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Argonne National Laboratory

1000-MW(e) METAL-FUELED FAST BREEDER REACTOR

by

L. E. Link, J. E. Ayer, D. K. Butler,
K. A. Hub, W. B. Loewenstein,
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ANL-7001

Reactor Technology (TID-4500)

AEC Research and

Development Report

CPSTI PRICES

H.C. \$ 4.00; MN .75

Argonne National Laboratory

9700 South Cass Avenue

Argonne, Illinois 60439

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June 1966

Operated by The University of Chicago

under

Contract W-31-109-eng-38

with the

U. S. Atomic Energy Commission

Acknowledgments

Some parts of the Appendix (Section 6.0) have been prepared by staff members other than the authors. Their names will be found in the corresponding part of the Appendix.

The authors wish to acknowledge the assistance of the following persons in the preparation of this report:

L. BURRIS and W. J. WALSH, for furnishing design specifications relating to equipment size, heating requirements, and criticality limitations for the conceptual pyrochemical process.

H. H. HUMMEL, for his contributions to numerous discussions of the nuclear design of the reactor.

K. E. PHILLIPS, for assistance in carrying out the reactor-physics calculations.

A. B. SHUCK, for his assistance and encouragement in providing many helpful suggestions relative to fuel-fabrication processes and techniques.

G. J. VOGEL, for furnishing a basic design and preliminary cost estimates for the waste-disposal facility.

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Abstract

A preliminary design study of a metal-fueled fast power breeder reactor has been made to explore the burnup limits using a model with fuel elements that have sufficient void volume for expansion of gaseous fission products. A discharge burnup of 6% appears to be a reasonable limit, according to the model, for a somewhat degraded neutron spectrum with significant Doppler broadening and a breeding ratio of about 1.5. The expansion volume is suitable for including larger fractions of titanium or zirconium in the fuel to take

advantage of greater high-temperature strength of such fuel for possibly higher burnup with only slight adverse effect on neutronic performance. The fuel-cycle results for a combined processing-fabrication plant located for simple shipment of low-decontamination fuel to three power plants, each of 1000-MW(e) capacity, are: an electric energy cost of somewhat under one mill per kW-hr; and a geometric doubling time for fissile plutonium of 7.6 years.

1.0 Introduction

This study was conducted for the purpose of presenting metallic fuels in a fashion somewhat comparable to the presentations for oxide and carbide fuels, that being in the form of a reactor for a plant having a net electrical power output of 1000 MW(e). However, in doing this it was decided that a close-coupled, short-cooled, fuel cycle should be considered, using the latest developments at Argonne National Laboratory. This meant pyrochemical processing and remote fabrication of injection-cast fuel elements. It was further decided that the blanket portions of the total fuel system should be given the same process and fabrication consideration as the core. This yields a fuel-cycle facility with the only cycle effluents being structural-material scrap, fission products, and surplus plutonium; and the only input required being structural materials for fuels propagation and makeup U^{238} to compensate for fission, process losses, and surplus plutonium. The idea that blanket uranium discharged from the reactor would not recycle and that makeup blanket uranium would be "cold", and thus fabricated in unshielded facilities, was held to be a short-term view.

Every country with a national program for the utilization of uranium has investigated metallic fuels for fast reactors because the breeding potential is inherently the best of any fuel form by a significant margin. Even in dilute systems designed for high heat removal and with limited blanket thickness, the breeding ratio can be as high as 1.7 using uranium-plutonium fuel. The ability of the expansion of metal-

lic fuel to compensate for large values of reactivity has been a prime consideration in the design of developmental reactors to ensure operability and safety. Metallic fuel has proven to be well-suited for fuel recycle in pyrochemical technology, with its advantages of maximum fissile mass in a batch, short cycle time, and protection of personnel from inherent radiation by completely remote handling. Thus, there are strong incentives for considering metallic fuel for fast power-breeder reactors.

During twenty years of development in nuclear energy, there has been steady progress in technology pertinent to the utilization of fast reactors in power plants. Nuclear physics information now available permits reasonably reliable estimates of rather detailed neutronic performance of large, fast reactors. The burnup capability of uranium oxide fuel extends to a few atom percent in commercial practice. Clad samples of different chemical forms of uranium mixed with plutonium in concentrations matching design values for fast reactors have been successfully exposed to several atom percent burnup. Sodium-cooled power reactors in existence should provide a measure of proof testing as part of the continuing program in sodium-component and fuel developments. Thus, the technical progress to date is sufficient to make meaningful design projections of what commercial fast power reactors might be like.

Several groups are working on exploratory concepts. These efforts have a common aspect of extrap-

olating present technology to very high burnup to permit operation for several months before partial refueling. Another incentive for this approach is that fuel-cycle cost estimates, based on private ownership of fuel and anticipated commercial practice for a fuel-cycle industry, are somewhat under 1 mill/kW-hr. A major implication in this low cost figure is that the high burnup is attainable at high specific power, kW/kg Pu, to reduce fissile-material inventory cost.

Metallic fuels for fast reactors have been under development, particularly at Argonne National Laboratory, since the start of developments for the first Experimental Breeder Reactor, EBR-I. While EBR-I was the first nuclear reactor to generate power, the reactor was an experiment whose prime purpose was the successful operation of a fast reactor and proof that calculated breeding gains were factual. However, metallic fuels designed to be considered for eventual commercial fast power reactors were probably not seriously investigated until the early planning stages of EBR-II.

This early planning at Argonne resulted in a decision to consider the unit processes needed for a fuel-cycle facility integrated with the EBR-II reactor and specific to its fuel. This decision was made because it appeared that the use of the highly enriched fuels needed by fast reactors established a need for processes tolerating fuel cooled for short periods with resulting low external inventories; also remote fabrication appeared to be a desirable, if not a necessary, feature for any reactor with recycled fissile and fertile materials.

The first pyrochemical operations established for the EBR-II fuels do not remove certain of the more-noble fission products. This led to the so-called fissium alloys used for the first operating stages of EBR-II.

These early developments of the fissium fuel were aimed at accepting what are now considered relatively low burnups, of the order of 1 to 2%. Throughout this early development of fissium fuel, most workers assumed that only low-burnup fuels were feasible. The reasons for this belief were the known tendency of metallic fuels to swell; the incompatibility of most plutonium-containing alloys with ferrous or nickel alloy cladding, and the relatively low melting point of the uranium-plutonium-fissium alloys. Thus an attitude developed that the metallic fuels were poor competitors for the commercial fast breeder power reactor systems. This was highlighted by the general trend among those doing calculations and projections for the fast reactors to illustrate fuel-

cycle costs for the ceramic fuels, oxides and carbides, at a burnup of at least 10%.

Results that have been obtained largely within the past two years indicate that the metallic fuels are much better than had been assumed. Compatible cladding materials having satisfactory neutronic characteristics have been developed. Higher-melting uranium-plutonium alloys are being studied, burnups of metallic fuels have exceeded 8 a/o, and sufficient knowledge has been gained on the swelling characteristics of the metallic fuels to allow conservative designs for burnups to at least 10% if it is desirable from cost and operations considerations. An example of a recent development is the study of improved fuel alloys containing 25 a/o titanium or zirconium. These should permit higher burnup. See Sections 2.1.6, 6.1.2. Also, developments have shown that the zirconium fuel alloy is compatible with stainless steel and similar cladding alloys. See Section 6.3.1. Thus there is the possibility of using less expensive cladding with such fuels.

The EBR-II Fuel Cycle Facility is now in operation in conjunction with the reactor. It utilizes compact pyrochemical processing, effected only 10-14 days after fuel is discharged from the reactor. It also accomplishes the completely remote fabrication of fuel for return to the reactor. The facility started operation September 2, 1964. As of the middle of June 1965, fuel had been recovered from eleven irradiated reactor fuel subassemblies, and four radial blanket subassemblies have been processed for examination. The rate of processing has thus far been limited only by the rate at which subassemblies have been discharged. A fuel subassembly composed of processed irradiated fuel has been remotely fabricated to meet initial fuel specifications and is now in the reactor core.

About 52 kg of irradiated fuel has been processed in five runs by melt refining, to secure purified ingots for further processing, with an average yield of 94%. This compares very favorably with yields of 90-95% obtained in development studies. Seventeen additional melt-refining runs have been made to prepare additional alloy or to consolidate recycle material. The irradiated ingots have been converted to acceptable fuel pins by injection casting in six runs, with an average production of 52 pins per run. Rejected shard and heel material is recycled; and a skull-reclamation process has been developed. More than 200 pins of processed irradiated fuel have thus far been assembled and welded satisfactorily, and more than 100 have been bonded and bond-tested. Almost 600 acceptable pins of unirradiated fuel al-

may have also been produced in the fabrication equipment by strictly remote techniques with good yields at all steps in the process.

All process and auxiliary equipment, including cranes, manipulators, and transfer devices, has been determined to be operable or has been modified for

improved operation. An atmosphere of argon containing a small amount of nitrogen and about 40 ppm each oxygen and water vapor has been maintained in the argon cell without difficulty for the past 15 months. Operations are proceeding to determine capabilities of the processes and equipment.

2.0 Summary

2.1. DESIGN

The reference design offers a combination of fuel, cladding, and core arrangement that gives an operable reactor with an average fuel burnup of 6% at discharge, a breeding ratio of about 1.5, good control, and exit temperature for coolant of 1000°F.

The chief elements of the design are given in Table 2.1.

The fuel pins, composed of an alloy of uranium and plutonium with titanium, are contained in V-Ti tubes, along with the uranium axial-blanket pins. The blanket has about 50% more uranium atoms per unit of reactor surface than those in designs for the 1000-MW(e) reactor with ceramic fuel. The tubes make up fuel subassemblies, which are of three kinds: the regular subassemblies, those in control-rod positions, and those at the outer zone of the core. In all subassemblies, the tubes are contained in hexagonal tubes of 17-4 PH steel. This alloy was chosen because its coefficient of expansion closely matches that of the tube alloy at temperatures of interest and because it is compatible with sodium. The subassemblies in the control-rod positions contain at the center a tube, in which boron carbide control rods are located. The outer surface of the core-boundary subassemblies is formed by triangular pieces of beryllium clad with stainless steel. The core subassemblies are arranged in two zones of fuel enrichment. The outer zone is of subassemblies with the higher-enrichment fuel.

There are two blankets, an inner and an outer. They are made up of subassemblies similar in design to the fuel subassemblies. The fertile material, U^{238} , is within V-Ti tubes. The inner surface of the inner blanket is formed by triangular pieces of beryllium like those of the fuel elements in the core-boundary position. Surrounding the inner blanket is the outer blanket of subassemblies in two rows.

In the module, the high-enrichment subassemblies of the outer core region and the subassemblies of the inner blanket are oriented so that the beryllium pieces each fit together to form a thin, uniform region of moderator, except at the corners of the core. The outer and inner peripheries of the ring of modules have zones of beryllium outside the outer blankets followed by stainless steel.

Six core modules are arranged in a circular pattern, with the radial centerlines forming the flats of a cen-

tral hexagon. A radial region common to all modules serves as a neutron and thermal shield. The gaps that define the modules divide the outer blanket subassemblies symmetrically. The gaps are filled with sodium to allow expansion and contraction of subassemblies in each module without coupling of radial movements between modules. The arrangement of the subassemblies within each module and of the modules is such that the entire reactor of six modules has a common top level.

The sodium coolant flows into the reactor through a plenum, a structure of three circular plates, which also serves as support for the modules. From the plenum the sodium flows upward through the subassemblies, and out the top of the modules to heat exchangers.

The radial location of subassemblies is controlled by two plates at each module. A titanium pivot plate rests on the top plane of the plenum-support structure. A driver plate of stainless steel rests on the middle plenum plate. The plates have holes for the bottom of subassemblies. Alignment of these holes and their closure around the fuel subassemblies as the temperature reaches operating temperature, because of the differential expansion of the two plates, locks the fuel subassemblies. Fuel subassemblies are oriented by vertical plates set in the under side of the bottom plenum-support plate.

The reactor tank, a cylinder 20 ft in diameter and 30 ft high, contains the modules, reflector, coolant plenums, coolant inlet and outlet tubes, a top neutron shield, a bottom thermal shield, and a rotating shield plug at the top. This plug has a degree of rotation and access plugs so that all fuel, outer beryllium, and outer reflector pieces can be replaced vertically.

2.1.1. Approach

Conceptual inquiry was conducted along a single path. Many somewhat arbitrary decisions were, therefore, necessary. Only the major bases are mentioned here. An important premise was to utilize the metallic system to obtain a neutron energy distribution that would afford high breeding performance consistent with a significant Doppler effect. This basis eliminated the extremes of energy distribution: the hard-spectrum, very high breeding, fissile-seed modules embedded in blanket; and the soft-spectrum, low breeding, single core with mixed fuel interspersed

TABLE 2.1. SELECTED CHARACTERISTICS OF THE CONCEPTUAL DESIGN

Reactor Arrangement		
Number of modules in a ring		6
Module geometry		
Core volume, liters		784
Core height, cm		92
Equivalent core diameter, cm		105
Axial-blanket thickness, cm		33
Radial-region thicknesses, cm		
Core region 1		42
Core region 2		10
Inner radial beryllium		4
Inner radial blanket		10
Outer radial blanket		24
Outer beryllium (at ring boundaries only)		12
Reflector (at ring boundaries only)		36
	Region 1 (Inner)	Region 2 (Outer)
Core Composition		
Fuel element, v/o	46	50
Sodium coolant, v/o	39	42
Control region, v/o	6	0
Shrouds and fuel-element spacers, v/o	9	8
Initial fissile conc, w/o	13.5	17.5
Initial plutonium conc, w/o	17	22
	Pu _f	U
Reactor Loading (approximate average), metric tons		
Core	2.5	14
Blankets	1.0	80
Power		
Nominal electrical output, MW(e)		1000
Reactor output, MW(t)		2510
Fraction produced in core		0.85
Core values		
Output, MW(t)		360
Average power density, kW/liter		450
Maximum power density, kW/liter		670
Average specific power, kW/kg of Pu _f		860
Average linear power, kW/ft		6.6
Heat Transfer and Flow		
Maximum heat flux, Btu/(hr) (sq ft)		6.9 × 10 ⁵
Maximum sodium velocity, ft/sec		24
Reactor pressure drop, psi		93
Total primary coolant flow, lb/hr		101 × 10 ⁶
Temperatures		
Fuel centerline (max), °F		1440
Cladding (max), °F		1126
Sodium (max), °F		1082
Mixed sodium discharge, °F		1000
Steam from superheaters and reheaters, °F		900
Control Rods		
Number per core		13
Boron carbide enrichment, %		50
Total worth per core, %k		6.7
Breeding Performance		
Core conversion ratio		0.64
Total breeding ratio		1.48
Geometric doubling time, yrs		7.6

with moderating material. The intermediate type chosen was a core surrounded first by a thin region of moderating material and then by a radial uranium blanket. The specific physical arrangement selected was a ring of modules with the core of each module surrounded by a thin beryllium zone.

The spacing of adjacent cores by a uranium blanket was based on very low reactivity coupling. The control requirements were thus determined on an isolated core basis. Burnup compensation was expected to be a major requirement. What was looked for in core dimensions and in material concentrations, particularly U²³⁸, was a balance between a suitable sodium-void effect and a core conversion ratio that would permit several month's loading life before partial refueling without incurring significant breeding loss by neutron poison type of burnup reactivity compensation. Because a low core volume fraction for control rods was rather arbitrarily chosen, enriched boron carbide is the only likely material.

The procedure in design exploration was to keep the mixed fuel volume fraction of core constant. In fact, the cladding outside diameter, spacing of fuel elements, and other subassembly dimensions were fixed so that coolant and structural volume fractions were constant. These dimensions were selected so that the hydraulic diameter and coolant volume fraction would result in reasonable values of pressure drop and coolant temperature rise for a core height which, in turn, anticipated the magnitude of acceptable sodium void reactivity effect for an L/D near one.

The chief purpose of detailed investigation was to establish a core radius for single enrichment as determined by fission source distribution and desired fuel and cladding temperatures for a chosen fraction of total power. The effective radius was adjusted so that it corresponded to a uniform hexagonal array of fuel subassemblies on the basis of six modules for the total power.

Finally, the ratio of the maximum to average radial fission source density was decreased by adding a higher-enrichment zone in the outer ring of fuel subassemblies. The enrichments in the two zones were adjusted to keep core power constant with the beryllium ring moved in so that one half of it was in the outer fuel subassemblies. All exploration of the two-zone and single-zone fissile content was performed with nearly constant mixed fuel, structure, and coolant volume fractions.

2.1.2. Safety

An aspect of roughing in design is safety. A common acknowledgment is the importance of having a negative reactivity effect due to temperature rise in

the fuel. Coherent expansion of fuel is not usually regarded as a significant mode for negative reactivity, particularly in the range of the target burnups. The significant effect is Doppler broadening in U^{238} in mixed fuel; the effect is almost zero in Pu^{239} . The suitable magnitude of the Doppler coefficient depends somewhat on the particular temperature-level regime considered in a design. However, widespread consideration is being given to augmenting the coefficient by incorporating materials of low molecular weight, even when moderator atoms are present in the fuel.

The positive reactivity component in sodium-coolant voidage is a safety concern that is influencing the thinking about physical arrangements in the design of high-power fast reactors. Core shapes such as the pancake, the annulus, or the multiple core module are being considered. If the effect were ignored, plutonium inventory would likely be reduced somewhat. There is thus incentive to be just safe enough. Regardless of what reactivity criterion of sodium voidage is to be established, well-developed designs will have provisions for reducing the chance of sodium voids and minimizing their magnitude in order to assure satisfactory reactor operation at the desired power densities.

2.1.3. Basis for Fuel-element Design

Approximately nine percent of the core volume was reserved in the fuel elements for accumulation of fission-product gas on the premise that cast pins expand radially and axially until restrained by cladding wall and end structure. Most of the fuel is expected to be in the fully expanded form over the loading interval, because fuel is known to swell at a rather small fraction of the target burnup magnitude and because fuel is replaced for only one third of a core at each refueling. Only a small negative reactivity effect is associated with the 6% axial expansion of new fuel pins.

Implicit in this approach is the choice of a suitable fuel composition. The 5 w/o fissium mixture with uranium and plutonium initially considered was replaced atom for atom with titanium, chiefly to enhance neutronic performance. Although larger additions of titanium afford higher-temperature operation with only small neutron-absorption penalty, use of a fission-product-gas volume for burnup prediction and fixed heavy-atom volume fraction in the core to have sufficient U^{238} meant that 9 a/o titanium was reasonable for the single-path conceptual inquiry.

Thermal performance of the fuel element was set in part by the effect of reduced density of fuel on conductivity. Power rating per element and sodium exit temperature were chosen to result in a central fuel temperature that afforded a margin of tempera-

ture difference to first melting that would make the Doppler coefficient of reactivity significant.

A maximum cladding temperature of about 600°C is consistent with the above considerations. The V-20 w/o Ti alloy was chosen as having sufficient creep strength at time and temperature to withstand the effect of buildup of fission-product gas to high fuel burnup. The ductility of this material suggested by its crystal form affords a basis for expecting a few percent strain without rupture for fast-neutron irradiation matching burnup, i.e., greater than 10^{23} nvt. Thus, accommodation to irradiated fuel shape at different temperature levels is inferred. Of probable significance is the fact that a low coefficient of expansion and good thermal conductivity combine to result in lower thermal stress than for stainless steel at a chosen heat flux. The vanadium alloy can be fabricated into small-diameter, thin-wall tubes.

The tubing outside diameter of 0.21 in. was chosen in a rather *a priori* manner as representative of the range in which fissile-inventory cost and fuel-fabrication cost compensate. Cladding thickness was optimized for maximum burnup at fixed weight of fuel pin per fuel element; a balance was obtained between the effect of a thick wall restraining pressure of fission-product gas and the effect of a thin wall permitting more volume for burnup gases.

Permissible burnup is believed to have been estimated conservatively by assuming that the fuel is arbitrarily weak and, therefore, by estimating performance of fuel cladding on the basis of all of the fission-product gas pressuring the tube. Two approximations of this burnup model were utilized in design to arrive at an average axial burnup of the fuel pin; the relevant expansion is from a density of 17.6 to 12 g/cm³. An average burnup of 7.1% is consistent with the approximation of a uniform pressure of fission-product gas over fuel height producing the limiting stress-creep condition at the top of the core where coolant is hottest. A maximum of 8.3% is consistent with the approximation of a localized pressure against the cladding somewhat downstream of the core mid-plane, where a combination of rise in sodium temperature and local burnup produces the limiting condition. The local burnup value is consistent with the 7.1 average axial burnup approximation. A discharged-fuel burnup of 6% is achieved because of the low maximum-to-average radial power generation possible with the two-zone enrichment core with beryllium at the edge.

2.1.4. Basis of Core and Blanket Structural Design

Design of subassembly and fuel element was investigated in some detail to assure that closely

spaced elements could be suitably loaded and restrained in a manner consistent with acceptable pressure drop, with the use of automatic fabrication techniques developed for low-decontamination fuel. Lateral restraint of fuel elements within a subassembly was based on a close match of shroud and fuel-element coefficient of expansion by a rather arbitrary choice of shroud material. A simple experiment was performed to confirm that pressure bowing of shroud flats is small and significantly less than the spacing of corresponding parts of adjacent subassemblies.

Control of radial movement of core and inner blanket shroud hexagons within a module was based on differential expansion of a titanium plate and a stainless-steel plate acting at two levels on the lower part of each subassembly to cause selected contact at less than inlet operating temperature of the coolant. The hexagonal tessellation of fuel and blanket subassemblies was extended only to the module boundaries to isolate cores from radial movements in adjacent modules.

The arrangement of control rods inside of fuel subassemblies was preferred to preserve the above uniformity. Control worth was based on a minimum of 2% subcriticality with the core in its most reactive mode and with one control rod removed for refueling its subassembly position. The number of core subassemblies was determined on the basis of choosing the completed hexagonal array, which resulted in handling less than 2% of the core in a refueling step.

Structural features were also based on hydraulic holddown of subassemblies. The single-pass, upward sodium flow was split by fixed orifice arrangement into paths for core and radial blanket cooling. Blanket temperature patterns were anticipated to be acceptable if lateral mixing was enhanced by helical-wire spacing of elements and if plutonium buildup was limited.

2.1.5. Design Characteristics

Of particular interest among the characteristics given in Table 2.1, is the breeding ratio of about 1.5. It is a composite of many effects due to kind and arrangement of materials. The close spacing of fuel elements contributes to high uranium density in the axial blankets. The outer beryllium and stainless-steel reflector make the radial-blanket thickness of 34 cm equivalent in breeding to about 44 cm thickness without this moderator region. The beryllium at the core boundary reduces the breeding ratio about 10%. However, it increases the core conversion ratio somewhat—from 0.61 to 0.64. With this latter value, the burnup reactivity loss over 6 months' operation between refueling one third of a core is estimated at about 3%.

