data.

TABLE C2

Comparison of Pressure Drop Predictions

ΔP (psi)	JUPITER	Veca et al. ⁽²⁰⁾	PSID ⁽¹⁷⁾
Core	29.93	31.89	27.96
		ave = 29.925	
Momentum Change	1.10	0.90	1.15
		ave = 1.025	
Spacer Grip	9.14	4.34	10.64
		ave = 7.49	
Entire Fuel Assembly	40.24	42.47	42.00

★ U.S. GOVERNMENT PRINTING OFFICE: 1979-614-036/16