TABLE 2.2. SELECTED REACTIVITY EFFECTS

Core Designation	No Beryllium Ring			Beryllium at Core Boundary		
	Doppler coeff. $\times 10^2$		40% of Na removed, %k	Doppler coeff. $\times 10^2$		40% of Na removed, %k
	Constant Na	40% less Na		Constant Na	40% less Na	
Single core Fizzium alloy	-0.16	-0.12	-0.08	-0.35	-0.32	-0.18
Single core Ti alloy (9%)	-0.23	-0.17	-0.28	-0.52	-0.47	-0.32
Two-zone core Ti alloy (9%)	-0.21	-0.16	+0.06	-0.41	-0.36	0.00

The corresponding boron carbide compensation reduces the breeding ratio in the range of 0.02 to 0.03.

The beryllium ring at the core-blanket interface augments the core average negative Doppler effect by a factor of two or more. It also makes the reactivity effect of uniform sodium voiding more negative. Selected results are shown in Table 2.2.

A limited study of the space dependence of the Doppler effect indicates that the beryllium ring augments the effect by about a factor of six in the outer one third of the core. There is, therefore, the result that the peripherally weighted Doppler effect can be significant and is not diminished by centrally located sodium voiding. A detailed analysis of the spatial factors influencing the sodium void reactivity effect on safety was not in the scope of the study.

There is an interplay between uniform coolant-void reactivity figure of merit and core-expansion reactivity. The former can be held constant by increasing core height at the expense of diameter; however, the core becomes more sensitive in reactivity to radial fuel movement. For the radius chosen, 52 cm, the compaction coefficient of reactivity is significant but not very restrictive. Only a few cents in variation in reactivity is caused by fuel movement associated with mismatch of coefficients of expansion of core materials and/or coolant pressure bowing of shroud flats. The significance of the radial expansion as a negative reactivity effect is illustrated by the decrease of 50 cents for the change from refueling temperature to full-power temperature pattern. The concomitant decrease due to Doppler effect is slightly larger.

The 6% burnup in discharged fuel is the result estimated for two-zone enrichment power flattening and for 7.1% average axial burnup in fuel elements. This average is the value estimated using the gas volume

allowance model, i.e., estimating creep performance of V-20 w/o Ti cladding in restraining the full pressure effect of fission-product gas in the fuel. The low tertiary-material content of fuel consistent with allocation of volume for fission-product gas plus the higher plutonium content in the outer zone result in a rather low value for the solidus temperature. The corresponding fuel operating temperature is raised somewhat, compared with that for single enrichment. These two factors of higher plutonium and higher temperature result in a lower temperature difference in the region of important Doppler broadening. The sodium-void reactivity effect is less favorable for two-zone enrichment, as seen in Table 2.2. The investigation of burnup according to the model thus reached a point of diminishing returns in going from single-zone to two-zone enrichment to achieve an estimated 10% increase in burnup.

Another primary result of the overall study is the estimation of fuel-cycle cost at somewhat less than 1 mill/kW-hour for simple shipment of low-decontamination fuel between a combined reprocessing re-fabricating plant and three fast reactor type power stations of 1000 MW(e) each.

2.1.6. Effect of More Tertiary Material in Fuel

Some scouting work has been done to compare neutron performance of fuel containing 10% titanium with that having 25 a/o titanium and 25 a/o zirconium, based on single enrichment and reactor size and conditions approximating the reference module. The plutonium fraction of the heavy atoms is increased about 10% for both titanium and zirconium. Breeding ratio is decreased by 0.03 for the additional titanium and by 0.05 for the zirconium. The reactivity effect for 40% sodium voiding and the Doppler coefficient estimate remain almost constant.

The additional tertiary material could be incorporated into the reference concept by utilizing part of the volume reserved for fission-product gases. High burnup would need to be predicated on strong fuel to restrain a large fraction of the burnup gases. The increases in solidus temperature with increased tertiary content—110°C for zirconium and 80°C for titanium—form part of the basis for anticipating that these fuel compositions would be stronger than the low-alloy fuel at operating conditions. Another part of the basis is that these temperature differences are approximately the gains in temperature margin between solidus and operating level because the effective thermal conductivity of expanded low-titanium fuel is very close to the conductivity of unexpanded high-titanium or -zirconium fuel with some advantage for zirconium; it is assumed that linear power rate and sodium discharge temperature are not varied.

Detailed calculations of the neutronics of the substitution of the higher titanium or zirconium content are given in Appendix 6.1.2.

2.1.7 Turbine-Generator

The turbine-generator arrangement that matches the reactor outlet temperature of 1000°F probably will consist of two 500-MW(e) units at a primary steam pressure of 1800 psig, and primary and reheat steam temperatures of 900°F. These conditions are based on likely turbine sizes in the near future.

2.2. FUEL CYCLE

The fuel-cycle facility consists of the on-site components employed to process discharged fuel, fabricate processed fuel into fuel elements, and dispose of wastes. The plant design is an approach to that of the smallest close-coupled commercial plant and has a full capacity equivalent to 2.35 times the fuel output from the reference reactor. It utilizes compact pyrochemical processes and fabrication techniques that are controlled remotely. The pyrochemical process used was chosen because this is a type of process that can tolerate short-term-cooled (15 days) fuel; and there is not only extensive laboratory and pilot-plant experience for it, but also certain of the major steps have been successfully demonstrated on a plant scale with irradiated fuel at the EBR-II Fuel Cycle Facility. By June 1965, pyroprocess melting had been carried out, fuel pins had been cast, and fuel subassemblies had been manufactured from irradiated fuel, as well as from unirradiated fuel. These total operations have been accomplished with minimal problems, even though they are startup operations for a completely new type of process. The Fuel Cycle Facility has also provided a good basis for the cost estimates here.

The sequence of operations in the cycle is: storage of fuel assemblies to reduce heat generation; disassembly and decladding of assemblies, removal of fission products from the molten fuel by halide slagging; separation of the uranium and plutonium by a re-torting process in which the uranium precipitates; recovery of the metals as ingots; injection casting of fuel pins; demolding, shearing, and inspection of the pins; loading fuel jackets with sodium; assembly of fuel rods; welding and inspection of the rods; and assembly of the fuel elements.

The radioactive fission products are separated in concentrated form in waste streams from the process steps. Waste having the major part of the activity, is fixed and packaged as a solid salt or as metal in steel containers, which are given interim cooling in a canal at the processing site and then shipped to a

final location for burial. The solid waste has higher densities and higher thermal conductivities than those from major alternative processes. Fission-product xenon, krypton, iodine, and tellurium are separated as volatile matter. Iodine and tellurium are removed by traps and filters; the material is then buried. Xenon and krypton are liquefied, stored, and, after being diluted, are released through a stack to the atmosphere under suitable atmospheric conditions. Aqueous wastes are evaporated to reduce the volume by a factor of 50 and are stored underground in tanks within concrete vaults.

2.3. FUEL-CYCLE COSTS

The estimate of investment costs is based largely on experience with EBR-II. The plant capacity is considered to be the smallest that would be of commercial interest. For a single 1000-MW(e) reactor, operation of the fuel-cycle facility for two shifts per day, five days per week is more economical. This is termed partial-capacity operation. Operating the same plant three shifts per day, seven days per week at 80% plant factor is termed full-capacity operation.

The estimated fuel-cycle investments for building and equipment are shown in Table 2.3. Estimates of annual charges are shown in Table 2.4. These charges are based on 6.75% per year rate for the cost of investment money, a 13% return, and depreciation schedules for 30 years for building, 15 years for non-process equipment, and 5 years for process equipment.

An analysis of fuel-cycle costs was made; a plant load factor of 80%, a 9% per annum charge on working capital, and a value of \$10 per gram of fissionable plutonium were assumed. The unit-energy costs for metallic fuels, at both 6 and 7 a/o burnup, are pre-

TABLE 2.3. FUEL-CYCLE INVESTMENT

	Investment in Building and Equipment, \$	
	Partial capacity*	Full capacity**
Processing and fabrication	10.2×10^6	10.5×10^6
Waste disposal	1.2×10^6	2.1×10^6

* Annual throughput of 10,800 kg core fuel and 23,600 kg blanket fuel.

** Annual throughput of 25,400 kg core fuel and 55,500 kg blanket fuel.

TABLE 2.4. ANNUAL CHARGES FOR FUEL CYCLE

	Annual Charge, \$	
	Partial capacity	Full capacity
<i>Processing and Fabrication</i>		
Capital charges and depreciation	2.6×10^6	3.1×10^6
Operating expense	4.5×10^6	11.5×10^6
<i>Waste Disposal</i>		
Capital charges and depreciation	0.24×10^6	0.41×10^6
Operating expense	0.17×10^6	0.39×10^6
Total annual charge	7.6×10^6	15.4×10^6

TABLE 2.5. EQUILIBRIUM FUEL-CYCLE COSTS FOR METALLIC-FUEL SYSTEMS

Core burnup, a/o	6.0	7.0
<i>Processing, \$/kg</i>		
Core and axial blanket fabrication	155	155
Radial blanket fabrication	150	150
Chemical processing	60	60
<i>Unit Energy, mill/kW-hr</i>		
Fabrication	0.63	0.54
Chemical processing	0.29	0.25
Pu credit	(0.47)	(0.45)
Working capital	0.51	0.58
	0.96	0.92

sented in Table 2.5. Costs are presented for close-coupled volatility processing and central-plant aqueous processing. They provide for the cost of waste disposal, including the cost of perpetual storage. The products of processing are oxide or carbide feed materials for refabrication. The cost of close-coupled volatility processing is based on the full-capacity rate of the conceptual pyrochemical plant of this study, which corresponds to a reactor power generation rate of 2350 MW(e). These costs were developed in a separate study of the economics of processing fast breeder reactor fuels containing about 20% plutonium on a metallic basis and irradiated to about 10% burnup.^{2,1}

REFERENCE

- 2.1 M. Levenson, V. G. Trice, Jr., and W. J. Mecham, *Comparative Cost Study of the Processing of Oxide, Carbide, Fast and Metal-Breeder Reactor Fuels by Aqueous, Volatility, and Pyrochemical Methods*, ANL-7137 (May 1966).

3.0 Reactor System

3.1. Reference Data

The reference reactor is a sodium-cooled fast breeder with metallic fuel in both core and blanket zones. The core fuel is an alloy of uranium-plutonium and titanium clad with vanadium-titanium alloy tubing. The axial blankets are metallic uranium and are integral with the core cladding. The radial blankets are also metallic uranium clad with vanadium-titanium alloy tubing. The fuel is grouped in six modules grouped with their center located on the circumference of a circle. The fuel elements are in hexagonal subassemblies in each module. The control material is boron carbide. The core has two enrichment zones. The radial blanket has two zones, with the inner zone having elements of smaller diameter to accommodate to the higher heat-generation rate in that zone. The inner radial blanket zone also contains some beryllium. The details of the design are given in the data sheet that follows. See Section 6.2.1 for the thermal and hydraulic analysis of the reactor.

DATA SHEET FOR REFERENCE DESIGN

General

Reactor power, MW(t)	2510
Fraction produced in cores	0.85
Nominal electrical plant output, MW(e)	1000
Sodium inlet temperature, °F	720
Sodium outlet temperature (avg), °F	1000
Steam pressure, psig	1800
Steam initial temperature, °F	900
Steam reheat temperature, °F	900
Net plant efficiency, %	39.8
Core conversion ratio	0.64
Total breeding ratio	1.48
Geometric doubling time for reactor fuel, years	7.6

	<u>Pu_f</u>	<u>U</u>
Reactor loading (approximate average), metric tons		
Core	2.5	14
Blankets	1.0	80

Reactor Geometry

Number of modules in ring	6
Active core height, cm	92
Equivalent core diameter, cm	105
Core volume (each module), liters	784
Axial-blanket thickness, cm	33
Radial-region thicknesses, cm:	
Core region 1 (inner)	42
Core region 2 (outer)	10
Inner radial beryllium	4

DATA SHEET FOR REFERENCE DESIGN— Continued

Inner radial blanket	10
Outer radial blanket	24
Outer beryllium (at ring boundaries only)	12
Reflector (at ring boundaries only)	36

Power

Power per module, MW	420
Core power per module, MW	360
Fraction in core	0.58
Fraction in axial blanket	0.04
Fraction in radial blankets	0.11
Core power density (avg), kW/liter	450
Core power density (max), kW/liter	670

Core Data

Number of subassemblies per module	61	
Number of subassemblies in inner zone	37	
Type of subassembly	Hexagonal	
Hexagon cladding thickness, mils	80	
Subassembly pitch, in.	5.2	
Fuel tubes per subassembly	331	
Fuel-tube OD, in.	0.210	
Fuel-tube wall thickness, mils	18	
	Region 1	Region 2
	(Inner)	(Outer)

Core composition

Fuel element, v/o	46	50
Sodium coolant, v/o	39	42
Control region, v/o	6	0
Shrouds and fuel-element spacers, v/o	9	8
Initial fissile conc, w/o	13.5	17.5
Initial plutonium conc, w/o	17	22
Fuel-element composition, v/o		
U-Pu w/o Ti		31
U-Pu-2 w/o Ti		69
Initial density of U + Pu + Ti, g/cm ³		17
Expanded density of U + Pu + Ti + F.P. g/cm ³		12
Fissile conc of fuel discharged from:		
Central zone Pu _f /(Pu + U + F.P.), w/o		11.8
Outer zone Pu _f /(Pu + U + F.P.), w/o		14.5
Total Pu/fissile Pu		1.28
Initial weight of U + Pu in reactor core, metric tons		17.94
Initial weight of Pu _f loaded into core, metric tons		2.5
Final weight of Pu _f discharged from core, metric tons		2.3
Specific power (core avg), kW/kg Pu _f		860
Average linear power, kW/ft		6.6
Core conversion ratio		0.64
Total breeding ratio (reactor)		1.48

DATA SHEET FOR REFERENCE DESIGN—
Continued

Axial Blanket

The axial blanket material above and below the core is contained within the same cladding as the core fuel. Volumetric compositions are the same as in the two zones of the core.

Expanded fuel density, g/cm ³	15
Weight of uranium, metric tons	16.5
Average Pu _f content at discharge, w/o	1.9
Blanket composition (smeared), v/o	
Sodium	40
Structure	9
Control	4
Blanket element	47
	100

Inner Radial Blanket

Number of subassemblies per module	30
NOTE: Part of each subassembly contains some Be blanket region	
Height of blanket, cm	130
Volume per module, liters	484
Composition, v/o	
Fuel element	50
Sodium	42
Steel	8
Fuel-element composition, v/o	
V-20 w/o Ti	31
Uranium	69
Expanded density of fuel, g/cm ³	15
Fuel-cladding diameter, in.	0.21
Fuel-cladding thickness, mils	18
Weight of uranium for reactor, metric tons	14.87
Average Pu _f content at discharge, w/o	3.6

Outer Radial Blanket

Number of subassemblies per module	62
Height of blanket, cm	110
Composition, v/o	
Fuel element	69
Sodium	24
Steel	7
Fuel-element composition, v/o	
V-20 w/o Ti	26
Uranium	74
Fuel expanded density, g/cm ³	15
Fuel-cladding diameter, in.	0.58
Fuel-cladding thickness, mils	40
Weight of uranium for reactor, metric tons	47.4
Average Pu content at discharge, w/o	2.5

Core and Blanket Fuel Design

<i>Core</i>	
Cladding temperature (max), °F	1126
Fuel centerline temp. (max), °F	1440
Expanded fuel length, in.	36
Linear power (avg), kW/ft	6.59
Heat flux, Btu/(hr)(sq ft)	
avg	4.1×10^5
max	6.9×10^5
Maximum sodium temperature in subassembly, °F	1082

DATA SHEET FOR REFERENCE DESIGN—
Continued

Core and Blanket Fuel Design—Continued

Maximum sodium velocity, ft/sec	24
End-of-life cladding stress due to fission products, psi	
avg	20,000
max	33,000
Maximum thermal stress, psi	4,000
Yield stress for 0.2% offset at 1200°F, psi	50,000
<i>Inner radial blanket</i>	
Cladding temperatures (max), °F	1176
Fuel centerline temperature (max), °F	1187
Expanded fuel length, in.	51.2
Linear power (avg), kW/ft	0.76
Heat flux (max), Btu/(hr)(sq ft)	1.4×10^5
Maximum sodium temperature in subassembly, °F	1159
Maximum sodium velocity, ft/sec	4
<i>Outer radial blanket</i>	
Cladding temperature (max), °F	1177
Fuel centerline temperature (max), °F	1227
Expanded fuel length, in.	43.3
Linear power (avg), kW/ft	1.38
Heat flux at hot spot (max), Btu/(hr)(sq ft)	1.45×10^5
Maximum sodium temp in subassembly, °F	1158
Maximum sodium velocity, ft/sec	2.9

Control Rods

Number of control rods per module	13
Material	B ₄ C
Enrichment, %	50
Rod OD, in.	1.56
Rod-cladding thickness, mils	80
Active length, in.	18 or 36
Maximum worth of a single rod, % k/k	-0.7
Total worth per core, % k	6.7
Control required for fuel burnup, % k/k	1.5
Shutdown margin, % k/k	about 2

Primary and Secondary Sodium Systems

Primary sodium flow, lb/hr	101×10^6
Primary sodium temperature, °F	
In core	720
Out core	1000
Pressure drop, psi	93
Number of Na-Na heat exchangers	6
Number of primary pumps	6
Secondary sodium flow, lb/hr	97.8×10^6
Secondary Na inlet to heat exchangers °F	665
Secondary Na outlet from exchangers, °F	960
Secondary sodium pressure drop, psi	30
Water flow to steam generator, lb/hr	7.62×10^6
Water temperature, °F	465
Nominal throttle pressure, psig	1800
Nominal steam temperature, °F	900
Steam flow to reheater, lb/hr	7.02×10^6
Nominal reheat pressure, psig	455
Nominal reheat steam temperature, °F	900

Power Plant

<i>Output, MW</i>	
Gross turbine	1063
Gross generator	1040

DATA SHEET FOR REFERENCE DESIGN—
Continued

Power Plant—Continued

Net plant	1000
Gross turbine heat rate, Btu/kW-hr	8067
Steam flow, lb/hr	7.62×10^6
Condenser pressure, in. Hg	$1\frac{1}{2}$
Condenser flow, lb/hr	5.2×10^6
Shaft-driven boiler feed pumps, MW	23
Stages of feedwater heating	7
Number of turbine generator units	2

3.2. Physics

Physics calculations have been carried out in support of the evaluation study for a 1000-MW(e) fast reactor fueled with plutonium-uranium metal. The design includes a ring of six core modules separated and surrounded by a fertile blanket. Each module is equivalent to a core of about 800 liters, loosely coupled to the neighboring modules. The modular design reduces the positive sodium coefficients of reactivity to an acceptable level.

Initially, efforts were concentrated on a one-zone, 840-liter core module. Concurrent work on design, however, indicated the desirability of improving the power density. As a result, a two-zone core was studied to allow more uniform power distribution across the core, with a resulting increase in power density. A two-zone core is used in the reference design. The characteristics of the two designs differ in some features, which are discussed.

3.2.1. Compositions and Dimensions of One-zone Module

The regional compositions and dimensions used in the neutronics analyses are those of a one-zone core and its associated blankets and reflectors. The effective radial-blanket thickness of a module is dependent upon the radial direction. For example, in a direction between modules, the neighboring core modules share radial blanket material and no radial reflectors are used. In contrast, in a radial direction perpendicular to the ring of six core modules, the radial blanket thicknesses differ, and the blanket contains also a reflector on its periphery. In intermediate radial directions, still other effective blanket thicknesses will occur. Thus a two-dimensional analysis of the ring of six modules will ultimately be required.

These preliminary studies have therefore been directed primarily toward understanding the neutronics of a given 840-liter module core, assuming a range of radial-blanket thicknesses. Outer radial-boundary conditions of zero current with no radial reflector and zero flux with radial reflector have both been studied.

The dimensions of the one-zone core module, asso-

ciated blanket, and reflectors used in the neutronics analyses are:

Core	840 liters vol, 92 cm height, 53.9 cm radius
Axial blanket	33 cm thick
Axial reflector	55 cm
Radial Be region	5.3 cm
Radial blanket	
Region 1	6.1 cm
Region 2	Range of thicknesses ~17–38 cm
Radial reflector	37 cm

These specifications correspond closely to the dimensions and composition requirements set for this design study. The corresponding quantities for the two-zone module are listed in subsequent parts of this section. Some of the dimensions and other specifications arrived at in the physics calculations in this section differ somewhat from those used in the reference design.

The atomic densities of the materials used in the various regions are listed in Table 3.1. A modification of the Argonne 26-group set was used.^{3,1}

In the core these atomic densities represent (v/o): fuel element, 45; sodium, 40; steel structure, 9; and control mechanism, 6. The fuel-element composition is 30 v/o V-Ti (80 w/o V, 20 w/o Ti) cladding and 70 v/o (U + Pu + 4 w/o fissionium + fission products). With 5.5 heavy-atom % burnup in the core as a basis, the fission-product pairs are taken to represent an average of 2.75% heavy-atom burnup: i.e. 2.75% of the (U + Pu) atoms are fission-product pairs. The expanded (U + Pu + Fz + F.P.) density has been assumed to be 12 g/cm³. The atomic-density ratios of Pu isotopes to total Pu atomic density have been assumed: 0.751, 0.210, 0.0295, and 0.0085 for Pu²³⁹, Pu²⁴⁰, Pu²⁴¹, and Pu²⁴² respectively. The 6 v/o of control has been replaced by 4 v/o of Na and 2% steel for most of the analyses to be described. This corresponds to the case of no poison control inserted in the core. For calculations with control poison in the core, the 6 v/o of the control was represented by 2 v/o of natural boron carbide, 2 v/o of steel, and 2 v/o of sodium.

The axial-blanket atomic densities correspond to 45 v/o fuel element, 40 v/o sodium coolant, 9 v/o steel structural material, and 6 v/o for control movement. The latter has been represented by 4 v/o additional sodium and 2 v/o additional steel. The 45 v/o containing fuel element consists of about 70 v/o U²³⁸ and 30 v/o of the V-Ti cladding alloy. One w/o of the uranium fuel is assumed to be Pu²³⁹, which represents the average fissile concentration at mid-life of the core. The expanded fuel density (i.e. U²³⁸ + Pu²³⁹) is assumed to be 15 g/cm³ in the blankets.

The atomic composition of the axial reflector corre-

TABLE 3.1. REGIONAL ATOMIC DENSITIES USED IN SINGLE-ZONE MODULE CALCULATIONS, 10^{24} UNITS

	Core	Axial Blanket	Axial Refl	Radial Be Region	Radial Blanket		Radial Refl
					Reg 1	Reg 2	
Pu ²³⁹	.00893*	.000119	—	—	.000299	.000191	—
U ²³⁸ †		.011824	—	—	.012172	.018902	—
Stainless steel							
Fe	.006765	.006765	.01599	.00738	.00615	.004305	.058425
Cr	.001782	.001782	.004212	.001944	.00162	.001134	.01539
Ni	.000847	.000847	.002002	.000924	.00077	.000539	.007315
Na	.00968	.00968	.0132	.00110	.00946	.00550	.0011
Fizzium	.000892	—	—	—	—	—	—
(+Zr)‡	(.000892)	—	—	—	—	—	—
F.P. pairs	.000256	—	—	—	—	—	—
V	.007272	.007272	.007541	—	.007595	.009523	—
Ti	.001933	.001933	.002005	—	.002019	.002532	—
Be	—	—	—	.103	—	—	—

* For the core, this quantity includes also the higher Pu isotopes: Pu²⁴⁰, Pu²⁴¹, Pu²⁴², as well as Pu²³⁹ and U²³⁸.

† 1500°K, U²³⁸ cross-section parameters used for Doppler calculations.

‡ To simulate the non-capture properties of the listed fizzium capture material.

sponds to 60 v/o sodium, 26 v/o steel, and 14 v/o V-Ti cladding alloy.

The radial beryllium-containing zone surrounding the module consists of 5 v/o sodium, 12 v/o steel, and 83 v/o beryllium.

The atomic densities of the inner radial blanket region represent 47 v/o fuel element, 10 v/o steel structure, and 43 v/o sodium. The 47 v/o fuel element consists of about 70 v/o U²³⁸ and 30 v/o V-Ti cladding alloy. Of the uranium fuel, 2.4 w/o is Pu²³⁹; this represents the average fissile concentration at mid-life.

The outer radial blanket region contains 68 v/o fuel element, 7 v/o steel structure, and 25 v/o sodium. The fuel element consists of about 74 % U²³⁸ and 26 % V-Ti alloy. The average fissile concentration at mid-life is represented by Pu²³⁹ present as 1 w/o of the uranium fuel.

The radial reflectors are not present between modules. They must be considered, however, to assess their effect at the outside of the ring of the six modules. Calculations with steel reflectors and with beryllium reflector regions between outer blanket and steel reflectors have been carried out. The steel reflector is 95 v/o steel and 5 v/o sodium. The composition of the outer beryllium region is taken to be the same as that of the core-blanket interface region described previously.

3.2.2. Treatment of Axial-reflector Savings

Axial core dimensions were increased by calculated reflector savings to allow the use of the one-dimensional cylindrical diffusion theory codes. The axial-reflector savings were calculated both with the normal sodium volume fraction and with the sodium volume

fraction reduced by 40 %. Use of reflector-savings values dependent upon the sodium content of the core is necessary in order to estimate more correctly subsequent cylindrical calculations of reactivity changes resulting from 40 % reduction in the core sodium. The slab-geometry diffusion-theory calculations for the axial reflector savings assumed extrapolated radial bare core dimension of 71.0 cm; i.e., $B_{\text{radial}}^2 = .0011472 \text{ cm}^2$. This corresponds to an *a priori* assumed radial reflector savings of 17.1 cm. The calculated axial reflector savings with full sodium and 60 % of full sodium in the core are about 18.72 cm and 20.57 cm respectively. The axial-reflector savings thus increases by about 1.85 cm upon reduction of the core sodium by 40 %. This increase represents of the order of a few tenths of a percent in reactivity. If this increment height increase is not included in the sodium-void calculations, the results will be considerably overoptimistic. Except where noted otherwise, the axially bare core height upon removal of 40 v/o of the core sodium was simultaneously increased by 3.70 cm overall.

The axial source distribution for the case of sodium in the core is given in Fig. 3.1.

3.2.3. Estimate of Axial Contribution to Breeding Ratios

The cylindrical geometry, extrapolated bare height, breeding ratios must be augmented by the breeding contributions in the axial blankets and in the regions of the radial blankets situated axially below and above the core height.

These axial breedings have been estimated by auxiliary slab-geometry calculations to obtain the ratio of axial blanket captures in U²³⁸ relative to total ab-

FIG. 3.1. Axial Source Distribution.

sorption in axial blankets and the ratio of net leakage and fission sources in the axial blankets relative to leakage into the axial blankets. These ratios are 0.80 and 1.12 respectively; also the ratio of Pu^{239} absorptions to U^{238} captures is about .09 in the blanket. These auxiliary calculations assumed radial buckling, B_r^2 , corresponding to radial extrapolated bare cores of 71 cm. Subsequent calculations employing energy-dependent radial bucklings for cases with and without the beryllium regions showed no significant variations from these ratios. Leakages into axial blankets and into axial regions of the radial blankets for the various cylindrical cases, obtained by energy-volume integrations of $\text{DB}_z^2\phi$ over the extrapolated bare-height cylindrical cores and radial blankets, together with the above ratios, were then employed in estimating the axial breeding contributions.

For systems with no radial beryllium region at the core-blanket interface, the axial-blanket regions contribute about 0.38 to the breeding. With a beryllium region at the interface, this contribution is lowered to 0.31. The lower value is in part the result of the lesser core-to-radial-blanket leakage with, therefore, a subsequent lower leakage axially in the radial-blanket regions.

These values are applicable for the isolated modules having a thick (44.1 cm) radial blanket surrounded by

the steel reflector as well as for the cases having the thinner (29.1 cm) radial blanket surrounded by the beryllium reflecting region.

3.2.4. Effects of Radial Beryllium-containing Regions and Variation of Radial-blanket Thickness

The effects of the radial beryllium-containing regions at the core-blanket and/or blanket-reflector interfaces upon critical composition, breeding, core conversion ratio, sodium effects, and Doppler effects of isolated modules are listed in Table 3.2.

The beryllium-containing region at the blanket-reflector interface was assumed to have a composition identical with that listed for the core-blanket interface. The thickness, however, was 15 cm rather than 5.3 cm.

The beryllium at the core-blanket interface augments the negative Doppler by a factor greater than two. The calculations show that the beryllium region at the core-blanket interface can also augment the negative reactivity due to uniform voiding of core sodium.

The presence of the inner beryllium region reduces the breeding by a maximum of about -0.15 (i.e. 1.58-1.43), of which about 45% is due to reduction in axial-breeding contributions. With a somewhat thinner blanket, the presence of the beryllium region reduces

TABLE 3.2. EFFECTS OF BERYLLIUM REGIONS AND BLANKET THICKNESS UPON ISOLATED REFLECTED MODULES

Calculated Quantities	Radial Blanket Thickness	
	29.1 cm	44.1 cm
<i>No Beryllium Region</i>		
Crit Conc*	0.190	0.190
C.C.R.†	0.60	0.60
T.B.R.‡	1.46¶	1.58**
Sodium§	-0.06% <i>k</i>	-0.07% <i>k</i>
Doppler	-0.16	-0.16 (-0.11)
<i>Beryllium at Core-Blanket Interface</i>		
Crit Conc	0.189	0.189
C.C.R.	0.63	0.63
T.B.R.	1.40††	1.43††
Sodium	-0.17% <i>k</i>	-0.17% <i>k</i>
Doppler	-0.36 (-0.33)	-0.35 (-0.32)
<i>Beryllium at Core-Blanket and Blanket-Reflector Interfaces</i>		
Crit Conc	0.189	
C.C.R.	0.63	
T.B.R.	1.42††	
Sodium	-0.18% <i>k</i>	
Doppler	-0.35 (-0.32)	

$$* \text{ Crit Conc} = \frac{(\text{total Pu}) \text{ in core}}{(\text{total Pu} + \text{U}^{238}) \text{ in core}}$$

$$\dagger \text{ C.C.R.} = \frac{(\text{captures in U}^{238}, \text{Pu}^{240}, \text{and Pu}^{242}) \text{ in core}}{(\text{absorptions in Pu}^{239} \text{ and in Pu}^{241}) \text{ in core}}$$

$$\ddagger \text{ T.B.R.} = \frac{(\text{captures in U}^{238}, \text{Pu}^{240}, \text{and Pu}^{242}) \text{ in core and blanket}}{(\text{absorptions in Pu}^{239} \text{ and U Pu}^{241}) \text{ in core and blanket}}$$

§ Reactivity resulting from a 40% reduction in core sodium.

|| Based on reactivity resulting from U^{238} Doppler effect in core for temperature change 750°K to 1500°K with constant composition (and with 40% less sodium in core). The Δk 's have been converted to $T(\delta k/\delta T)_{\text{Doppler}}^{10^5}$, assuming this quantity to be independent of temperature.

¶ Includes 0.33 axial contributions.

** Includes 0.38 axial contributions.

†† Includes 0.31 axial contributions.

breeding less than does a system with the same thinner blanket but no beryllium at the core-blanket interface.

The addition also of an outer beryllium region in the case of the thinner (29 cm) radial blanket, however, results in a breeding ratio of 1.42, which is comparable to 1.43 obtained with the thicker blanket when the inner beryllium region is present.

The beryllium-containing region at the radial interface of the core and blanket results in radial power flattening, as shown in Fig. 3.2, in which a comparison is made between the radial source distribution with and without the beryllium region. The spectral degradation resulting from the presence of the beryllium region is shown in Table 3.3. The central-group fluxes are compared with the average core fluxes, both with and

without the inner beryllium region present. This preferential lowering of the spectrum in the radial peripheral region of the core is advantageous in that centrally located sodium voiding of the core does not diminish the peripherally weighted Doppler effect.

3.2.5. Spatial Dependence of the Sodium-void and Doppler Reactivities

Radial spatial dependence of sodium-void and Doppler effects have been studied both with and without the beryllium regions. Infinite-height cylindrical geometry was used for these radial studies. One-zoned core modules having fissium-alloy fuel were used.

The comparative reactivities due to 40% sodium removals separately in three equal-volume radial regions are given in Table 3.4.

The presence of the radial beryllium region results in a more rapid decrease of the sodium-void effect as a function of core radius. These results are for infinite-cylinder geometry; thus axial effects are not included. If it is assumed that the axial effects are independent of the radial position in the core, the spatial values may be modified to approximately include the axial effects. This has been done in Table 3.4 to obtain the finite-cylinder values. The infinite-cylinder values were re-normalized to the values for the total core obtained from previous finite-height core values. The latter are the values -0.06 and -0.17 given in Table 3.2. Thus

TABLE 3.3. CENTRAL AND AVERAGE GROUP FLUX SPECTRA IN CORE

Group J	E_{LJ}	Central Flux		Average Flux	
		No Be	With Be	No Be	With Be
1	3.68 Mev	.0163	.0163	.0157	.0158
2	2.23	.0315	.0315	.0303	.0310
3	1.35	.0552	.0553	.0528	.0548
4	.825	.0838	.0841	.0807	.0818
5	.498	.1214	.1221	.1180	.1169
6	.302	.1459	.1469	.1436	.1398
7	.181	.1448	.1456	.1443	.1313
8	.111	.1288	.1291	.1302	.1200
9	.0674	.0968	.0967	.0995	.0927
10	.0409	.0721	.0718	.0759	.0716
11	.0248	.0416	.0414	.0443	.0446
12	.015	.0182	.0181	.0195	.0221
13	.0091	.0118	.0117	.0126	.0155
14	.0043	.0141	.0140	.0153	.0214
15	.0026	.0018	.0018	.0020	.0035
16	.00204	.0049	.0049	.0054	.0082
17	.00123	.0050	.0050	.0055	.0095
18	.00096	.0022	.0022	.0025	.0046
19	.000583	.0013	.0013	.0015	.0049
20	.000275	.0003	.0003	.0004	.0034
21	.000101	.0001	.0001	.0001	.0036
22	.000029	.0000	.0000	.0000	.0030

FIG. 3.2. Comparison of Radial Source Densities with and without Beryllium Region.

TABLE 3.4. RADIAL REGIONAL SODIUM VOID REACTIVITY, % k

Radial Region	Region			Sum	Total Core
	1	2	3		
Infinite cylinder					
No Be	0.71	0.18	-0.099	+0.80	+0.78
With Be	0.66	0.11	-0.22	+0.55	+0.53
Finite cylinder					
No Be	0.43	-0.10	-0.37	-0.04	-0.06
With Be	0.43	-0.12	-0.45	-0.14	-0.17

with axial effects included, the presence of the beryllium does not alter as much the radial dependence of reactivity due to sodium voiding.

Reactivities due to Doppler broadening occurring separately in three equal-volume radial regions of the core are given in Table 3.5. The listed Doppler coefficients are based on an assumed temperature increase of 750°K to 1500°K in the U^{238} fertile material. Simultaneously with the regional increase in U^{238} temperature, there is also assumed a 40% decrease of sodium in the same region. The reactivity increment due to sodium reduction is not, however, included in the listed co-

efficients. The geometry is an infinite-height cylinder. The negative Doppler effect in the outer third of the core is augmented by a factor of about six with the beryllium region present.

3.2.6. Worth of Central Subassembly

The core of each module is considered to be composed of fuel subassemblies. Calculations have been made to determine the reactivity worth of the central subassembly. It is assumed that removal of the central subassembly is accompanied by simultaneous addition

TABLE 3.5. RADIAL REGIONAL DOPPLER COEFFICIENTS, $T(\delta k/\delta T)_{\text{Doppl.}}^{10^2}$ FOR INFINITE CYLINDERS

Radial Region	Region			Sum	Total Core
	1	2	3		
40% Na decrease					
No Be	-0.076	-0.048	-0.033	-0.157	-0.140
With Be	-0.077	-0.091	-0.200	-0.368	-0.364
Full Na					
No Be	-0.097	-0.063	-0.037	-0.197	-0.195
With Be	-0.097	-0.104	-0.215	-0.415	-0.405

of sodium to fill the resulting void. The effects on reactivity of progressively and homogeneously reducing the volume fraction of the fuel subassembly, with simultaneous replacement by sodium is shown in Fig. 3.3. The modified radial region has a radius of 6.95 cm. The complete removal of the central subassembly reduces k by about 1.6 %.

3.2.7. Effect of Dispersed Poison

The use of boron-containing control and shutdown rods is being considered. An estimate of the effects of boron poison was calculated for the system with beryllium-containing region and thick radial blanket. Two v/o of $B_4^{nat}C$ was homogeneously substituted for an equal volume of core sodium. The reactivity change was -5.4% . The corresponding increment changes in internal and total breeding were -0.08 and -0.10 respectively. The reactivity variation in a fuel cycle is estimated to be 2 to 3 %k. The time-average reactivity variation will be about 1 to 1.5 %k. The effect on breeding will therefore be only about one-fourth of the previous increments, or about -0.02 and -0.03 respectively.

3.2.8. Substitution of Titanium-alloy Fuel for Fizzium Alloy

The neutronics effects of the presence of fizzium in the fuel material of the core has been studied by substitution of an equal number of titanium atoms for the fizzium atoms. (This *ad hoc* interchange of fizzium atoms with titanium atoms results in a slight decrease in the expanded fuel density, from 12 g/cm³ to 11.7 g/cm³, in these neutronic calculations.)

Results of the calculations are given in Table 3.6 for cylindrical-geometry, axially extrapolated bare core systems having a radial blanket about 29 cm thick,

surrounded by a 15-cm region containing beryllium, which is in turn surrounded by a 37-cm reflector.

The use of titanium rather than fizzium increases breeding by about $+0.06$ and the presence of the inner beryllium-containing region decreases breeding by about -0.13 . This decrease is less than the -0.15 deduced from Table 3.2 because the thinner blanket, together with the outer beryllium region, is not quite as efficient as the thicker blanket alone, if the inner beryllium is not present in both cases.

By comparison with the sodium-voiding results given in Table 3.2, the removal of fizzium substantially increases the magnitude of the overall negative sodium-void effect. The presence of the beryllium region affects the sodium-void effects less, however, with the titanium-alloy fuel.

The titanium cases have an incremental change in axial reflector saving of 1.05 cm upon removal of 40 % of the core sodium. The sodium-void values have been corrected for this effect.

Removal of the fizzium improves the Doppler reactivity effects; these values are approximately 40 % more negative.

3.2.9. Reactivity Effects of a Central Poison Column

The worth of a central cylindrical column of poison and its effects upon breeding, sodium void, and Doppler effect have been estimated to obtain data for subsequent control-rod design. The $B_4^{nat}C$ poison is contained in a cylindrical volume of 2.54 cm radius. The effects of substituting this poison column for an equal volume of core material are listed in Table 3.7. Column insertion in this calculation thus replaces only 0.00222 volume fraction of core material. This substitution is in a

FIG. 3.3. Multiplication Decrease Resulting from Reduction of Control Subassembly Density.

TABLE 3.6. COMPARISON OF TITANIUM- AND FIZZIUM-ALLOYED FUELS

Be Region	Calculated Quantities	With Titanium	With Fissium
No Be at core-blkt inter-face; Be at blkt-refl inter-face	Crit Conc*	0.182	0.190
	C.C.R.†	0.64	0.60
	T.B.R.‡	1.62¶	1.56¶
	Sodium§	-0.28% <i>k</i>	-0.08% <i>k</i>
	Doppler	-0.23 (-0.171)	-0.16 (-0.12)
Be at core-blkt interface and at blkt-refl interface	Crit Conc*	0.179	0.189
	C.C.R.†	0.68	0.63
	T.B.R.‡	1.49**	1.42**
	Sodium§	-0.32% <i>k</i>	-0.18% <i>k</i>
	Doppler	-0.52 (-0.47)	-0.35 (-0.32)

$$* \text{ Crit Conc} = \frac{(\text{total Pu}) \text{ in core}}{(\text{total Pu} + \text{U}^{238}) \text{ in core}}$$

$$\dagger \text{ C.C.R.} = \frac{(\text{captures in U}^{238}, \text{Pu}^{240}, \text{and Pu}^{242}) \text{ in core}}{(\text{absorptions in Pu}^{239} \text{ and in Pu}^{241}) \text{ in core}}$$

$$\ddagger \text{ T.B.R.} = \frac{(\text{captures in U}^{238}, \text{Pu}^{240}, \text{and Pu}^{242}) \text{ in core and blanket}}{(\text{absorptions in Pu}^{239} \text{ and in Pu}^{241}) \text{ in core and blanket}}$$

§ Reactivity resulting from a 40% reduction of core sodium.

|| Based on reactivity resulting from U²³⁸ Doppler effect in core for a temperature change of from 750°K to 1500°K with constant sodium composition and with 40% less sodium in core. The Δk 's have been converted to $T(\delta k/\delta T)_{\text{Doppl.}}^{10^2}$, assuming this quantity independent of temperature.

¶ Includes 0.38 axial contribution.

** Includes 0.31 axial contribution.

TABLE 3.7. REACTIVITY EFFECTS OF A CENTRAL B₄C CYLINDRICAL COLUMN

	Δk	Crit Conc*	C.C.R.†	T.B.R.‡§	Na	Na Doppler	
						Na Out¶	Na In
No B ₄ C	0	0.181	0.65	1.69	-0.24% <i>k</i>	-0.17	-0.24
B ₄ C in	-0.0130	0.186	0.63	1.68	-0.14	-0.16	-0.22
B ₄ C ho-moge-neous	-0.0062	0.183	0.64	1.68	-0.18	-0.16	-0.23

$$* \text{ Crit Conc} = \frac{(\text{total Pu}) \text{ in core}}{(\text{total Pu} + \text{U}^{238}) \text{ in core}}$$

$$\dagger \text{ C.C.R.} = \frac{(\text{captures in U}^{238}, \text{Pu}^{240}, \text{and Pu}^{242}) \text{ in core}}{(\text{absorptions in Pu}^{239} \text{ and Pu}^{241}) \text{ in core}}$$

$$\ddagger \text{ T.B.R.} = \frac{(\text{captures in U}^{238}, \text{Pu}^{240}, \text{and Pu}^{242}) \text{ in core and blanket}}{(\text{absorptions in Pu}^{239} \text{ and in Pu}^{241}) \text{ in core and blanket}}$$

§ Include 0.38 axial-breeding contribution.

|| $T(\delta k/\delta T)_{\text{Doppl.}}^{10^2}$.

¶ Doppler coefficient for core having 40% less Na.

cylindrical-core module having titanium-alloy fuel, a 38-cm-thick radial blanket with zero-current outer boundary conditions, no beryllium regions, and one-zone core. The last row in the table gives the effects resulting from an assumed homogeneous core distribution of the poison contained in the column. (In this case no core material is assumed to be removed.)

3.2.10. Inter-module Breeding and Module Coupling

An estimate has also been made of the breeding in radial blanket regions between modules and of the reactivity coupling between two adjacent modules. Four systems analogous to those compared in Table 3.6 were calculated without the outer beryllium regions and with zero-current boundary conditions to simulate the breeding properties between modules. To obtain estimates of the variation in breeding with blanket thickness and of the reactivity coupling between modules, two blanket thicknesses were studied. The thicker blanket, 38 cm, corresponds to essentially the uncoupled module with zero-current outer boundary conditions. The thinner blanket, 23 cm, corresponds approximately to an average separation distance in the direction between modules.

Calculated breeding ratios for these zero-current cases are given in Table 3.8. These values have been corrected for axial breeding effects by using the values previously obtained. Axial breeding effects for zero-current radial conditions, however, should be somewhat larger than for the cases having reflectors.

From geometry considerations, estimates have been made of the incremental breeding increase. It is assumed for simplicity that $\frac{2}{3}$ of the radial periphery represents zero-flux boundaries, that $\frac{1}{3}$ represents zero-current boundaries, and that the latter is composed half of the thinner blanket and half of the thicker. Thus the cases of a total blanket thickness of 23 cm in Table 3.8 correspond to the thinner, and the total blanket thickness of 38 cm to the thicker. The zero-flux cases are from Table 3.6. The incremental increase in breeding due to increased breeding in radial blanket regions

TABLE 3.8. COMPARATIVE BREEDING RATIOS FOR CYLINDRICAL-GEOMETRY CELL CASES

Beryllium Region	Total Blanket Thickness, cm	Breeding Ratio		C.C.R.	
		With Ti	With Fz	With Ti	With Fz
None	23	1.68*	1.62*	.67	.625
	38	1.69*	1.63*	.65	.60
At core-blanket interface	23	1.52†	1.45†	.69	.64
	38	1.52†	1.46†	.69	.63

* Includes .38 axial contribution.

† Includes .31 axial contribution.

between neighboring modules is thus estimated as +0.02 and +0.01, respectively, depending upon whether the core-blanket beryllium region is absent or present.

Reactivity differences between the thick and thin blanket cases of Table 3.8 are:

Be Region	Fuel Alloy	
	Ti	Fz
None	1.43% (1.20%)	1.36% (1.14%)
At core-blanket interface	0.45% (0.34%)	0.40% (0.30%)

The analogous values obtained assuming 50% less Pu^{239} in the radial blankets are given in the parentheses.

The reactivity coupling of a given module to its two nearest neighbors will be about a third of the values given above. Two-dimensional calculations incorporating the ring of modules, however, are required to accurately calculate coupling and its dependence upon module separations and inter-module fissile buildup.

3.2.11. Two-zone Enriched Module

Use of two-zone enriched core modules, together with a beryllium interface between core and radial blanket, has been considered in order to improve power flattening. In the two-zone core case the module dimensions and compositions differ from those listed for the one-zone core. These dimensions are:

Core region 1	511.2 liters, 92 cm height, 42.2 cm radius
Core region 2	272.4 liters, 92 cm height, 10.1 cm thick
Axial blanket	33 cm thick
Axial reflector	55 cm
Inner radial Be region	4.2 cm

Radial blankets

Region 1	9.7 cm
Region 2	24.0 cm
Outer Be region	12.0 cm
Radial reflector	36.0 cm

The module core volume is thus about 784 liters. Analyses were by cylindrical one-dimensional geometry and zero-flux outer-boundary conditions.

The atomic densities used in the calculations of the various regions are listed in Table 3.9.

The same axial reflector savings and variation of axial reflector savings used in the previous one-zone modules, with titanium fuel alloy, were used.

In the two-zoned cases these atom densities represent respectively 45.79 and 49.69 v/o fuel element, 39.42 and 42.40 sodium, and 8.80 and 7.91 steel structure. The inner core zone in addition contains 6 v/o control made up of about 58 v/o Na, 35 v/o steel, and an assumed 7 v/o B_4^{nat}C to represent poison present during operation. The fuel element composition is 31.3 v/o V-Ti (80 w/o V and 20 w/o Ti) cladding and 68.7 v/o (U + Pu + Ti) alloy plus fission-product pairs. With the use of 6 heavy atom % as the average burnup, the assumed fission-product pairs are taken to represent a time average value in burnup cycle of 3% burnup; i.e. 3% of the (U + Pu) atoms are fission-product pairs. Expanded (U + Pu + Ti + F.P.) density has been retained at 12 g/cm³. The fuel alloy has been assumed to contain about 8.9 a/o titanium.

Results of cylindrical one-dimensional calculations for the zoned module with and without the core-blanket beryllium region are given in Table 3.10 for three outer enrichment ratios, $(\text{Pu}/\text{Pu} + \text{U})_{\text{core reg 2}} = 0.20, 0.21, \text{ and } 0.22$. Criticality in all cases was by variation of $(\text{Pu}/\text{Pu} + \text{U})_{\text{core reg 1}}$.

TABLE 3.9. REGIONAL ATOMIC DENSITIES USED IN ZONED MODULE CALCULATIONS, 10²⁴ UNITS

	Core		Radial Be Region	Radial Blkt		Outer Radial Be Region	Radial Refl
	Reg 1	Reg 2		Reg 1	Reg 2		
Pu^{239}	.009088*	.009862*	—	.000311	.000194	—	—
U^{238}			—	.012647	.019216	—	—
Stainless steel							
Fe	.006701	.004865	.015375	.004865	.004287	.008610	.057810
Cr	.001765	.001281	.004050	.001281	.001129	.002268	.015228
Ni	.000839	.000609	.001925	.000609	.000537	.001078	.007238
Na	.009436	.009328	.002200	.009328	.005326	.001320	.001320
F.P. pairs	.000281	.000305	—	—	—	—	—
V	.00772	.008378	—	.008378	.009527	—	—
Ti	.002961	.003213	—	.002052	.002334	—	—
Be	—	—	.08034	—	—	.09888	—
B_c^{nat}	.000342	—	—	—	—	—	—
	.000086	—	—	—	—	—	—

* For core regions, these quantities include also the higher Pu isotopes: Pu^{240} , Pu^{241} , Pu^{242} , as well as Pu^{239} and U^{238} .

TABLE 3.10. RESULTS OF CYLINDRICAL CALCULATIONS FOR TWO-ZONE CORE MODULES

Be Region	$\left(\frac{\text{Pu}}{\text{Pu} + \text{U}}\right)_{\text{core}}$		$\left(\frac{\text{Fissile Pu}}{\text{Pu} + \text{U}}\right)_{\text{core}}$		C.C.R.	T.B.R.	40% Na Void In Core	Doppler*		Sources Peak to Average at Cylindrical Midplane
	Reg 1	Reg 2	Reg 1	Reg 2				Na in	Na out†	
No Be at core-blkt interface, but	0.178	0.200	0.139	0.156	0.63	1.63‡	+0.02% <i>k</i>	-0.21	-.16	1.35
Be at blkt-refl in- terface	.176 .173	.210 .220	.137 .135	.164 .172	.63 .62	1.63 1.63	+.06 +.10	-.21 -.21	-.16 -.16	1.32 1.29
Be at core-blkt in- terface	0.176	.200	.137	.156	.66	1.50§	-.05% <i>k</i>	-.41	-.36	1.25
and										
Be at blkt-refl in- terface	.173 .170	.210 .220	.135 .132	.164 .172	.65 .65	1.50 1.50	.00 +.04	-.41 -.41	-.36 -.36	1.20 1.16

* $T'(\delta k/\delta T)_{\text{Doppl.}}^{10^2}$.

† For 40% less sodium in core.

‡ Includes 0.38 axial contribution.

§ Includes 0.31 axial contribution.

FIG. 3.4. Radial Source Density Distribution (Outer Enrichment Ratio, 0.210).

Comparisons of these two-zone results with those of the analogous one-zone titanium-alloy-fueled cases in Table 3.6 show that the resulting breeding ratios are essentially unchanged even though in the central zone B_4C is present in the case listed in Table 3.10. Doppler effects are not changed by the use of zone enrichment. The lesser Doppler effect in the two-zone, relative to one-zone, beryllium-interface case is instead largely

due to the smaller effective thickness of this beryllium region. Zoned loading does, however, decrease the previous more optimistic sodium-void effect. This becomes more evident with increase in enrichment in the outer core zone. The combined effects of zoned

FIG. 3.5. Radial Source Density Distribution (Outer Enrichment Ratio, 0.220).

loading and a beryllium-containing region of moderate size do, however, considerably improve the peak-to-average source value. Radial source distributions for two of the zoned cases containing the beryllium region are shown in Figs. 3.4 and 3.5. The latter case has the better peak-to-average value and more uniform radial distribution.

Integrated radial-dependent sodium removal from the two-zoned, beryllium-interface, module having outer core region enrichment of 0.21 has been calculated. Removal of 40 % of the sodium internal to cylindrical radii 29.8 cm, 42.2 cm, and 52.3 cm respectively, result in multiplication changes of +0.24 %, +0.23 %, and 0 %, respectively. The latter value of zero is that for 40 % sodium removal from the entire core of the module.

3.2.12. Equilibrium Fuel Cycle and Reactivity Loss from Burnup

Two properties of the 1000-MW(e) system that are connected with long-term operation have been studied. The first was the equilibrium fuel composition eventually reached. The second was the change in reactivity to be expected between refuelings.

For this study the CYCLE code^{3,2} was used. CYCLE is a one-dimensional code by which is calculated the equilibrium fuel composition for a specified burnup and refueling scheme, as well as the isotopic changes and changes in reactivity that must be made to maintain criticality over the cycle. Because a single dimension is assumed, the code in cylindrical geometry cannot take into account changes in the axial-reflector savings that would occur in the actual system. On the other hand, if spherical geometry is used, the code assumed a uniform blanket all around. It is therefore necessary to estimate the characteristics of the proposed system on the basis of a series of problems with appropriately varied compositions and geometries.

In calculating the equilibrium composition, an additional restriction is that the core loading be uniform. Therefore, it was assumed that the properties of the reference design with the two enrichment zones could be estimated, assuming a single enrichment with the same volume fraction of fuel cladding, structure, and coolant.

In the calculations, the following fuel-handling scheme is assumed: The system starts with a uniform core composition in each cycle. When 2 % of the heavy atoms in the core have fissioned, a third of the fuel is reprocessed, the fission products are removed, 96 % of the heavy atoms are recovered, and enough fresh U^{238} , Pu^{239} , and boron are added to restore the core to its initial composition. Since the code computes the amount of boron burned, it is necessary to reload some

boron each cycle in an amount that must be adjusted by the user to result in an appropriate average concentration corresponding to that needed for control. The reloading is done so that the core is again uniform and the next cycle commences. No provision is made for adding or removing materials during the cycle.

Problems to determine equilibrium composition were run in spherical geometry with both the axial and radial blankets of the reference design. The equilibrium atom densities, together with those of the reference design, are shown in Table 3.11. The actual system would have densities intermediate between those obtained with each blanket. The numbers show that the isotopic composition of plutonium used in the reference design is quite close to that which the CYCLE calculations give for the equilibrium cycle.

As used, the code starts each cycle with the initially specified blanket composition. It computes the changes in isotopic composition in the blanket over the cycle. Calculations for longer burnups show that the blanket buildup is essentially linear. Table 3.12 shows atom densities of Pu^{239} and Pu^{240} at mid-cycle in various blanket regions. These densities are compared with the densities estimated for the reference design. The agreement is seen to be satisfactory.

TABLE 3.11. INITIAL ATOM DENSITIES OF URANIUM, PLUTONIUM, BORON, AND FISSION PRODUCTS IN CORE (10^{24} atoms/cm³)

	Reference Design	Equilibrium Compositions	
		With radial blanket	With axial blanket
U^{238}	0.007773	0.007572	0.007535
Pu^{239}	0.001190	0.001205	0.001288
Pu^{240}	0.000333	0.000382	0.000334
Pu^{241}	0.000047	0.000066	0.000049
Pu^{242}	0.000014	0.000017	0.000011
B (natural)	0.000223	0.000232	0.000256
Fission products	0.000289	0.000395	0.000395

TABLE 3.12. AVERAGE BUILDUP IN BLANKETS AT MID-CYCLE (10^{24} atoms/cm³)

	Pu^{239}		Pu^{240}
	Reference design	Computed	Computed
<i>Radial-Blanket Regions</i>			
Innermost (56.48-66.16 cm)	0.00031	0.00027	0.00003
Central (66.16-78.16 cm)	0.00019	0.00028	0.00001
Outermost (78.16-90.16 cm)	0.00019	0.00018	0.0
<i>Axial Blanket</i>			
One region	0.00012	0.00011	0.0

The reactivity change due to burnup during cycle is determined in a separate calculation in which a desired burnup expressed as the fraction of fissions of the heavy atoms during this cycle is specified. The burnup was taken to be 2% in the core. As with that of equilibrium composition, the reactivity change for the actual two-dimensional cylinder was estimated by using a series of one dimensional calculations. The reactivity change for the reference systems with two enrichment zones in cylindrical geometry was determined to be -2.6%. In this calculation it was assumed that the height was constant; that is, there was no change in the axial direction. A correction for axial effects was estimated by two subsidiary calculations. First, the reactivity change for a single-enrichment-zone core was calculated to be -2.8%. The reactivity changes less with burnup in the two-zone core than in the one-zone core. The equivalent one-zone core was next used in spherical geometry with the radial blanket all around. The reactivity change was -2.5%.

Calculations for spherical systems having the equilibrium concentrations were also made. For the spherical core with axial blanket all around, the change was -3.1%. With a radial blanket, it was -2.9%. The actual change for the reference system with both zones can be estimated to be between -2.5 and -2.7%. For the equilibrium composition in two zones it would be between -2.9 and -3.1%.

The burnup calculations support the original assumptions on which the reference design was based, both as to equilibrium composition and the amount of reactivity that will be lost by burnup each cycle. The later quantity also supports the estimate of average amount of boron that must be present to vary reactivity over a burnup cycle.

3.2.13. Reactivity Coefficients and Dynamic Behavior of Modules

Thermal expansion of the reactor modules can be divided into radial and axial components. Estimates of these components were made using spherical geometry (with uniform core composition) by obtaining the volume coefficient of expansion for the sphere and converting it into components of a cylinder by assuming:

$$\frac{\Delta V}{V} = \frac{\Delta h}{h} + 2 \left(\frac{\Delta r}{r} \right)_{\text{cylinder}}$$

Expansion coefficients of reactivity obtained in this way for various assumptions about the mechanism of the expansion are:

$$\frac{\Delta k}{k} = \alpha \frac{\Delta h}{h} + \beta \frac{\Delta r}{r}$$

	α	β
Axial (expanding fuel, displacing blanket)	-0.30	
Axial (contracting fuel; leaving cladding, coolant, and structure in gap)	-0.26	
Radial (contracting core radius, leaving cladding, coolant, and structure in gap)		-0.53
Radial (contracting core radius, filling gap with inner radial blanket)		-0.62

Exploratory studies on the dynamic behavior of a single module of the 1000-MW(e) system have been made for two kinds of transient conditions important to considerations of safety. These transient conditions are ramp insertions of reactivity and loss of coolant flow. Calculations were performed using a modified version (RP351X) of the FORE program.^{3,3} This cylindrical-geometry code computes the temperature distribution as a function of time across an average and the hottest fuel pins under conditions of time-varying reactivity, and, if desired, time-varying coolant velocity. The code includes feedback from the Doppler effect and from expansion of fuel, cladding, coolant, or structure.

Information was sought on the importance of the magnitude of the Doppler feedbacks and of fuel or structural expansion. Doppler coefficients appropriate for systems both with and without a beryllium region at the core-blanket interface were used. Thermal-expansion coefficients corresponding to axial expansion of fuel meat and radial expansion produced by assuming stainless-steel structure were also used. These were selected as possible sources of negative feedback for calculational purposes as examples. In practice this axial expansion of the fuel might be lowered by its confinement in the expanding V-Ti cladding. In addition, availability of fuel expansion with the high-burnup fuel remains to be demonstrated. Samples of U-5Fs alloy with burnup of about 1.3 a/o, however, demonstrated essentially the same transient expansion as fresh fuel in TREAT experiments.^{3,4}

The results shown in Table 3.13 indicate that the time before fuel melting may be increased by 20% by adding a beryllium region of sufficient thickness. Fuel temperature coefficients of reactivity corresponding to axial expansion result in a 33% increase under the same conditions. For the problems of loss of coolant flow the input reactivity was held constant. These indicate that a radial-expansion coefficient of reactivity characteristic of expansion of stainless steel contributes more feedback than does the Doppler effect, even with the beryllium region present. The modular core has the feature (of importance for metallic fuel because of the small Doppler effect available) of making possible significant expansion effects.

TABLE 3.13. SINGLE-MODULE DYNAMIC BEHAVIOR

No.	Type of Problem	$(dk/dT)_{\text{expansion}}$	$[T (dk/dT)]_{\text{Doppl.}}^{10^2}$	Fuel Melts, sec		Coolant Boils, sec		Feedback Reactivity, * \$
				Peak channel	Average channel	Peak channel	Average channel	
1	Ramp insertion \$2/sec; large Doppler Coeff		-0.47	0.44	0.52	>2.0†	>2.0†	-0.18
2	Ramp insertion \$2/sec; small Doppler Coeff		-0.12	0.36	0.45	>0.57†	>0.57†	-0.04
3	Ramp insertion \$2/sec; small Doppler Coeff and axial fuel expansion	$-8.1 \times 10^{-6}/^{\circ}\text{F}$	-0.12	0.48	0.74	1.46	1.56	-0.40
4	Ramp insertion \$.20/sec; small Doppler Coeff		-0.12	2.18	2.39	2.49	2.57	-0.10
5	Loss of coolant flow;† large Doppler Coeff		-0.47	6.25	7.40	7.60	8.16	-0.17
6	Loss of coolant flow; large Doppler Coeff and radial expansion	$-3.7 \times 10^{-6}/^{\circ}\text{F}$	-0.47	9.20	9.70	>10.0†	>10.0†	-0.58
7	Loss of coolant flow; small Doppler Coeff and radial expansion	$-3.7 \times 10^{-6}/^{\circ}\text{F}$	-0.12	8.30	8.80	9.47	9.47	-0.42

* Total feedback reactivity at time corresponding to peak channel fuel melting.

† Flow decays exponentially from 22 ft/sec for 13.2 sec to a final velocity of 0.8 ft/sec.

‡ Problem terminated at time shown before boiling occurred.

The results show that the prompt feedback available between operating temperature and fuel melting is not more than about 0.5 dollar from the combined effects of axial fuel expansion and Doppler effect. The Doppler effect is necessarily limited with metallic fuel even with the addition of the thin moderating beryllium annulus, primarily because there is only about a 20% rise in absolute temperature available between operating and melting temperatures of the fuel. For improved feedback, radial expansion introduced, for example, by a fuel cermet structure could provide an improved reactivity-feedback mechanism.

In all studies of the loss of coolant flow, initial boiling took place near the axial core boundary. Thus, initial sodium-void reactivity would be negative. Initial coolant voiding in the ramp studies also took place above the core centerline but not as close to the axial core extremity as in the case of the loss-of-flow studies. Generally, the larger Doppler coefficient of reactivity obtained with a beryllium annulus seems to provide a time to melting that is consistent with times necessary to initiate protective devices for reactivity reduction. A small Doppler coefficient, coupled with a reliable prompt axial expansion coefficient, provides similar times to melting. In all of these studies it was assumed that the modules are relatively uncoupled. Detailed study of the interaction of a malfunction in one module and its influence upon a neighboring module is a problem requiring additional study.

Potential slumping of fuel pins within the cladding

presents an area of undetermined hazard. The initial, unirradiated, fuel-pin diameter is 0.15 in., while the inside cladding diameter is 0.174 in. The large amount of effective void filled with bond sodium is required to accommodate irradiation growth. If effective slumping were to occur, the height of the fuel column in the cladding could be as much as 25% less than that for the unirradiated pin. However, only freshly charged fuel is susceptible to efficient slumping, since it is expected that swelling to fill the radial gap within the cladding will occur when only partial burnup has been attained. Furthermore, one-third of the core is replaced at each cycle; thus only a third of the core would be available for efficient slumping for roughly half of the time between successive refuelings. It is estimated that the reactivity increase would accrue gradually over a period of time that would provide an opportunity for protective measures. If detailed design were to show that less void space is required in the fuel, the slumping reactivity would be correspondingly less. It is also possible to conceive of a segmented metallic fuel element, consisting of individually supported segments within the cladding. This would significantly reduce potential slumping reactivity. Such a design eliminates the already unreliable negative axial-expansion coefficient of reactivity. This latter approach would, in this design concept, require both the beryllium annulus for enhanced Doppler reactivity and the fueled cermet for structural expansion reactivity.

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3.3 REFERENCE DESIGN

3.3.1. Neutronic Considerations

Details of the physics calculations for this reactor have been covered in Section 3.2. Concurrently with the work on these calculations, investigations were carried out on the neutronics of the reference design. The results were incorporated into the specifications listed in the data sheet in Section 3.1.

It is possible to design a metallic-fuel reactor with a neutron-energy distribution approximating that of a reactor with any other fuel by loading discrete regions of the core with materials of low molecular weight. For the reference design, however, it was decided to investigate a system that would most nearly represent the inherent neutron-energy spectral characteristics of an entirely metallic core consistent with operability. In a strong fuel with low burnup, expansion of the fuel axially and radially with increase in temperature aids in stabilizing reactivity. For the reference design, with weak fuel and high burnup, it was deemed necessary to compromise the inherent spectral type by including some low-molecular-weight scattering material to provide a significant negative Doppler coefficient of reactivity.

A beryllium zone at the core periphery was chosen as more effective than one at the core center. Heat generation is flatter over the core than it is without beryllium. Reactivity coupling between core modules may be reduced slightly. There is a compromise in total breeding, but the core breeding ratio is increased slightly. Also, the Doppler reactivity effect is in nearby U^{238} -bearing fuel and is not affected by decrease in sodium density in the central part of the core.

The minimum total breeding ratio considered to be acceptable was 1.4. An internal breeding ratio of 0.6 was considered minimal for a reasonable loss in reactivity between refueling of one-third of a core. The internal breeding ratio is defined as the number of

U^{238} and Pu^{240} absorptions per atom of Pu^{239} and Pu^{241} destroyed in the core.

The core size and length/diameter ratio were chosen as probably giving a reasonable sodium-void effect; the resulting core height of 3 ft is consistent with thermal and hydraulic parameters.

The arrangement of six cores in a ring to provide the total heat load allows each core to have its own neutron-detection equipment, which is unaffected by neutron reception from other modules. The ring array provides maximum radial catching efficiency for neutrons, with the minimal blanket loading consistent with individual core-instrument monitoring.

The control rods and associate guides allow for shutdown margin, startup, and burnup compensation with natural boron carbide. See Appendix 6.2 for details of control-rod programming and the radial restraint of bowing of fuel elements.

3.3.2. Fuel Materials

FUEL ALLOY

The alloy considered for this study is a uranium-plutonium alloy clad in vanadium-titanium tubing. Until 1964, Argonne's work on metallic fuels had been concentrated on the fissium alloy of the uranium-plutonium system. This was largely because of the program's orientation to EBR-II and the initial processing procedures used in the EBR-II Fuel Cycle Facility. This program with the fissium alloy has given an understanding of the swelling characteristics of this particular alloy, so that a conservative fuel-design approach can be taken for desired fuel-burnup levels up to at least 10%. (See Appendices 6.2.2 and 6.3.3).

Since improved processing techniques now allow other alloys to be considered, attention has turned to the titanium and zirconium alloys, which have higher melting points than those of alloys with a corresponding amount of fissium. These titanium and zirconium alloys also have more favorable neutronic characteristics than do the fissium alloys, and they are compatible with the neutronicallly favorable V-20 w/o Ti cladding alloy. Recent tests have yielded promising data for use of the zirconium alloy with either the high-nickel alloys (Hastelloy-X) or with type 304 stainless steel. There is some cost saving in using these latter materials, although studies do not indicate that the vanadium alloys are prohibitive in cost.

More data on the uranium-plutonium alloys are given in Appendix 6.3.1.

FUEL CLADDING

The cladding material is vanadium alloyed with 20% titanium. This alloy is characterized by high strength and ductility up to 650°C, low thermal-stress charac-

teristics, low penetration rates for fuel (measured up to 750°C), low corrosion rates in sodium, and low neutron absorption. Appendix 6.3.2 covers in detail the fabrication and properties of this alloy. The outside diameter of the cladding was originally set at 0.200 in. A simplified optimization of comparing total fuel-fabrication cost with plutonium working capital gives a minimum cost of electrical energy for a fuel-cladding diameter of between 0.15 and 0.25 in. The value was increased to 0.21 in. during design of the fuel element.

3.3.3. Fuel Element

For the burnup model chosen, weak fuel in strong cladding, it is postulated that no gaseous fission products escape from the fuel but exert pressure on the cladding according to the gas-law relationships of pressure, volume, and temperature; see Appendices 6.2.2. and 6.3.3.

The expanded heavy-atom density of fuel was set at 12 g/cm³ of fuel to be certain of consistency with an internal breeding ratio of 0.6 for the pin size and arrangement chosen. With the burnup model, an average core burnup of 6% was obtained.

The maximum linear heat rate was selected with due regard for the effect of expansion of fuel on its conductivity. Uncertainty in the effect of void formation on conductivity is reflected in the large difference between calculated central fuel temperature and the first melting temperature. The expanded density of 12 g/cm³ affords a realistic density margin before the condition is reached in which conductivity corresponds to fuel dispersed in fission-product gases. The selected maximum linear heat rate of 10 kW/ft is consistent with sodium and cladding temperatures.

The mixed exit sodium temperature from the reactor is 1000°F. The average temperature rise across the reactor is 280°F. The maximum cladding temperature is about 1126°F.

The V-20 w/o Ti tube, the fuel pin, 34 in. long, and axial blanket pins 13 in. long inside, comprise the main features of the fuel element, shown in Fig. 3.6. The strong-cladding concept implies axial as well as radial restraint for fuel. The fuel pin is allowed to expand under irradiation to a length of 36 in. before the chain of lower axial blanket pin, fuel pin, and upper axial blanket pin is pressed between the bottom plenum cap and the restraint plug. The sodium forming the heat-transfer bond of the fuel pin is displaced to a plenum above the upper blanket pin as the fuel swells. The plenum height is more than a third of the core height, so that extra length is available for inert gas at a final pressure that does not contribute significant stress to the cladding tube. The manner of swelling is expected to allow most of the sodium to be displaced upward.

The upper axial blanket rod is a loose fit in the tube to allow sodium to pass and to permit some subsequent swelling of the blanket uranium. The lower blanket pin in the relatively cool sodium may not swell, but displacement of a small amount of sodium that may be trapped below the fuel section is possible with the small gas pocket provided in the bottom plenum. Significant compressibility is thus provided.

The fuel pin is loaded into its cladding tube in a remotely operated assembly machine in an inert-atmosphere, shielded cell. The cladding tubes with complete lower end fittings are sodium-loaded and assembled in the hexagonal tube, but they are not fastened to their lower ends before being brought to the shielded cell. Blanket and low-decontamination fuel pins are loaded into the open tubes and are sodium bonded, for example, by a vacuum-pressure bonding technique. Each rod is raised individually for inserting the restraint tube, capping, welding, and leak testing. Because fuel-element spacing in the subassembly is by smooth tubes, any element failing the test can be replaced with another one or with an inert rod. The final assembly step is to pull each pin down to the subassembly grid by the wire extension of the pop rivet pin and to expand the fuel-element tip radially below the grid by the upsetting action of the pop rivet pin head; the expansion is suitably limited and shaped by a rivet fixture.

Other topics related to the fuel-element concept are: physical properties of V-20 w/o Ti—Appendix 6.3.2 and design approximations for fuel-burnup model—Appendix 6.3.3.

3.3.4. Fuel Subassemblies

A primary factor in considering how to organize fuel pins into shrouded bundles and suitable in-core confinement is the acceptably safe fraction of the core handled at one time. The upper limit was chosen to be 2%, which would require a core loading of at least 50 subassemblies. The minimum loading in a uniform hexagonal core that meets this criterion is 61 subassemblies, or 1.64% of the core loading per subassembly. Symmetry of the array allows the thin beryllium zone at the core periphery to be placed so that its postulated neutronic effect can be rather closely approximated in practice.

The manner of placing the beryllium in subassemblies has become involved in the number of fuel pins in an average subassembly. To preserve this average figure at the original number, 331, after reducing the core volume from 840 to 780 liters, the beryllium region was moved inward to straddle the saw-tooth boundary between the core and the inner radial blanket. Triangular pieces occupy parts of subassemblies at the boundary (Fig. 3.7).

FIG. 3.6. Fuel Element.

FIG. 3.7. Horizontal Section of Reactor at Midplane of Core Modules.

The key structure of a subassembly is the hexagonal shroud tube. The coefficient of expansion of the V-20 w/o Ti alloy is about two-thirds of the coefficient for AISI type 300 series stainless steel. It was, therefore, decided to use 17-4 PH steel as material for the hexagonal subassembly tube because its coefficient closely matches that of the vanadium alloy over the temperature range of interest and is apparently independent of the metallurgical history of the steel.

If all pins in a subassembly would undergo equal heat generation, the average burnup achieved in the subassembly would be the maximum allowable per pin. Similarly, the heat removal would not be limited by an edge row of pins at a higher heat-generation rate. For the particular arrangement of pin spacers, there would not be hotter sodium along one flat of the hexagonal tube than along its parallel flat on the other side to give tube bowing. If, in addition, all the subassemblies in a module had the same heat generation, the core module would produce maximum power.

These goals are approached closely in the radial direction in the reference design by the combination of two-zone enrichment and beryllium at the core periphery. The steep outward gradient of fission source in the outer and higher-enrichment zone is cancelled over the zone thickness by the steep inward gradient associated with neutrons returned from the beryllium. The heat generation at the outer edge of the inner zone is raised relative to the centerline value if the combination of outer-zone enrichment and beryllium thickness produces a high generation rate. The outer-zone heat rate was selected to be below the centerline value to minimize difference in enrichment. The effect of high heat generation in the central region can be partially overcome by overcooling, since the relatively small volume of discharged coolant does not significantly lower the mixed exit temperature. Since the control-rod positions are in the inner enrichment zone, there is only one kind of outer-enrichment assembly.

Each fuel subassembly consists of three sections: the hexagonal shroud tube containing fuel elements, the lower end section, and the shield pole. The lower end section supports the assembly, provides surfaces for radial and circumferential location, provides plenum seals and hydraulic holddown area, and provides for flow into the subassembly without inhibiting flow distribution throughout the high-pressure plenum. The shield pole locates and orients a top neutron shield piece supported by each subassembly and provides the subassembly tip for engagement of the refueling mechanism.

REGULAR SUBASSEMBLY

The 331 fuel elements of a regular subassembly (Fig. 3.8) are spaced by complete-circle tubes at every

other common center of three elements. This concept for minimum pressure drop is believed to have been first published by Sampson and Luebke.^{3,5} These spacer tubes are held apart during assembly of fuel-element tubes in the hexagonal tube by wires similar to those described in Ref. 3.6. Larger tubes hold the elements away from the wall of the hexagonal tube. The estimated clearance of about 0.010 in. between fuel-element assemblage and hexagonal wall is probably decreased slightly in going from the temperature of manufacture to the temperature level of operation, because of the difference in expansion characteristics of V-20 w/o Ti fuel-element tubes, type 304 stainless-steel spacer tubes, and 17-4 PH hexagonal tubes.

Minimum clearances between the fuel-element assemblage and hexagonal tube are determined under conditions of full pump flow by the outward bowing of the shroud tube flats. The estimated total deflection of parallel flats, which is 0.020 in. for the 0.080-in.-thick wall, occurs near the bottom of the hexagonal tube, where the pressure differential is about 70 psi. The fuel elements as an assemblage are expected to move with the hexagonal tube, owing to restraint at the inside of the corners of the hexagonal tube, so that radial expansion of the core occurs according to expansion of these shroud tubes. Their expansion as a system is defined by bosses at the hexagon corners; their vertical location is near the core midplane. The bosses are a nominal 0.020 in. high, so that 0.040 in. between adjacent flats allows the estimated 0.020-in. deflection of the flats, due to pumping action, without making the core radius sensitive to this action.

The fuel elements are held down to oppose the force of pressure drop by pins passing through and upset underneath a grid structure, as indicated in the prior discussion of fuel elements. The grid structure is made up from stainless-steel strips stamped to a semihexagonal form, held back to back and spot-welded. Every other hexagon so formed is rounded to receive a fuel-element pin. Thin-section hexagonal rings above and below the grid complete the features of the hexagonal tube section of the regular subassembly.

The lower end section of the fuel assembly is made of type 300 stainless steel. Its hexagonal top part is a few thousandths of an inch smaller across flats than the inside dimension of the 17-4 PH hexagonal tube. These two parts are joined at the center of each flat by arc spot-welding while they are accurately aligned in the welding jig. The welds often produce slightly depressed areas, but the region may be flush or as high as 0.020 in.

The transition from a hexagonal to a round shape is made to achieve a large outside diameter of the round that is consistent with reasonable ligament strength of the plenum-support plates. Thus, a large area hydraulic holddown is achieved by using the pressure

Fig. 3.8. Details of Regular Fuel Subassembly.

difference between the high-pressure plenum and low-pressure plenums below it. Additional holddown force is obtained by utilizing the net area between the inside area of the hexagonal tube and the outside area of the round. The pressure in the region outside the assemblies is low because of the very low flow rate achieved by the close fit at operating temperatures between the round section and the titanium pivot plate on top of the plenum-support structure.

The large round-tube part of the lower end section terminates in the high-pressure plenum. Coolant flows into this part through the end and through suitably shaped slots in the wall. These slots are covered to varying degrees, as determined by experiment, by skirts attached to the underside of the upper plate of the plenum to assist in flow distribution to the various fuel-subassembly positions. The spider transition from the tube to the small-diameter rod, which allows end flow into the subassembly, contains a section, 3 in. long, of 0.11-in.-diameter wires set in a staggered pattern to develop pressure drop for suitable distribution between end flow and slot flow. The solid rod between the spider transition and the seal plug has a length and a diameter determined by the requirement for flow distribution in the high-pressure plenum.

The seal plug of the lower end section is a round tube of two sections with different outside diameters. The upper portion with the larger diameter is joined to the solid rod by a conical section, which acts also as a seal and serves to prevent interference with removal of the subassembly. The conical transition between the sections of different diameters provides the seat and seal for supporting the fuel subassembly and making the hydraulic holddown effective. A line engagement on the lower plate of the high-pressure plenum appears feasible, since the contact will be made only 30 or 40 times during plant life. The diametral clearance between tube and plate is a nominal 0.006 in. for $\frac{3}{4}$ in. of height just below the seat and seal to provide additional radial restraint during differential movement associated with the scheme to keep the subassemblies locked together radially. For the remaining engagement of these two parts the clearance is, say, $\frac{1}{8}$ in. The end of this bottom tube is tapered inward to guide the subassembly. It is also slotted at six places corresponding to acceptable circumferential orientation of each regular subassembly. These slots engage vertical plates set in the lower plate of the high-pressure plenum; the engagement for the last half inch of travel is a nominal 0.004 in. to allow proper locking of the subassemblies of a core. The bottom of the subassembly extends 5 in. below the orientation plates so that failure to engage is readily discernible with visual aids at the top of the subassembly.

A shaft, 2 in. in diameter, extends from the top or the hexagonal section to the end feature for engaging the refueling mechanism. This shaft consists of a tube and central plug composed of layers of shielding material; a stainless steel region about 3 in. in diameter might allow sufficient fast-neutron streaming to vitiate the performance of the hexagonal shield pieces centered on each $2\frac{1}{2}$ -in. shaft. The discharge adapter connecting the shaft to the hexagonal-tube section of the subassembly also supports and circumferentially locates the hexagonal shield pieces. The vertical plates of the adapter are shaped to permit radial flow of the hot sodium discharged from each subassembly. The bottom part of the adapter contains a grid for limiting any upward movement of the tubes that space the fuel elements. The shield pole is joined to the hexagonal section by the same engagement means as that used for joining this section to the lower end piece.

FUEL SUBASSEMBLIES IN CONTROL-ROD POSITIONS

The control-rod pattern consists of the central position and two rings of positions, each for six rods. Thus, there are 13 rods per core module or a total of 78. Each rod is guided in a round tube, which is in turn set in a hexagonal tube centered in the subassemblies. Replacement of these assemblies entails removal of the corresponding control rod. The minimum reactivity shutdown margin during refueling is estimated to be about $2\Delta k/k$. The arrangement for rod cooling prevents expulsion of uncoupled control rods due to a pressure drop in flow of fuel coolant.

The 270 fuel elements of a subassembly located in a control-rod position are spaced, loaded, tested, and held down to a grid plate as are those of a regular subassembly. See Figs. 3.9 and 3.10. (In Fig. 3.10, the shield-pole and hexagonal-tube sections of the fuel subassemblies in control-rod positions are designated I and II respectively. The fuel assembly in the core-boundary position is designated III.) The fuel-element spacing is preserved by a central hexagonal tube having small external raised areas, which might be designated as a hobnail pattern. The round control-rod guide tube inside the hexagonal tube rests on the closed bottom end of the latter. Slots in the bottom part of the guide tube establish a flow path between the annulus of the control rod and its guide tube and the six small gaps between the guide and hexagonal tubes. A small downward flow of hot sodium past the control is achieved by a modified venturi effect at the top of the hexagon-tube section of the subassembly; sodium discharged from the fuel-element spaces is conducted into an annulus of a smaller cross section, with suitable holes in the inner wall that communicate with the six gaps between the guide and the small hexagonal tubes. Flow into

Fig. 3.9. Cross Sections of Fuel Subassemblies.

FIG. 3.10. Details of Control and Core-boundary Fuel Subassemblies.

the control-rod annulus is at the top of the shield pole, which is an extension of the guide tube. The top of the control rod is larger in diameter to act as the free-fall limit for the rod; the guide tube has holes near the top to allow sodium to enter the annulus. The small downward flow of sodium is considered to be primarily for purging. Most of the cooling of the control rod is expected to be by conduction to sodium flowing past the fuel elements.

The pressure drop associated with the venturi effect at the top of the hexagonal section is utilized to balance flow among the subassemblies, since the control-rod-position assemblies require 80% of the regular-assembly flow on a uniform heat-generation basis.

CORE-BOUNDARY FUEL SUBASSEMBLY

The 276 fuel elements of a core-boundary subassembly (Fig. 3.9 and 3.10, III in Fig. 3.10) are loaded and secured as in a regular subassembly. The stainless-steel cladding of the triangular beryllium piece has hobnail patterns to preserve fuel spacing and to provide space between the cladding of the hexagonal tube. Sodium flow in the latter gaps is restricted so as to keep temperature differences in the hexagonal-tube wall at a maximum of the order of 50°F at any cross section to limit bowing. A small flow of sodium in contact with the beryllium ensures proper thermal conductance.

The bottom tube of the lower end section has only one vertical slot to engage a vertical plate set in the lower plate of the high-pressure plenum. The vertical plate is set to orient the core-boundary fuel subassembly with the beryllium piece facing the beryllium of the inner blanket subassembly. The restriction on orientation also ensures that this fuel assembly with its higher enrichment will not be placed in a regular fuel-assembly position.

Other topics related to the fuel-subassembly concept are: plenum-support structure, Section 3.3.7; and special topics in structural design, Appendix 6.2.

3.3.5. Blanket

AXIAL BLANKET

The uranium metal pieces of the axial blankets are placed in the same tube as the fuel pin. The sodium bond displaced by pin swelling passes the upper blanket pin to a plenum above. The weak-fuel concept is employed in determining allowance for burnup of the axial-blanket pieces. With this allowance and with the close spacing of fuel elements, the blanket thickness of 13 in. is roughly 50% more uranium atoms per unit of reactor surface than the 1000-MW(e) designs with ceramic fuel do.

RADIAL BLANKET

The two kinds of radial blanket subassemblies, inner and outer, are in three vertical sections corresponding to those of the fuel subassemblies. The hexagonal-tube section is located vertically so that the sodium discharge is at the same level as that for the core. Above this level are shield-pole and hexagonal-shield pieces, which have the same details as those previously discussed. The main differences in concept between blanket and fuel assemblies are in the size and spacing of elements and in the lower end sections.

The radial blanket may be considered to be divided into three circumferential segments according to neutron performance. About a third of each module's blanket is common with those of adjacent modules; the neutron gradient is zero. Another one-third approximately catches neutrons from adjacent modules so that the effective thickness is more than the blanket thickness assigned to a module. The final third is two subassemblies thick; beryllium is used to increase its effectiveness. The height of the radial blanket is close to 4 ft; a detailed study might show that a greater thickness for the one-third between modules is optimum.

Radially, the blanket is divided into two zones. The inner zone contains half of the beryllium at the core periphery; it has pins of a diameter appropriate to maximum plutonium buildup according to one-sixth replacement at each one-third of a core refuelling. One-ninth of the outer blanket is replaced on the same schedule. Outer blanket subassemblies are moved inward (for the circumferential two-thirds not between modules) to limit residence time to no more than 5 years and to reduce plutonium inventory.

The uranium pins in V-20 w/o Ti cladding are designed for swelling according to their degree of burnup. The elements are the same length as fuel elements. A restraining pin is used in the upper part to serve as neutron reflector and to limit axial uranium swelling.

The 276 uranium elements of the inner radial blanket subassembly are (Figs. 3.11 and 3.12) spaced by spiral wires to cause mixing of the coolant. Because heat generation varies widely over the loading life of the subassembly as well as over the cross section, better thermal performance is achieved by mixing. The beryllium pieces are identical with those of the core-boundary fuel subassembly, but the cladding has no hobnail pattern on the side adjacent to the uranium elements.

The lower end section is attached to the hexagonal tube by arc spotwelding. The diameter of the tubular section below the transition from the hexagonal tube is larger than that for fuel subassemblies so that no

FIG. 3.11. Radial Uranium Blanket Elements and Subassembly.

OUTER RADIAL BLANKET SUBASSEMBLY

FIG. 3.12. Details of Radial Blanket Subassembly.

blanket subassembly will seat in the core without its being noticed. Similarly, the fuel-assembly top will project above the other tops if this assembly is placed in the blanket region, since its lower end section is much longer than the height available in the blanket region.

This part of the blanket has slots to allow coolant to enter from the low-pressure plenum. Below the slotted wall is the solid section required for hydraulic holddown. The under side is round to allow rotation of the subassembly centerline above the pivot plate and to allow maximum area at low pressure. The support plug on which the subassembly rests is shown in Fig. 3.13. Pressure loss at reasonable leakage flow is obtained by the fit of the subassembly end cylinder between the support plug and the structural tube. This tube conducts leakage flow to the low-pressure region below the high-pressure region.

An orientation pin set in the driver-plate ring insert matches a vertical slot in the subassembly end cylinder to ensure that the beryllium part faces the core. The diameter of the pin is larger than the slots in the outer blanket subassemblies so that these subassemblies cannot be placed adjacent to the core without being noticed.

The 61 uranium elements of the outer radial blanket subassembly (Figs. 3.11 and 3.12) are also spaced by spiral wires to enhance coolant mixing. A V-20 w/o Ti cladding is used because of the long residence time of the uranium in this part of a core module. The two rows of this region are utilized to allow subassemblies to be moved from the module outer edge to the position adjacent to the inner blanket subassembly. Short cylindrical skirts attached beneath the upper plate of the low-pressure plenum balance flow so that the blanket subassemblies may be moved from positions of lower heat generation to those of higher heat generation without the need for pumping excessive quantities of coolant.

3.3.6. Module Arrangement

Figure 3.13 shows the arrangement of subassemblies in the core modules and the arrangement of the modules.

There are 37 lower-enrichment subassemblies comprising the inner core region. Twenty four are the regular kind and 13 are for control-rod positions. Center-to-center spacing of the rods is 9 in.

The 24 higher-enrichment subassemblies and the 30 inner-blanket subassemblies are oriented so that their beryllium pieces form a uniform, thin moderator region, except at the hexagonal corners of the core. The corner fuel subassemblies are at the maximum distance from the core center, but they have some extra heat generation due to the adjacent blanket-subassembly beryllium

pieces; however, the region formed by these tapers to an edge. In the thermal analysis, core-corner effects were assumed to compensate each other to within the accuracy sufficient for the study.

The outer radial-blanket subassemblies, which are parallel to four of the six flats of the hexagonal shape formed by the beryllium regions, are in two rows of 26 subassemblies each. In these four regions blanket material is shifted toward the core, to decrease residence time and plutonium inventory. The other two regions of five blanket subassemblies each are in single rows and receive neutrons from two cores to the greatest degree.

The remaining subassembly positions of a module consist of a row of beryllium and two rows of stainless steel. Two instrument subassemblies are located in the inner row of stainless-steel units and one in the outer row. An additional radial region common to all modules serves as a combination neutron and thermal shield.

The six core modules are arranged in a circle with radial centerlines forming the flats of a central hexagon. For this centerline arrangement the cracks in the hexagonal pattern that define modules divide the outer-blanket subassemblies symmetrically. Thus, each module has a row of outer-blanket subassemblies between modules. The gaps between modules are filled with sodium to allow expansion and contraction of subassemblies in each module without coupling of radial movements between modules.

The lengths of beryllium pieces and uranium blanket pins are symmetrical about the midplane of the fuel pins. All vanadium-titanium tubes are of the same length. Triangular pieces of stainless steel are placed above and below the beryllium pieces to occupy the full height corresponding to the lengths of fuel and blanket elements. The hexagonal-tube sections of the subassemblies are all the same length and at the same vertical position. Thus the entire reactor of six modules has a common level for discharge of hot sodium from the hexagonal sections of the subassemblies.

3.3.7. Plenum-support Structure

The plenum-support structure consists of three type 304 stainless-steel circles 20 ft in diameter spaced apart to form two regions of coolant flow: a high-pressure plenum below and a lower-pressure plenum above the middle plate (Fig. 3.14). Sodium from the common annular region of the reactor tank enters the high-pressure plenum of each module and flows inward to the region of core-subassembly inlet openings. Some sodium bypasses these openings through holes in the middle plate and thus flows into the lower-pressure plenum to provide blanket coolant. A small flow of sodium passes through openings in the bottom plate to a low-

NOTE ALL DIMENSIONS IN INCHES

- A LOCATION OF HEXAGONAL SHIELD PIECES
- B COOLANT DISCHARGE PLENUM
- C UPPER AXIAL BLANKET
- D EXPANDED CORE HEIGHT
- E LOWER AXIAL BLANKET
- F Ti PIVOT PLATE
- G LOW PRESSURE PLENUM
- H SST DRIVER PLATE
- I HIGH PRESSURE PLENUM

FIG. 3.13. Arrangement of Subassemblies and Core Modules.

FIG. 3.14. Plenum-support Structure.

pressure region, flows downward to cool the bottom neutron shield, and then flows outward and upward through tubes to cool the radial neutron shield. Another small flow of sodium enters the regions outside of subassemblies from holes in the top plate.

The bottom plate is reinforced from below by vertical ribbing to permit the plate to carry the weight of the subassemblies and part of the weight of the plenum-support structure, with a maximum deflection that is probably determined more by allowable stresses than by subassembly centerline tilt. See Fig. 3.14. Each module has its own movements for radial locking of subassemblies. The ribbing supports do not contact the lower plate over the $3\frac{1}{2}$ -ft diameter below each core, so that space is allowed for the subassembly-orientation plate.

The bottom and middle plates are spaced and held to resist internal pressure chiefly by thick-walled tubes at each location for blanket subassembly and reflector unit. These tubes terminate at the upper plenum, since they assist hydraulic holddown for blanket subassemblies. The top plate is held by tie bolts to the middle plate; these bolts are assumed to be uniformly located at the common center of every three subassembly lower end sections.

Each plate has 918 holes corresponding to all fuel- and blanket-subassembly positions. Another 462 holes match the number of outer beryllium, stainless-steel-reflector, and instrument-tube positions. Additional penetrations in all three plates are for tubes to conduct sodium from the low-pressure region to the neutron and thermal shield. The various thicknesses of the major plates of the plenum-support structure would be determined as part of a more detailed design effort.

At each of the six module positions are two plates for controlling the radial location of subassemblies (Fig. 3.14). A titanium pivot plate, about an inch thick, rests on the top major plate; a type 304 stainless-steel driver of $1\frac{1}{2}$ in. thickness rests on top of the middle major plate. These two plates are accurately positioned with respect to each other and have accurately manufactured hole patterns for contacting surfaces on the lower end sections of subassemblies. The exact relationship of the hole pattern in the pivot plate to that in the driver plate to be specified by design depends on several factors, which include the temperature of refueling, the fits and tolerances of subassembly and hole diameters, and the vertical location of bosses defining limited areas of contact among subassemblies.

The hole patterns in the two plates are manufactured so that they almost match at some temperature below the operating level. Near this temperature the subassemblies have maximum clearance for refueling. As the system is heated, the differential expansion of the

titanium and stainless-steel plates closes the net gaps and then tilts the subassembly centerline toward the centerline of the core. Further heating develops radial forces at the bosses. The bosses on fuel subassemblies are located so that the core is locked near its midplane. The bosses on blanket subassemblies are located higher, so that they contact fuel subassemblies near the top of their hexagonal sections to correct for flexure due to core midplane contact and for thermal bowing.

An example is worked out in detail in Appendix 6.2.4 for one radial line of subassembly positions. A temperature of 420°F was arbitrarily chosen for matching hole patterns in the two plates. For the fits and tolerances chosen the average condition gives positive touching of core subassemblies at 570°F . At about this temperature the six cores could safely be started and brought to sufficient power for heating the massive structures at the limiting rate. At full operating power the temperature rise of the coolant to the core midplane expands the core. Concentrated load reaction corresponding to maximum beam deflection at this level is estimated to be 278 pounds. The maximum fiber stresses are approximately 7500 psi. The maximum deviation from a centerline for a subassembly in a control position is approximately .045 in. The corresponding net radial clearance of the control subassembly is 0.060 in.

In the example the bowing action of fuel subassemblies was estimated for the maximum heat-generation gradient across a subassembly without mixing of the coolant; the maximum occurs in a row with control-rod positions. Bowing of blanket subassemblies was assumed to be exactly compensated by inward bowing of the subassembly with beryllium and by outward bowing of the outer blanket subassembly. The degree of coolant mixing that might be achieved in blanket subassemblies is not known.

Some conclusions based on the study of the differential-expansion plate arrangement for radial locking of a core are: (1) the inwardly directed forces from the full circular array of subassemblies are probably associated with average clearance conditions. (2) There is no effect on these forces or on deflections of subassembly hexagons due to small radial or circumferential misalignments of pivot and driver plates; a radial tolerance with respect to a plumbline merely tilts a whole core assembly; a circumferential tolerance results in a slight twist to the pattern of vertical centerlines. (3) The temperature of 400°F chosen for refueling is probably too low even for the concept of reactor tank and separate primary loop because of the energy required to heat the system up to the initial touching temperature of the core.

Fuel subassemblies are oriented by short vertical plates set in the orientation plate under the bottom

plate; it may be advantageous to set these as semicircular keys.

The under sides of the middle and top plates have short circular skirts of suitable length to distribute inlet flow: those under the middle plate for the core, those under the top plate for the blanket. Thus all six surfaces of the three major plates of the plenum support structure have appurtenances that refine position or flow distribution.

3.3.8. Reactor Tank and Top Shield

The reactor tank has a nominal diameter of 20 ft and a height of 30 ft. It is supported by a flange resting on an offset in the top shielding opening. See Fig. 3.15. The load wall is the cylinder joined to the flange at its top edge and joined to the middle plate of the plenum-support structure at its lower edge. All loads for the reactor subassembly, reflector unit, and neutron shield are carried to the middle plate and transmitted through the joint to the load wall. The total load is estimated to about 500 tons.

The lower portion of the load wall is surrounded by another cylinder to form the annular plenum for inlet-sodium flow. The bottom plate of the plenum-support structure is attached to this outer wall to complete the inlet-flow system. The ellipsoidal bottom completes a chamber containing suitable neutron and thermal shield and coolant by-pass flow; the exiting-flow path is upward through tubes in the plenum-support structure and then through suitable passages in the radial thermal shield.

Above this thermal shield is another cylindrical wall suspended from a 12-in.-thick cylinder with a flange resting on the load-wall flange. This inner wall is in contact with the hot exiting sodium and takes the shock of a reactor scram. The sodium level provides submerged exit flow of the sodium into the lines to the sodium-sodium heat exchangers. The lines float inside of other pipes that make the structural connections between the load wall and heat-exchanger walls.

The section of the inner wall above the highest sodium level is about 12 in. thick, to provide space for tubes extending down to the 18 positions for neutronic instrumentation; instrument leads are not disturbed by preparations for refueling. The tubes are considered to be double-walled to provide an annular region for flow of cool sodium from the lower pressure plenum to the 12-in.-thick section; at this level, holes communicate to the 20-ft-diameter annulus formed by the inner wall and the load wall. Cooling of instruments by inert gas is probably necessary.

The 12-in.-thick wall is also used to provide space for rods connected to thermal and neutron-shield units to hold them close to the load wall and to lift them prior to suitable engagement for replacement if necessary.

The inside diameter of the 12-in. thick inner wall was chosen to be compatible with the diameter of the large top shield plug.

The rotating shield plug has a degree of rotation and access ports so that vertical replacement can be made of all fuel, outer beryllium, and outer reflector pieces. The central area of the large plug is available for small openings for insertion of viewing equipment to be used during fuel transfer and control-rod coupling.

The top shield is 8 ft thick. The shield and plug are designed to afford access to the region over the reactor during operation without radiation limit. The plug for the fuel-transfer opening has one set of control drives, located about centerline. The other five sets are appropriately spaced on the top shield plug.

3.3.9. Refueling

All modules of the reactor are shut down for refueling. Pumping and cooling are continued to bring all parts of the reactor system down to the temperature level for which the pivot and driver plates are designed to have matching hole-centerline patterns. A cooling regime established for removing fission-product heat maintains this level.

The next essential preparation for refueling emphasized is disengaging the control-rod drive. To assist in this operation, each control-rod-drive extension to the control-rod top has a hole, $\frac{1}{2}$ in. in diameter, housing a rod to decouple the control rod and extension. These decoupling rods are grouped to a ring suitably guided for powered vertical movement.

The procedure for uncoupling control rods is as follows: The grouped decoupling rods are driven down onto spring-loaded pins in the control-rod tops to disengage the rods from their extensions. Each control-rod extension is individually driven upward 1 ft. The decoupling rods are raised 1 ft. Two radial arms located near the middle of this 1-ft movement each sweep out about 150 degree arcs if all decoupling and vertical movements have been performed. During reactor operation, these radial arms are cooled by contact with sodium that is flowing out. Interlock prohibition of plug removal must be removed so that plug rotation is possible.

A partial listing of choices made for establishing a fuel-handling procedure is:

1. Each subassembly is located with visual aids.
2. The amount of manipulation under sodium is minimal.
3. Spent fuel is transferred in a sodium-filled container.
4. Fuel is transferred to storage in a shielded cell over the reactor and adjacent vault below the floor level.

FIG. 3.15. Reactor Tank and Plugs.

FIG. 3.16. Vertical Section of Reactor and One of Six Primary Sodium Loops.

5. Each subassembly exchange is completed before any other subassembly position is disturbed.

The outline of fuel-transfer procedure begins with the assumption that the periscopes or other visual aids are in place, that the control-disengagement routine is finished, and that the small shield plug is lifted to a storage position. Since each module is to have a third of a core replaced at each refueling time, the large shield is already in position. The fuel-transfer machine is fitted to the opening so that the machine is properly aligned with the subassembly coordinates.

The first action involving a subassembly is the removal of the hexagonal neutron-shield piece from the subassembly chosen for replacement; this shield piece is stored above for reuse at the same position. A tube sealed at the top end is lowered along the subassembly centerline until it dips below the sodium surface. Sodium is sucked up in the tube to the height of, for example, 12 ft. A shaft inside of the tube engages the top of the shield pole of the subassembly and pulls the subassembly up in the tube. A valve arrangement at the tube bottom is closed and the tube is lifted just out of the sodium to determine that a seal has been made. Volume for the seal feature is provided from six adjacent subassembly positions, since the neutron-shield pieces have only tubular extensions 3 in. in diameter, in the vicinity of the sodium surface. Provision is made during transfer for natural-circulation cooling by an extended heat-removal surface attached to the outside of the transfer tube.

It is proposed that the replacement subassembly be handled by a bare transfer shaft so that orientation is set by a combination of loading-fixture restraint and observation.

Because of height limitations, the subassembly with a control rod is taken to the storage area before removing the control rod. The new subassembly is loaded with the unburned control rod in the storage area. A neutron-transmission check is made on new control rods to establish their identity as control rods.

3.3.10. Primary Sodium Circuit

It is proposed that the primary sodium circuit (Fig. 3.16) consist of: (1) a reactor inlet-flow plenum common to all six core modules; (2) a common discharge into the reactor tank region above the fuel subassemblies; and (3) six loops joined to these two plena. Each loop has: a vertical exchanger receiving sodium at 1000°F directly from the reactor; a vertical, free-surface, variable-speed, centrifugal pump; and a check valve. These pieces of equipment are completely joined by piping.

The hot sodium rising from the modules discharges at a common level from the subassembly hexagonal tubes. Radial flow with respect to the module centerline

is promoted so that uniform upward flow through the upper neutron shield over the whole module is approached. Some flow will angle directly to the discharge shroud between the outer blankets of a pair of modules, but the main flow will be to the upper few inches of the sodium in the reactor tank. The main flow will then be radial with respect to the reactor tank centerline until it reaches the region near the tank periphery. Here the flow will develop into streams leading to the discharge lines. These lines are far enough below the operating level of the sodium, and they are large enough, to prevent entrainment of cover gas. The pressure drop in the line to the heat exchanger is about 2 ft of sodium so that at operating level the sodium submerges the ends of the fuel assemblies about 9 in. and thus flows radially in the reactor tank, with the only restrictions being the control-rod shafts. Cover-gas pressure is the same in the reactor and the heat exchangers.

The combined free surface of hot sodium in the six heat exchangers—8 ft for each exchanger—is made almost equal to that in the reactor tank—20 ft in diameter. At pump shutdown the two surfaces equalize and thus expose the ends of the subassemblies for reloading operations with visual aids.

In each heat exchanger the primary sodium flows inward around the outside of tubes in an annular bundle. The inlet flow is submerged to prevent gas entrainment. Near the bottom of the tube bundle the primary sodium is directed outward by a conical baffle into a small reservoir. Thus, the bottom of the heat exchanger is in essence a tank that prevents any residual bubbles from reaching the pump suction.

Sodium pressure at the pump inlet is made to match pump requirements by a combination of low pressure drop in the heat exchanger, submergence, and cover-gas pressure. Reasonable values for these are:

Primary sodium ΔP in heat exchanger	— 20 ft
Submergence	10 ft
Cover-gas pressure	40 ft
Net	30 ft

3.4 INTERMEDIATE SODIUM SYSTEM AND STEAM-RAISING UNITS

Only a small amount of effort was expended in defining this portion of the plant. The main purpose of this preliminary work was to choose approximate sodium conditions and flows in order to match up the nuclear power source with the electrical-generating equipment. Energy is transferred from the primary sodium to the intermediate sodium through six heat exchangers. The inlet temperature of the intermediate sodium is 665°F and the outlet temperature is 960°F. The outlet sodium from the intermediate heat ex-

$$\text{GROSS TURBINE HEAT RATE} = \frac{7,845,778(1416.8 - 448) + 7,242,152(1467.5 - 1298.3)}{1,094,165 \text{ KW}} = 8067 \text{ BTU/KWH}$$

FIG. 3.17. Gross Unit Heat Balance for Stream-Electric Plant.

changers flows to the superheater-evaporator-economizer unit or to the parallel reheater unit, and then back to the intermediate sodium-sodium heat exchanger. A flow-control bypass around this exchanger controls steam conditions at partial loads. Six variable-speed mechanical pumps having a combined flow of about 220,000 gal./min with a 30 psi developed head are part of the system.

Very little work was carried out to define or optimize these systems; however, scouting calculations, based on estimated heat-transfer coefficients and costs per unit surface area for the sodium-sodium heat exchanger and steam-raising units were made. These calculations indicated that an increase in the temperature of the primary sodium by 50°F, i.e., 770 inlet and 1050°F outlet, causes a meaningful decrease in the capital cost of these units if no significant additional problems with materials or stress are encountered. There appears to be much capital charged to the intermediate sodium system, steam-raising units, and associated building and auxiliary equipment. The definition of these costs and knowledge of the influence of various parameters on them are necessary to arrive at a properly balanced power plant. Further work in defining the technical aspects and related economics of this portion of the plant should be carried out.

3.5 TURBINE-GENERATOR PLANT

As mentioned in previous sections, the selection of the steam conditions below those of modern practice was made in an attempt to suitably accommodate opposing design interests. Steam conditions are 1800 psig and 900°F; reheat to 900°F is used. Characteristics of the steam plant are:

Primary steam pressure, psig	1800
Primary steam temperature, °F	900
Reheat steam pressure, psia	445
Reheat steam temperature, °F	900
Feedwater temperature, °F	465
Stages of feedwater heating	7
Nominal unit description	500 MW—TC4F
Gross turbine heat rate Btu/kw-hr	8067
Number of turbine generator units	2
Gross turbine output, MW	1063
Net electrical generation (plant), MW	1000

A "gross turbine preliminary heat balance" is shown in Fig. 3.17. While the numbers given in this balance are not explicitly for the unit described, they are sufficiently close to represent the generating unit. The detailed information pertaining to these units may be obtained in the United Engineer's study reported in Ref. 3.7. Two turbine-generators are used for the power-developing units, as indicated in the previous study, although manufacturing capability now exists for a single unit.

The boiler feed pumps are driven by the main turbine. The total output of the generators and the boiler feed pumps is 1063 MW(e). A breakdown of the in-house loads in MW(e) is approximately as follows:

Boiler feed pumps	23
Sodium pumping	17
Plant auxiliaries	23

Calculations based on a previous study of large steam-electric generating units, which were carried out by United Engineers and Constructors for ANL,^{3,7} show the steam-electric plant for the 1800 psig-900°F/900°F steam cycle might have a capital cost 5 to 10% higher than that of an installation using a more modern cycle of 2400 psig-1000/1000°F; however, with a single turbine-generator unit, the 1800 psig condition would not have as great a cost differential as indicated in that study. Thus, the capital-cost penalty on the electrical-generating portion of the plant for a 1000°F sodium outlet for the reactor system is not large.

Additional knowledge of fuel behavior at higher temperature and the cost of the steam-raising units may allow the selection of a steam cycle of higher temperature and pressure within an overall plant optimization.

REFERENCES

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- 3.6. *Liquid Metal Fast Breeder Reactor Design Study*, GEAP-4418 (January, 1963).
- 3.7. *Engineering Study for Large Steam-Electric Unit*, work performed under ANL Contract No. 31-109-38-1717 with United Engineers and Constructors, Inc., TID-23257 (November, 1964).

4.0. Fuel-cycle Facility

The fuel-cycle facility consists of the onsite components of the power station that are employed in processing discharged fuel, fabricating processed fuel into fuel elements, and disposing of wastes. The design of the conceptual plant to process and fabricate discharged fuel is based on that of the EBR-II Fuel Cycle Facility, which is close-coupled to the EBR-II reactor and employs pyrochemical procedures to process and fabricate fuel. Detailed descriptions of the EBR-II Fuel Cycle Facility have been published.^{4.1, 4.2}

The EBR-II Fuel Cycle Facility, utilizing a compact pyrochemical process and completely remote fabrication techniques, has been successfully handling fuel from the reactor only 10–14 days after its discharge. Since Sept. 2, 1964, fuel has been recovered from seven irradiated subassemblies by melt refining, and a fuel assembly composed of processed fuel has been remotely fabricated to meet initial fuel specifications. This subassembly is back in the reactor core for additional burnup. All equipment has been determined to be operable and, after the usual experience of correcting initial malfunctions and making newly conceived improvements, operations are proceeding in a relatively straightforward fashion to determine capabilities and limitations of the processes and equipment.

4.1. CAPACITY REQUIREMENT

The specifications of the reference reactor pertinent to the fuel-cycle throughput requirements appear in Table 4.1. Reactor shutdowns for refueling are at 206-day intervals based on an 80% reactor plant factor. Since radiation damage to process streams is not a problem in pyrochemical processing, an extended period of cooling is not required before the fuel is processed and the fuel may be processed and fabricated during the 206-day interval between shutdowns for refueling.

The plant size is an approach to the smallest commercial-size plant in that only single units are used for most of the unit operations. This fuel-cycle operation has a capacity appreciably in excess of the fuel supply from the reference reactor. A fuel-cycle facility designed for a three-shift, seven-day-week operation using only the throughput from one 1000-MW(e) reference reactor is not economical because the increased operation expense would not be offset by a reduction in the capital requirements for building and

equipment. The particular design studied has a fuel capacity equivalent to 2.35 times the fuel output from the reference 1000-MW(e) reactor at only a nominal increase in capital expenditure to provide additional surge capacity and incell materials-handling facilities. The plant design could be readily adapted to at least three times the reference throughput without appreciable increase in capital expenditures.

Figure 4.1 shows the time allotments during the refueling interval that would be germane to the fuel-cycle operations if the fuel-cycle plant were to handle only one reference reactor. Fuel handling consumes 15 days. During this period the decay of fission products is sufficient to reduce the heat-generation rate of the first elements available for processing to manageable levels, and no additional cooling prior to processing is required. Fuel-processing operations are allotted 147 consecutive calendar days and fabrication operations 177 days.

On this single-reactor basis, the fuel-cycle facility

TABLE 4.1. SPECIFICATIONS OF THE REFERENCE REACTOR THAT ESTABLISH THE FUEL-PROCESSING REQUIREMENTS

Total thermal power, MW(t)	2510
Net electrical power, MW(e)	1000
Fraction of total power generated in core	0.85
Total breeding ratio	1.48
Internal breeding ratio	0.64
Initial composition of fuel, w/o	
Core	
Uranium	79
Plutonium	19
Titanium	2
Blanket	
Depleted uranium	100
Core fuel burnup, % of actinides	6.0
Blanket fuel burnup, % of actinides	0.48
Composition of discharged fuel, w/o	
Core	
Uranium	76
Plutonium	16
Titanium	2
Fission products	6
Blanket	
Uranium	97.0
Plutonium	2.5
Fission products	0.48
Reactor cycle, days	206
Plant factor, %	80
Quantity of fuel discharged per cycle, kg	
Core	6100
Blanket	13300

Fig. 4.1. Time Allotments for Fuel-cycle Operations (Output for One Reference Reactor).

is operated on a schedule of two shifts per day and a five-day week. Operation of the facility in at least two shifts is required because of the time cycle of casting operations. The net processing throughput per operating day (16-hour day) is 185 kg and the fabrication throughput is 154 kg. Fuel is processed batch-wise at the rate of two batches per week. The proc-

essing plant factor (hours of plant operation as to fraction of the total hours between reactor shutdowns for refueling) is 0.34 and the fabrication plant factor is 0.41. Because of the intermittent character of the fabrication, the actual fabrication plant factor is about 0.34. The instantaneous capacity of the fuel-cycle facility is 319 kg per day.

4.2. PYROCHEMICAL PROCESSING

4.2.1. Flowsheet

The reference flowsheet for the decontamination of fuel discharged from the metal-fueled fast breeder reactor and the recovery of plutonium bred in the blanket fuel is presented in Fig. 4.2 and the stream specifications in Table 4.2. The flowsheet is not the only selection of pyrochemical steps that would satisfy the processing requirements. Alternative process steps are currently being studied, and it is expected that some of these will lead to more-economical processing. The reference flowsheet, however, illustrates the economics of pyrochemical processing. It was chosen because it consists of an assemblage of pyro-

* The removal of FPN II is not a routine operation. Titanium, if a component of the core alloy, is eliminated with the FPN II

** The residual amount of fission-product elements in the products is based on 0.324 w/o fission products in discharged blanket fuel.

Fig. 4.2. Principal Steps in the Reference Pyrochemical Process for Metallic Fuel, (U-Pu-Ti Core; U Blanket).

TABLE 4.2. STREAM SPECIFICATIONS FOR THE REFERENCE PYROCHEMICAL PROCESS (U-Pu-Ti CORE; U BLANKET)

	Kilograms per Batch																
	U	Pu	Mg	CdCl ₂	MgCl ₂	CaCl ₂	NaCl	XCl	FPSI	FPSII	FPV	FPNI	FPNII	Cd	Zn	Al	Ti
Influents																	
A1. Core	110	23.8							1.307	2.426	1.503	2.784	0.685				2.8
A2. Blanket	308	7.9							0.230	0.380	0.271	0.508	0.119				
A3. Salt to halide slagging					4	21											
A4. Salt to core-fuel chlorination				177.4			15.6	11.9									
A5. Solvent metal to blanket dissolution and core reduction			300											6661	772		
A6. Metal extractant to salt treatment			4													18	
A7. Metal wash to uranium retort			150														
Key Intermediate Streams																	
B1. Salt from halide slagging					0.4	21			1.307	2.426							
B2. Metal from blanket dissolution to uranium retort	418	31.7	285							0.380		0.508		6672	772		
B3. Metal from uranium retort to plutonium retort		31.7	435							0.380							
B4. Salt from blanket dissolution					61.04		15.6	11.9	0.230								
Solvent Metal to Recycle																	
C1. Distillate from uranium retort														6672	772		
C2. Distillate from plutonium retort			435														
Waste Effluents																	
D1. Metallic waste from treatment of halide slagging salt and trapped volatile matter from blanket dissolution			4							2.426			0.804			18	2.8
D2. Metallic waste from core chlorination												2.784		101			
D3. Salt waste from liquid metal extraction of intermediate salt wastes					65.44	21	15.6	11.9									
D4. Volatile fission product effluents not converted to metallic or salt wastes											1.774						
Products																	
E1. Uranium	418											0.508					
E2. Plutonium		31.7								3.080							
Fission Product Code									Flow Sheet Specifications								
FPV	Xe, Kr, Te, and I								a. Reactor cycle—206 days								
FPSI	Rb, Cs, Sr, and Ba								b. Quantity of core fuel discharged per cycle—6.1 metric tons								
FPSII	Rare Earths and Y								c. Quantity of blanket fuel discharged per cycle—13.3 metric tons								
FPNI	Mo, Tc, Ru, Rh, Pd, Ag, Cd, In, Sn, and Sb								d. Number of batches processed per cycle—42								
FPNII	Zr, and Nb								e. Core fuel per batch 145 kg; blanket fuel per batch—317 kg								
									f. Batches processed per week—2 in 147-day processing campaign per cycle								
									g. Composition of discharged core fuel, w/o—75.70 U, 16.35 Pu, 1.95 Ti, 6.00 fission products								
									h. Composition of discharged blanket fuel, w/o—97.04 U, 2.48 Pu, 0.48 fission products.								

chemical steps for which there is available sufficient support from laboratory and pilot-plant studies to provide a high degree of confidence in their technical feasibility, and for which there is available from component development studies enough information to develop conceptual equipment designs for use in the estimation of processing costs.

4.2.2. Preparation of Fuel for Processing

Preliminary operations are employed to prepare the fuel for processing. The core and blanket fuel assemblies are removed from the reactor and transferred to a sodium-cooled tank for temporary storage. The first fuel assembly is received at the fuel-cycle facility 15 days after the reactor shutdown and after its heat-generation rate from the decay of fission products had decreased by a factor of about 10, which affords a substantial reduction in the cooling requirements in subsequent process steps. Unlike those in aqueous processing, the influent streams in pyrochemical processing are not subject to radiation damage, and, therefore, a long period of cooling is not required. Prior to their receipt at the fuel-cycle facility, the fuel assemblies are steam cleaned to remove residual sodium. In the first operation in the fuel cycle, the fuel assemblies are disassembled and the excess structural metal is removed and discarded.

4.2.3. Removal of Fission Products

REMOVAL OF FISSION PRODUCTS FROM CORE FUEL

In the flowsheet and the tabulated data, the principal fission products are grouped as follows:

FPV	Xe, Kr, Te, I
FPSI	Rb, Cs, Sr, Ba
FPSII	Rare Earths and Y
FPNI	Mo, Tc, Ru, Rh, Pd, Ag, Cd, In, Sn, Sb
FPNII	Zr, Nb

The core and blanket fuel are processed separately in the initial process steps. Only the core fuel is treated to separate fission products in all groups. The blanket fuel is not treated to separate the FPSII and FPNI fission products. Pyrochemical processing does not completely remove fission products. However, in the reference flowsheet, Fig. 4.2, the expected removal of fission products, for which separation procedures are provided, is greater than 99%. Because of the presence of residual amounts of fission products in the processed fuel, remote refabrication of core and blanket

fuel elements and remote refueling of the reactor are necessary.*

Declad core fuel is melted in a beryllia crucible and contacted at 1250°C with a fused salt containing magnesium chloride. The FPV are volatilized. The FPSI and FPSII are oxidized by the magnesium chloride and extracted into the salt phase. The salt phase is separated and the partially processed metal is further treated to remove FPNI and FPNII in subsequent processing steps.

The FPNI are separated by oxidizing the metal with cadmium chloride in a tungsten crucible at 650°C. Uranium, plutonium, and the FPNII are chlorinated. The FPNI are not chlorinated and are accumulated in the cadmium-metal phase which is formed as a consequence of the reduction of cadmium chloride.

REMOVAL OF FISSION PRODUCTS FROM BLANKET FUEL AND REDUCTION OF CORE FUEL

The simplest process is to dissolve dechlorinated blanket fuel in a solvent metal mixture of cadmium-zinc-magnesium and simultaneously reduce uranium and plutonium in solution in the salt phase from the core fuel chlorination with the magnesium in the solvent metal. This process step is conducted at 650°C in a type 410 stainless-steel crucible. Fission products FPV are separated by volatilization and FPSI by extraction into the salt phase. The other blanket fission products are not separated and they accompany the uranium and plutonium in the solvent metal phase. This process results in the distribution of titanium and the FPNII between the two ultimate products. The titanium can be retained with the core fuel by separate processing of the core fuel; i.e., the chlorinated core fuel is first reduced, and that metal phase is transferred to the plutonium retort surge tank, after which the blanket fuel is dissolved. A third option, the separation of titanium by volatilization, is discussed in the next paragraph. The procedure has negligible effect on the cost of processing.

Removal of titanium and the FPNII is not a routine operation. They can probably be separated from the chlorinated core fuel by volatilization. The efficacy of their separation is highly dependent upon the composition of the salt phase. Conditions are required that will cause the volatilization of the FPNI with negligible volatilization of uranium and plutonium. The chemical feasibility of this separation has been established but additional laboratory and pilot-plant

* The use of processes that are capable of high levels of decontamination does not eliminate the need for remote fabrication procedures, because of the radiation hazard from plutonium isotopes in fuel irradiated in power reactors.

studies are needed to determine the operational requirements to satisfy the process objectives. The currently available experimental data indicate that a diluent salt containing at least 60 m/o magnesium chloride is required. The volatilization of zirconium tetrachloride from a salt of the following composition has been observed:

Constituent	m/o
Magnesium chloride	60
Sodium chloride	25
Potassium chloride	15

The required amounts of sodium chloride, potassium chloride, and magnesium chloride enter the process as salt diluents in the core-fuel chlorination step. The salt is not a repository for fission products and is used to avoid excessive losses of plutonium.

PARTITIONING OF URANIUM AND PLUTONIUM; RECOVERY OF URANIUM AND PLUTONIUM AS INGOTS OF METAL

Uranium and plutonium are partitioned in a retorting step. The solvent-metal phase from the blanket-dissolution step, which contains the uranium and plutonium recovered from the core and blanket fuel, is retorted in a beryllia crucible to volatilize cadmium and zinc. Uranium precipitates because of its negligible solubility in the magnesium solution that remains. The FPNI from the blanket fuel accompany the uranium. Plutonium and the FPSII remain in solution in the magnesium phase, which is processed in a separate plutonium retort furnace for the ultimate recovery of metallic plutonium. The solvent metals volatilized in the retort operations are recycled to the blanket-dissolution step.

The precipitated uranium is retorted in the uranium retort vessel to an ultimate temperature of 1200°C to remove the residual solvent metal and to consolidate the uranium into an ingot of metal. The plutonium solution is similarly treated in the plutonium retort furnace.

COMPOSITION OF RECOVERED URANIUM INGOTS AND PLUTONIUM INGOTS

The products of the reference process shown in the flowsheet contain FPSII and FPNI that were generated in the blanket. The amount of fission products in the processed fuel is proportional to the length of time the blanket fuel is irradiated. Even after very long periods of irradiation the amount of fission products retained in the processed fuel is a very small percentage of the amount of uranium and plutonium recovered. For a blanket burnup as follows:

Zone	Quantity of Fuel Removed per Cycle, kg
Axial blanket	5500
Inner radial blanket	2500
Outer radial blanket	5300

the composition of the products (w/o) is:

Uranium Product		Pu Product	
Uranium	FPNI Fission Products	Plutonium	FPSII Fission Products
99.9	0.1	99.1	0.9

The FPSII and FPNI fission products generated in the blanket fuel could be removed by adding to the processing plant additional facilities for chlorination of blanket fuel and for separation of larger quantities of solvent metal. However, since the amount of blanket-generated fission products retained in the processed fuel is of the same order of magnitude as that expected as a consequence of the separation efficiencies of pyrometallurgical processing, there is neither technical nor economic justification for further processing of the blanket fuel. On repeated recycle, the FPNI content of the uranium will build up slowly and may be expected to reach 0.72% after eight cycles (34 years of irradiation). The FPSII in the plutonium product will not build up because the fission products are removed in the processing of core fuel.

WASTE STREAMS

In a salt-treatment step to reduce the heat-generation rate of fission products in the salt waste, the FPSII and FPNII are stripped from the salt with Al-20 w/o Mg alloy. The ultimate salt waste contains the FPSI. The metallic waste consists of the Al-Mg effluent from the salt-treatment step and the cadmium effluent from the chlorination step; it contains the FPSII, FPNI, and FPNII.

4.2.4. Processing Operations and Equipment

The operations flow diagram appears in Fig. 4.3. All processing operations are conducted in an inert-atmosphere shielded cell. Fuel assemblies are delivered to the process cell after they have been steam-cleaned to remove sodium from all external surfaces.

Core and radial blanket assemblies are first dismantled. Core rods are cropped to separate the sections containing axial blanket pins. Core and blanket rods are then mechanically declad. The core and blanket pins are initially processed separately.

The core fuel is processed by halide slagging in a furnace and the salt phase is transferred to the salt-treatment furnace. The metal ingot is returned to a work station for weighing and sampling.

The ingot of processed core fuel is dissolved in fused

FIG. 4.3. Pyrochemical Processing of Metallic Fuel (U-Pu-Ti Core; U Blanket).

chloride salt in a furnace and the metal phase is accumulated in a metal receiver. Waste receivers are hermetically sealed at a remote welding station. The salt phase, which contains the uranium and plutonium, is transferred via a remotely replaceable line to a dissolution vessel.

Batches of blanket pins are prepared and are charged to the dissolution furnace, in which the blanket pins are dissolved in solvent metal and the chlorides of uranium and plutonium from core processing are reduced. The salt from this furnace is transferred to a waste receiver and held for analysis. Salt containing acceptably low concentrations of plutonium is pumped to the salt-treatment furnace.

The metal phase is transferred to a surge tank, from which the solvent metal containing uranium and plutonium is pumped intermittently to the uranium retort. The supernatant molten magnesium, which contains the plutonium, is periodically transferred to the plutonium product surge tank, from which it is pumped intermittently to the plutonium retort. Volatilized solvent metals are condensed in sodium-cooled condensers and collected in surge tanks. The condenser on the uranium retort is lined with silicon carbide to minimize corrosion by zinc-rich condensate. The products, consisting of ingots of uranium, are transferred to a work station where they are weighed

and sampled and batches are prepared for refabrication casting operations. The principle items of processing equipment are listed in Table 4.3.

4.3. REFERENCE FUEL-FABRICATION PROCESS

4.3.1. General

The remote fabrication of uranium-plutonium-fissium alloy was extensively studied for the EBR-II fuel cycle,^{4,3, 4.4, 4.5} and a simple, precision-casting operation was developed for the cycle. Precision casting, further streamlined, is proposed for the reference reactor. The larger clearances in these jackets allow a relaxation of pin-diameter tolerances and permit the use of lower-cost molds than for EBR-II fuel. Inspection of the fuel pins has been reduced to go, no-go, dimensional gaging; weighing; and visual examination. The flow diagram for manufacture of the fuel is shown in Fig. 4.4.

4.3.2. Injection Casting

Basically, the injection casting furnace consists of a vacuum-pressure chamber in which a number of precision-bore mold tubes are suspended vertically, open end down, above a crucible in which the fuel is melted. The furnace and the molds are evacuated

TABLE 4.3. PRINCIPAL ITEMS OF PROCESSING EQUIPMENT

Code	Function	Primary Container Diameter, in.	Container Material	Maximum Operating Temperature, °C	Heating Method
A-1	Halide-slugging furnace	14	Beryllia	1300	Induction
B-1	Chlorination furnace	17	Tungsten	800	Resistance
B-4	Metal-waste receiver	10	Mild Steel	800	Resistance
C-1	Dissolution and reduction furnace	27	410 SS	800	Resistance
C-10	Surge tank	26	410 SS	800	Resistance
D-1	Uranium retort	16	Beryllia	1300	Induction
D-4	Surge tank	11	410 SS	800	Resistance
D-9	Surge tank	22	410 SS	800	Resistance
D-13	Plutonium retort	10.5	Beryllia	1300	Induction
D-14	Surge tank	21	410 SS	800	Resistance
E-1	Salt-treatment furnace	10	410 SS	800	Resistance

during the melting cycle. The crucible is raised to immerse the mold ends and the furnace is rapidly pressurized with inert gas. The pressure on the surface of the metal in the crucible causes the evacuated molds to fill. As soon as the metal in the molds solidifies, the crucible is lowered.

High-silica glass is used as the mold material because it can be formed to satisfactory tolerances and it withstands the thermal shock of casting at 1350°C. One end of the mold tube is flattened and fused closed; the opposite end is cut at a 60° angle to the axis to aid in penetrating the melt surface and to allow excess metal to drip from the end of the mold. Reaction between the silica and the fuel alloy is prevented by a thin coating of thoria or yttria. Thoria or yttria-washed graphite crucibles are used successfully for melting uranium-plutonium-fissium fuel. The graphite serves as an induction heating receptor. Experience with the EBR-II injection-casting furnaces,^{4,6} which melt from 15 to 17 kg and cast as many as 160 molds at a time, indicate that the equipment can be scaled up to melt 70-kg charges to cast approximately 350 fuel pins at one time. Pressurized helium is circulated through the molds for rapid cooling.

A residual heel amounting to about 10% of the charge weight is left behind after casting. This heel is usually removed from the crucible after solidification, broken, and recharged into succeeding melts without significant loss. When the buildup of metal dross on the charge hampers the casting operation the heel is recycled to fuel reprocessing.

4.3.3. Fuel-pin Processing

DEMOLDING

Demolding is accomplished by crushing the glass away from the fuel pin. The present method is simple and effective.^{4,7} The charged molds are mechanically transferred, one at a time, onto two closely spaced

horizontal rails. The rail spacing allows a demolded fuel casting to drop through, but the glass mold will not pass. A hardened-steel blade is driven against the mold by a cylinder. The glass is cracked and the casting is driven through the space between the rails, while the glass fragments fall down a chute into a hopper. The glass fragments have been found to contain a fraction of one percent of fuel alloy. This fuel is recovered by pulverizing the glass and winnowing it through a gas separator, in which the fuel is separated by its greater density. About 95% of the fuel in the glass is recovered by this method. The remainder can be recovered by leaching if doing so is economically attractive.

SHEARING

The fuel pins are deliberately cast overlength, and approximately 50 mm is cropped from the top and bottom by a shearing or rod-parting operation. The end shards are collected and returned either to the pyrochemical or injection-casting operation. No further mechanical work is required to finish the fuel pins.

INSPECTION

A less-detailed inspection procedure is used for the fuel than for that of EBR-II. The rods are scanned visually for gross defects; weighed to the nearest tenth of a gram; and passed between a go, no-go, diameter gage. The acceptable pins are ready for loading.

4.3.4. Assembly and Testing of Fuel Rods

THREE-IN-ONE JACKET TEST

A triple-purpose test is used for the jacket. The three purposes of the test are:

1. Measurement of the jacket volume.
2. Proof-pressure testing of the jacket.
3. Leak detection of the tube and lower weld.

FIG. 4.4. Fabrication of Metallic Fuel.

The jacket is enclosed in a test chamber to within about 10 mm of the top. A seal gland is placed over the jacket and an upper chamber is sealed against this gland. Thus, the wall of the tube separates the upper and lower test chambers. The upper chamber has a calibrated gas reservoir and is fitted with a sensitive pressure transducer. The reservoir is connected to the test chamber by means of an O-ring spool valve. The volume of the reservoir is equal to the nominal volume of the fuel tube. If a given reservoir pressure, say 1400 kg/cm², is expanded isothermally into the tube, the resulting pressure should be 700 kg/cm² if the reservoir and tube volume are exactly equal. If the resulting pressure is less than 700 kg/cm², the tube volume is larger than that of the reservoir and if it is more than 700 kg/cm² the volume of the jacket is smaller than that of the reservoir. The resulting pressure may be calibrated in terms of tube volume. Correction may also be made for adiabatic expansion of the test gas.

The test pressure may be adjusted to just below the yield stress of an acceptable tube. If this is done, the tubes with seamed walls will rupture or yield and weak welds will be blown. Yielding will show as a volume change.

The lower chamber is a protective shroud to collect the fragments of ruptured tubes. It also may serve as a leak-detection chamber, if it is evacuated and connected to a sensitive pressure gage. This test may be performed very rapidly and can take the place of the much slower ultrasonic or eddy-current inspection and helium leak detection.

SODIUM LOADING

Normally a stock of low-oxygen sodium is available at a sodium-cooled-reactor plant. If not, commercial sodium can be cleaned and purified by heating it to about 600°C in contact with zirconium turnings while it is protected from the air by an inert atmosphere. All operations on sodium are carried out in a glove box. The clean sodium is cast directly into extrusion containers. The loaded containers are mounted on the extrusion press, which is equipped with a long runout tube. The press is calibrated to deliver a predetermined volume of sodium. Sodium wire is deposited in the bottom of the jacket by means of this tube and is cut by an electrical heating element at the end of the tube.

After the sodium is loaded into the jackets, it is melted to provide space for loading the other components. After the loading and premelting, the components are ready to be assembled with the other jacket hardware.

ASSEMBLY OF FUEL RODS

After sodium filling, the jackets are transferred into the remote facility where acceptable fuel pins are inserted. The procedure for fuel-rod assembly is a modification of that used in the EBR-II process.^{4,8} The jackets, containing sodium and fuel pins, are loaded into a magazine and charged to a bonding operation. Bonding is achieved by evacuating the magazine and simultaneously heating and vibrating the fuel rods. The maximum bonding temperature is 500°C and the number of impacts on the rods should not exceed about 1000. After bonding, the magazine is cooled to a temperature just above the melting point of sodium, the pressure in the magazine is brought up to cell pressure, and a restrainer is inserted into each jacket. The magazine loaded with bonded fuel rods is then transferred to welding and final inspection steps.

WELDING AND INSPECTION

Bonded fuel rods containing a seated restrainer are transferred in a magazine to a welder, where a closure weld is made. For rods of 6.35 mm maximum OD, a capacitor discharge weld is made. Where the fuel-rod diameter exceeds 6.35 mm, an arc-type girth weld is made.

Sealed fuel rods are inspected for leaks by a pressure technique^{4,9} developed for the EBR-II recycle process. Inspection for bond integrity may be neglected, since the sodium annulus is very generous (0.37 mm) in the fuel region. If it is found that 100% inspection for sodium bond and recycle of nonbond fuel rods is required, the existing technology can be used.^{4,10}

ASSEMBLY OF FUEL ELEMENTS

The fuel rods and fuel element hardware are assembled in a remote operation. The fuel rods are loaded, one row at a time, in an assembly jig. The spacers are positioned on top of each row of fuel jackets before the next row is added. The assembled jackets are compressed and the hexagonal tube forced over the jacket bundle. A temporary bottom fixture adapts the hexagonal tube and wire array to the assembly machine. The steel stem wires extend below the bottom end of the jacket and are laced through the proper holes in the lower stainless-steel grid. The grid is then forced into its proper position and arc-spot welded in place. The fuel rods are individually drawn into the lower grid by means of the extension wires. The knob on the end of the steel rod in the jacket fitting is withdrawn. This tightens the jacket tip in the grid and, as it passes beyond the bottom of the grid, expands the fitting against a bucking tool in

the loading fixture. When the metal of the jacket tip has expanded tightly in a counterbore in the lower fitting, further movement of the knob is blocked and the steel rod stem is snapped off at a weakening notch. The knob remains in the fitting, serving as a reinforcing key to prevent loosening of the fuel rod from the lower grid.

After all 331 fuel rods have been attached to the grid, the fuel element is completed by installing the end fittings and arc-spot welding them to the hexagonal tube. The element is gaged and inspected. It is then ready for return to the reactor.

4.4. WASTE DISPOSAL

4.4.1. General

In the reference fuel-cycle facility, the radioactive fission products in the spent fuel are separated in concentrated form in waste streams from the pyrochemical process steps. Waste having the major activity is fixed and packaged in steel containers in a stable form as solid salt or metal. This packaged waste is given interim cooling at the processing site and then shipped to a final burial location. Figure 4.5 gives the layout of the on-site interim storage canal.

A favorable feature of the compact pyrochemical processing method is that the high-level fission products are directly separated as a highly concentrated solid waste, with both higher densities and higher thermal conductivities than those from the major alternative processes. For example, the combined metallic-waste stream, which contains about 95% of the total fission-product heat at 30-days cooling, has an average concentration of fission-product elements of 4.4 w/o in a solid with a density of about 340 lb/cu ft and a thermal conductivity of about 30 Btu/(hr)(sq ft)(°F/ft). The combined salt waste, containing nearly all of the balance of the fission-product heat at 30 days, is a solid with about 1.2 w/o fission-product elements, a density of 101 lb/cu ft, and a thermal conductivity of about 1.0. For comparison, pot-calcined aqueous wastes of the acidic Purex or Thorex types have similar fission-product concentration, but they have lower values for solid densities and thermal conductivity, about 67 lb/cu ft and 0.25 Btu/(hr)(sq ft)(°F/ft) respectively.

The gaseous fission products are discharged to the atmosphere after time is allowed for the radioactive isotopes to decay to stable and innocuous forms. Low-level active waste from the pyroprocessing and fabrication steps are solids, which include spent equipment and materials such as scrap cladding from fuel-processing crucibles, filters, and injection-casting

FIG. 4.5. Canal for Interim Cooling of Containers of High-activity Waste from Full-capacity Pyrochemical Processing Plant.

molds. Low-level activity in the form of aqueous solutions is generated in the analytical laboratory and in decontamination operations. Miscellaneous wastes with negligible activity are in solid and liquid forms.

The processing rates and general waste-handling procedures are summarized in Table 4.4.

4.4.2. Sources of Fission-product Waste of High-level Activity in Process Separations

The high-level wastes are separated in the pyrochemical steps as shown in the process flowsheet in Fig. 4.2. The halide-slagging step transfers the highly active rare-earth fission products (FPSII group) from the metallic core fuel to a small amount of chloride salt. Because fission products are too highly concentrated in this salt for convenient storage of waste, the most-active fission products are stripped from the salt into an aluminum-magnesium alloy in a separate processing step. This alloy, because of its relatively high density and thermal conductivity, is an especially favorable form in which to fix the major fission products, which furnish about 95% of the total

TABLE 4.4. SUMMARY OF WASTE QUANTITIES AND DISPOSITION
(Fuel-cycle Facility at Full Capacity)

Wastes	Fission Products	Waste Quantities per Year			Interim Storage	Final Storage
		Fission products, kg	Metal or salt, kg	Total vol., cu ft		
Process Waste Streams						
Al-Mg metallic waste (from rare-earth removal)	Rare earths + Y (FPSII) (FPNII)	629	4,300		Combined metal stored under water, 8 years	Shipment to storage in mine (off-site)
Cd metallic waste (from noble-metal extraction)	Noble metals (FPNI)	575	17,600			
Subtotal		1204	21,900	142		
Chloride salt from rare-earth removal, blanket dissolution, and core reduction	Sr, Ba, Rb, Cs, (FPS I)	269	19,900	443	Combined salt stored under water, 1 year No Yes	Shipment to storage in mine (off-site)
Volatile matter trapped on fume trap*	I, Te	66				On-site burial
Noble-gas fission products	Xe, Kr	244				Venting after interim cooling
Total		1783				
Miscellaneous Wastes						
Removal of cladding metal from spent elements	—	—	13,300		No	On-site burial
Miscellaneous solid active process wastes†	—	—				
Miscellaneous aqueous wastes‡	—	—	—	8900	No	On-site storage in tanks
Nonactive liquid	—	—	—	—	No	On-site lagoon
Nonactive solid	—	—	—	—	No	On-site burial

* Buried with Fiberfrax fume traps in miscellaneous solid active process wastes.

† Includes spent crucibles, spent molds, etc.

‡ Waste from analytical laboratory and from decontamination operations.

fission-product heat generated after 30 days of decay.

Another metallic-waste stream is separated from the core fuel in the chlorination step, which follows halide slagging and blanket dissolution. As described in the flowsheet, Fig. 4.2, this stream consists of cadmium and contains the principal noble metals (FPN I group).

The above metallic wastes are cast into steel containers to give a combined metallic-waste stream. The details of the heat loads, interim cooling, and final disposal of the packaged metallic waste are presented in later sections on the description of facilities.

Certain fission products are separated as volatile matter in the halide-slugging step. Of these, xenon, krypton, and iodine are gaseous even at temperatures far below that at which halide slagging is carried out (1250°C). Another fission product, tellurium, is highly

volatile in both its elemental and chloride form at the halide-slugging temperature and is similarly separated as a volatile component. Fission products other than these four are retained in either the metallic or salt phases in the halide-slugging step. The iodine and tellurium are largely collected on the Fiberfrax vapor trap of the halide-slugging furnaces, while the xenon and krypton pass through the trap and are stored in tanks under pressure. After cooling for about 120 days, the waste gas can be safely released to the environment during periods of suitable atmospheric conditions. The details of the gas-handling steps are presented in the later section on description of facilities.

Certain other fission products, zirconium and niobium, form chlorides, which can be separated as volatile matter after the reduction of core fuel. Titanium, present as an alloying constituent, behaves similarly.

These volatile chlorides are condensed in cold traps and in a bed packed with calcium oxide, and they are subsequently added to the aluminum-magnesium waste stream described above.

The final salt waste stream contains the FPSI group of fission products (Rs, Cs, Sr, and Ba) as shown in the flowsheet, Fig. 4.2. This is a combined salt containing the FPSI fission products from both the halide-slugging step and the blanket-dissolution step. The combined salt is cast into steel containers. The heat load and method of handling the solid packaged waste will be given in the description of the waste facilities.

4.4.3. Interim Storage of Metallic and Salt Wastes

The major fission products separated in reprocessing are contained in the metallic and salt waste streams. The metallic and salt wastes are separated as liquids in the processing cell and are cast into steel cylinders, in which the wastes solidify. After the cylinders are filled to a specified level, a cap is welded in place in the cell with remotely operated welding equipment. The cylinders are then transferred out of the cell through an air lock and placed in a shielded transfer pot. The wastes are moved out of the building through a transfer tunnel and the cylinders are placed under water in the interim cooling canal. The depth of water in the canal is sufficient to provide radiation shielding for moving cylinders in the canal. After the interim cooling, the cylinders are removed from the canal, placed in a shielded container, and shipped to a remote site for final storage.

HEAT-GENERATION RATE IN WASTES DURING INTERIM STORAGE

Rates of heat generation in the wastes due to fission-product decay are based on compilations of fission-product spectra for fast fission of Pu^{239} by Burris and Dillon^{4,11} and on calculations of radiation emission for individual fission products by Steunenberg, Steindler, and Vogel.^{4,12} The maximum value for the waste at the time of separation (about 30 days) is 1500 Btu/(hr) (cu ft) for the salt waste and 88,000 Btu/(hr) (cu ft) for the metallic waste.

The high rate of heat generation in the metallic waste at initial conditions restricts the allowable diameter of the waste container. In order to provide that the heat flux at the container wall for underwater interim storage is in the lower range of values for nucleate boiling (that is, about 33,000 Btu/sq ft), the diameter of the container for metallic waste is limited to 16 in. Under these conditions, the temperature at the axis of the cylinder is calculated to be

about 330°C. The thermal conductivity of metal is taken to be 30 Btu/(hr) (sq ft) (°F/ft).

The heat-generation rate of the salt is lower and a cylinder diameter of 2 ft is chosen for convenience in handling. At initial conditions the axial temperature of the salt (in boiling water) is estimated to be about 375°C. The thermal conductivity of the solid salt is taken as 1.0 Btu/(hr) (sq ft) (°F/ft). Although at these temperatures some of the salt would be molten, no difficulty is expected, because the molten zone is confined within a zone of frozen salt at the wall. The reduction in the heat-generation rate or the wall temperature reduces the size of the molten zone. (An opposite temperature transition, in which the wall temperature would be increased, should be avoided by administrative control, because of the possibility of internal pressure produced by expansion of the salt on melting.) After about one year, the heat-generation rate in the solid salt is low enough to allow cooling by air convection.

Because of the non-corrosive environment to which the containers for metallic and salt wastes are exposed, steel is used as the container material. The outside of the container is aluminized^{4,13} to enhance resistance to air oxidation and to provide anodic protection in water.

For the annual waste load of the reference fuel-cycle facility at its full capacity, the interim waste-storage requirement is 49 cylinders/year of salt waste, (2 ft diameter x 4 ft long, 80% filled) and 35 cylinders/year of metallic waste (16 in. diameter x 4 ft long, 80% filled). The container cost for steel cylinders of nominal 1/4-in. wall is estimated at \$350 (salt) and \$90 (metal) each. For the interim storage of one year for salt and eight years for metal, the equilibrium capacity of the interim storage facility is 329 cylinders.

During the first year of interim cooling heat generation in both metallic and salt wastes decreases substantially because of the presence of relatively short-lived fission products. For additional cooling periods, however, the decrease of the heat-generation rate in the salt waste is proportionately much less than for the metallic wastes, because the only major activity remaining in the salt is that of the relatively long-lived fission products strontium and cesium. The specific heat-generation rates at the end of the interim cooling period are 260 Btu/(hr) (cu ft) for salt waste and 105 Btu/(hr) (cu ft) for metal, for interim cooling times of one and eight years, respectively.

For these interim storage times the maximum total heat-dissipation rate required for the interim cooling canal is 5.9×10^6 Btu/hr. For the wastes after interim

cooling the specific heating rate is 225 Btu/(hr) (cu ft) for the combined average of both salt and metal.

HEAT REQUIREMENTS AND RESTRICTIONS IN INITIAL TRANSFERS

In the initial handling operations, in which the waste cylinders are filled, capped, and transferred to the canal after about 30 days cooling, removal of fission-product heat requires special consideration. For the salt-waste container the surface temperature for natural air convection and radiation is approximately 250°C, which is satisfactory because the temperature is low enough to provide a frozen layer of salt at the inner surface of the container wall and because corrosion by air oxidation at the outer surface is not serious at this temperature. The temperature of the wall of the metallic-waste container under these conditions would be too high (about 800°C) for extended periods. Therefore, for normal in-cell operation, forced-draft cooling with argon is provided to keep the wall temperature at about 200°C, which is below the melting point of the metallic waste. Cooling is also required for transport of the waste container outside of the cell; this is readily provided by boiling water.

INTERIM STORAGE CANAL

The facility for underwater cooling of the packaged wastes could be of various designs. For the purposes of this cost analysis the reference design was patterned after that reported at Oak Ridge National Laboratory for the interim cooling canal designed for packaged solid wastes obtained by the pot-calcination of aqueous Purex and Thorex wastes.^{4,14}

The interim storage facility consists of a central canal for receiving and removing the cylinders, additional canal space for the storage of the cylinders, and a service area containing the equipment for water cooling and purification. An office and change room are included. The central canal and storage canals are constructed of reinforced concrete with walls and floor 1 ft thick. The inside of the concrete is painted with a polyamide-cured epoxy protective coating as an aid to maintaining high-purity water and for easier decontamination. A diagram of the canal facility for interim waste cooling is shown in Fig. 4.5.

The central canal serves as an area for underwater loading of the cylinders into casks prior to shipping off site. This portion of the canal is 30 ft long, 30 ft deep, and 24 ft wide. The storage portion of the canal is 24 ft wide and 110 ft long.

The cylinders of solidified waste are stored upright in the canal. They are arranged in parallel rows with a space between rows for moving them in and out. The

area of canal required for storage of the cylinders in this array is a function only of interim storage time and is independent of the diameter of the cylinder.

To maintain the waste cylinders in position during storage they are placed in individual stands constructed of a structural aluminum alloy and anchored in the floor. The cylinders are placed in and removed from these stands by use of an overhead 5-ton bridge crane. The storage canals are housed in a light-construction steel building to reduce accumulation of litter and to make operation in inclement weather less difficult.

The canal water is recycled to the service area for cooling and demineralization. The cooling system consists of pumps, intermediate heat exchangers, and a cooling tower. In the primary loop, water is circulated by pumping from the ends of the canals, through the heat exchangers, and back to the central facility. In the secondary loop, cooling water is pumped through the heat exchangers and returned to the cooling tower. Cooling systems are designed to handle the maximum heat generation of 5.9×10^6 Btu/hr. A temperature rise of about 15°C is allowed for the water during passage through the canals.

The water-purification equipment consists of a filter and deionizers, through which the water in the canals is circulated at a rate equivalent to a turnover time of one week. The water is deionized by passing first through a bed of strongly acid resin, and then through a bed of weakly basic resin. Tanks are provided for storage of acid and sodium carbonate regeneration solutions.

A masonry building is provided to house the water-purification system, a small maintenance shop, office, and change room. The building also encloses the central canal.

The storage portion of the canal, sized for the equilibrium capacity of waste cylinders, was calculated on the basis of a space between cylinders equal to the diameter of the cylinder in one dimension and half the diameter in the other. A reserve space allowance of 25% is added for contingency. On this basis, 2600 sq ft of storage canal is required. The closest comparable case in the ORNL study is 1042 sq ft for a one-year interim cooling canal for 14,800 MW(t) of pot-calcined acidic Thorex waste.^{4,14}

4.4.4. Volatile Fission Products

Fission-product xenon, krypton, iodine, and tellurium are separated as volatile matter in the halide-slugging and blanket-dissolution steps. The waste-handling step for these fission products is shown in Fig. 4.6. The bulk of the iodine and tellurium is removed by the Fiberfrax fume trap and the remainder

FIG. 4.6. Schematic of Off-gas Processing Equipment.

on the charcoal traps, which are part of the process off-gas system of the pyrochemical-processing plant. The noble gases, xenon and krypton, are liquefied and then stored about 120 days as gas at 800 psi in steel cylinders. Under suitable atmospheric conditions the cooled waste gas is diluted and discharged to the atmosphere through the stack.

The solid materials from the fume and charcoal traps are broken up and discarded in steel waste containers (2 ft in diameter) similar to those used for salt waste but of lighter gage. Two Fiberfrax caps (15-in. diameter, 1-in. thick) are discarded each day, one from each of the processing steps in which they are used. One cylinder can hold 42 caps. In each cylinder will be about 0.06 curie of I^{129} (1.7×10^7 -year half-life) and 2.5×10^5 curies of I^{131} (8.08-year half-life). Heat dissipation of the iodine and tellurium is about 0.5 kW and decreases rapidly with time. After interim cooling for about four months the waste containers are buried on site. The charcoal traps, containing small amounts of iodine and tellurium, are handled similarly, but interim cooling is not required.

4.4.5. Cladding and Fabrication Wastes

Solid wastes, consisting of cladding removed from spent fuel elements, waste molds from the injection-casting step of fuel fabrication, and discarded crucibles and other hot equipment are sealed and packaged in the 2-ft steel cylinders and buried on site. The quantities are shown in Table 4.5. The packed volume of cladding waste is estimated at 1 cu ft per day for

the full-capacity plant, assuming 75% void space in the container. The packed volume of spent injection casting molds is estimated at 1.2 cu ft per day with 60% void space. Crucibles and miscellaneous solid wastes have an estimated packed volume of 0.25 cu ft per day. The annual gross volume requires 100 of the 2-ft diameter standard waste containers.

4.4.6. Aqueous Wastes

Aqueous wastes containing activity are generated in the cleaning of sodium from the fuel subassemblies and from reactor components in the decontamination of equipment, in the analytical laboratory, and in miscellaneous operations. The total liquid volume,

TABLE 4.5. MISCELLANEOUS SOLID WASTES*

Type of Waste	Packed Volume, per Day, cu ft	Number of Containers per Year
Cladding removed from spent fuel elements	1.0	33
Waste molds from injection casting	1.2	38
Fiberfrax fume traps	0.4	14
Charcoal traps	—	10
Crucibles, etc.	—	5
		—
		100

* Void fraction 60% to 75%, depending on type of waste. Standard container 2 ft diameter x 4 ft length. Light-gage steel containers costing \$60 each. No interim storage required. Burial in concrete pipes set vertically in the ground.

the solids content, and the activity content are variable. For the purposes of this study, a volume of 67,000 gal. per year is estimated. The liquid is initially stored in a 25,000-gal. steel tank and evaporated to reduce the volume by a factor of 50. Evaporator overhead condensate, if not radioactive, is discharged into the lagoon. The radioactive concentrate is stored in a 50,000-gal. stainless-steel tank. For additional safety, the stainless-steel tank is located in a buried concrete vault.

4.4.7. Shipping

A single large shipping cask of less than 100 tons could hold eight salt-waste cylinders or 14 metallic-waste cylinders. On this basis a total of nine shipments per year are required. Cost per shipment (including use charge for cask) is estimated as \$6500/trip for round-trip distances of rail shipment up to 2000 miles. Annual shipping costs are \$65,000 on this basis. The above cost estimates were based on studies at ORNL and ANL.^{4.15, 4.16}

4.4.8. Integration of Waste Facilities

The various components of the waste facilities are integrated with the fuel-cycle facility as a whole. The only major building of the waste facility (separate from the fuel-processing and fabrication building) is the one enclosing the interim cooling canal for high-level wastes. The canal is located within 100 ft of the processing building and is connected by a ramp. The canal system includes facilities for handling heavy shipping casks used for transporting fission-product wastes. The waste building consists of the portion of low-cost construction over the storage canal and a high-bay general-purpose construction building over the central canal and over the transfer area for incoming and outgoing shielded casks. This high-bay portion of the building is extended in order to house, in separate sections, other components of the waste system. These other components include the heat exchangers, pumps, and water-treatment equipment for the cooling canal; the evaporator for miscellaneous wastes; and the charcoal absorber and liquefaction system for the storage of rare-gas fission products.

Other components of the waste system, located elsewhere than in the waste building, are:

1. Tanks for receiving and neutralizing miscellaneous aqueous wastes are located in the main processing building, where the wastes are generated.
2. Large mild-steel tanks for storage of the above neutralized aqueous waste are buried on-site. The aqueous wastes are piped into the tanks directly from the processing building and are pumped to the waste building for evaporation and concentration.

3. Stainless-steel tanks are provided on-site for storage of concentrated aqueous wastes (above). Concentrated waste is piped to the tanks from the waste building.

4. The tanks for storage of rare-gas fission products under pressure are buried on-site. Gas is received at the tanks from the waste building in piping. After cooling, the gas is disposed of through piping to the main exhaust air stack of the plant.

5. The exhaust air stack is located close to both the waste building and the fuel-processing building.

6. A shelter is provided for temporary storage for miscellaneous solid wastes destined for on-site burial. The shelter is located at the entrance to the burial zone.

4.4.9. Ultimate Storage

Ultimate storage is provided by permanent burial at a separate site. The overall evaluation of methods and locations for such burial is beyond the scope of this study. For purposes of cost estimation, the costs developed by ORNL for a central 15,000-MW(e) storage facility are used as a basis.^{4.17} For solid wastes of 225 Btu/(hr) (cu ft), approximately 0.17 acre/year of net mine area is required for the 443 cu ft/yr of total metallic and salt waste for the full-capacity fuel-cycle facility. Space limitations are posed by the maximum allowable mine temperatures. The total capital and operating charges estimated for the permanent burial are equivalent to the use charge of \$110,000 per year.

4.5. OPERATION AND COSTS OF FACILITY

4.5.1. Operation

Conceptual design drawings of the facility in which the fuel is processed and fabricated are shown in Figs. 4.7, 4.8, and 4.9, which are a plan view of the main working level, an elevation view, and an isometric view. Basically, the facility is a heavily shielded annulus in which processing and fabrication operations are conducted remotely under an inert atmosphere. The total land area occupied by the processing-fabrication plant is about 39,000 sq ft. The processing cell is an annulus 20 ft wide with an inner radius of 37 ft. The walls of the process cell are of high-density concrete 5 ft thick. The total area of the process cell is 5900 sq ft, of which 2900 sq ft is devoted to processing and 3000 sq ft to fabrication.

The design of the plant is similar to and based on that of the EBR-II Fuel Cycle Facility,^{4.2} which is an operating plant close-coupled to the metal-fueled Experimental Breeder Reactor II. The conceptual plant is about twice the size of the EBR-II plant. The space

FIG. 4.7. Plan View of Fuel-processing Cell.

FIG. 4.8. Elevation View of Fuel-processing Cell.

FIG. 4.9. Isometric View of Fuel-cycle Facility.

1. Tunnel connecting reactor and fuel-cycle facility	11. Maintenance annulus
2. Hot maintenance shop	12. Inner operating annulus
3. Decontamination cell	13. Office space
4. Analytical laboratory	14. Process control room
5. Outer operating annulus	15. Subcell
6. Fabrication influents glovebox	16. Cold receiving dock
7. Access tunnel to subcell	17. Skylight
8. Warehouse	18. Electromechanical manipulator
9. Administrative offices, lunchroom, and locker room	19. Bridge crane
10. Processing annulus	

allowances shown in the plan view, Fig. 4.7, for ancillary facilities reflect the recommendations of the EBR-II operating personnel. Alternative plant designs for the conceptual plant are not considered, on the assumption that the pertinent criteria were adequately evaluated in planning the EBR-II Fuel Cycle Facility. By using the basic design of the EBR-II Fuel Cycle Facility in the conceptual plant, the EBR-II costs of building and equipment could be used to provide a firm basis for the estimation of costs for the conceptual plant.

As shown in Fig. 4.7, fuel enters the facility via a tunnel which terminates at a large lock in the process cell. Fuel subassemblies are stored in gas-cooled pits to await processing. Processing proceeds counterclockwise around the annulus. Operations are viewed directly through strategically placed windows. Finished fuel subassemblies are retained in the storage pits until the reactor is shut down for refueling.

The process cell is served by two 5-ton bridge cranes, five Argonne-General Mills bridge manipulators, and 11 pairs of fixed-location Central Research Laboratories Model A manipulators. The CRL manipulators, installed in the outer wall of the cell, serve the work stations, of which there are two in the processing portion of the cell and nine in the fabrication portion of the cell. Processing and fabrication operations are controlled from a 64-ft-diameter central area inside the processing annulus. Plug-ins are provided in the central control room and the outer operating annulus for the remote control of bridge cranes and manipulators. The massive power supply units are located in the subcell and in the space above the control room.

Supporting facilities are located along the outer perimeter of the facility. The administrative offices, lunch room, and locker room comprise a small wing. On the opposite side of the plant is a large wing that houses the warehouse, facility for preparation of fabrication influents, and the stockroom. The fabrication influents enter the cell via a large (30 ft long by 4 ft wide) glove box. Along the perimeter of the plant is the analytical laboratory, which includes a small hot cell and a copper-shielded counting room. Also provided is a hot maintenance shop. Equipment to be repaired is removed from the cell via the main lock and transported through the cell to a port in the decontamination cell. The elevation view, Fig. 4.8, shows the spatial relationship between these egresses. In the stainless-steel-lined decontamination cell, the activity level of the equipment is reduced sufficiently to permit contact maintenance operations. As shown in the elevation view, the instrument shop, machine shop, and the processing-influents-preparation facilities are located in the area beneath the central control room.

Processing influents enter the cell via the waste-processing lock.

Steam and electric power are purchased and, with the exception of an emergency generating unit, no primary generating equipment is provided in the facility. Equipment for distribution of utilities, generation of high-frequency electric power, collection of sanitary wastes, purification of the cell atmosphere, and handling of process gases is located in the subcell area.

4.5.2. Costs

The reference fuel-cycle facility was designed as an approach to the smallest size that would be commercially attractive to build. This was discussed above in Section 4.1, Capacity Requirement. A cost evaluation was performed on the plant operated at the capacity of one reference reactor. The plant factor for this case was only 34%. This plant factor is low principally because reactor unloading, cooling, processing, fabrication, and refueling must occur within the 206-day period between reactor cycles. The plant investment costs for this facility increase only modestly until its full capacity is reached. On the other hand, operating costs of the reference plant design are more nearly proportional to the fuel feed rate to the plant. The result of the above considerations is that the reference fuel-cycle facility has substantially lower total unit costs at full capacity than at partial capacity—1000 MW(e). For the reference reactor conditions of 6% core burnup, the fuel-cycle facility has a capacity equivalent to 2350 MW(e) at 80% plant factor. The 7% core burnup allowance increases capacity of the fuel-cycle facility equivalent to 3000 MW(e). However, the capital cost of the waste portion of the cycle is increased appreciably for the large throughput.

The costs of fuel processing, fabrication, and waste disposal were estimated jointly by the Chemical Engineering and the Metallurgy Divisions. The cost of processing at full capacity was obtained by interpolation of results of a Chemical Engineering Division analysis of processing costs in plants operated at 80% plant factor.^{4,18} Adjustments were made to these costs to account for the savings realized in a plant that provides both processing and fabrication by the apportionment of overhead between the two functions. The conceptual designs of a plant to process fuel and fabricate as specified in the reference flowsheets are based on the existing EBR-II Fuel Cycle Facility. All processing and fabrication operations with highly irradiated fuel are conducted fully remotely in a large heavily shielded inert-atmosphere enclosure. Esti-

mates of building and installation costs are largely based on the costs incurred in the construction of the EBR-II Fuel Cycle Facility. Estimates of equipment costs are based on EBR-II costs, on the projected cost of plant-scale versions of components currently under development, and on manufacturers' quotations.

For the full-capacity plant, the primary cost of facilities is estimated to be \$13,000,000; with the estimated 95% confidence level that of the estimate range of uncertainty is \$11,000,000 to \$17,000,000; these figures are exclusive of overhead costs for a full-capacity fuel-cycle facility. The total annual operating cost, including all fabrication materials and administration costs, is estimated to be \$12,000,000, using V-20 w/o Ti alloy for cladding all fuel. The total annual charge for processing, fabrication, and waste disposal (including overhead, capital replacement, and return on capital, but excluding charges for inventory, processing loss, and credit for net plutonium produced) is \$15,000,000. A 13% per year return on non-depreciating capital is used for calculating the return on capital. This figure includes property insurance and taxes and a 6.75% per year rate for the cost of investment money, based on private utility practice as reported in Ref. 4.19. The 6.75% per year rate is also used to calculate capital replacement charges, based on estimated economic lifetime of plant components.

The cost results are summarized in Tables 4.6 through 4.19, and the costs for the reprocessing, fabrication, and waste-disposal portions of the plant are presented separately in the following sections.

ESTIMATED COSTS OF PYROCHEMICAL PROCESSING PORTION OF PLANT

For the reprocessing portion of the integrated fuel-cycle facility, a breakdown of estimated capital costs is given in Table 4.7, and one for operating costs in Table 4.8. These capital costs do not include overhead items.

TABLE 4.7. CAPITAL COST OF PYROCHEMICAL PROCESSING COMPONENTS AND LIFETIMES
(Partial Capacity)

	Total Cost, \$	Items of Various Lifetimes, \$		
		30 years	15 years	5 years
General Facilities				
Offices, change room, lunch room, etc.	150,000	60,000	90,000	
Warehouse	25,000	25,000		
Analytical laboratory	338,300	91,800	246,500	
Machine shop	77,000	22,000	55,000	
Decontamination area	156,000	80,000	76,000	
Argon Cell				
Argon process cell	1,951,600	1,650,600	301,000	
Shielding windows	150,000	150,000		
Manipulators	206,000		206,000	
Transfer lock	106,000	21,200	84,800	
Influent preparation	56,300	12,500	43,800	
Process Equipment				
Instrumentation	116,500		116,500	
Furnaces	154,600			154,600
Jumpers	77,000			77,000
Weighing stations	10,000		10,000	
Service leadthroughs	50,000		50,000	
Fuel assembly and dis- mantling machines	246,000			246,000
Agitators and pumps	20,000			20,000
Welding station	15,000			15,000
High-frequency power	124,400		124,000	
Auxiliary Equipment				
	314,400		314,400	
Total	4,344,100	2,113,100	1,718,400	512,600

TABLE 4.6. SUMMARY OF CAPITAL AND OPERATING COSTS FOR PYROCHEMICAL PROCESSING

	Partial-capacity Operation	Full-capacity Operation
Capital cost, \$	4,344,100	4,631,400
Annual operating cost, \$/yr	1,121,000	2,666,000

TABLE 4.8. ANNUAL OPERATING COSTS FOR PYROPROCESSING WITH PARTIAL CAPACITY

(For Plant Operation Only at Rate Needed for One Reference Reactor)

Labor	
Total direct and indirect labor*	\$ 591,000
Materials and utilities	249,000
General operating and maintenance materials†	155,000
General overhead and administrative expenses‡	126,000
Total	\$1,121,000

* The average allowance per man year of \$12,000/yr includes wage escalation in the near future plus all fringe benefits and additional expenses.

† This category is estimated as 20% of the annual labor charge plus 20% of the annual charges for replacement of all capital except construction overhead and preoperation costs.

‡ Taken as 15% of the total of labor, materials, and utilities.

TABLE 4.9. SUMMARY OF CAPITAL AND OPERATING COSTS FOR FUEL FABRICATION

	Partial-capacity Operation	Full-capacity Operation
Capital cost, \$	5,823,400	5,823,400
Annual operating cost, \$	3,400,300	8,785,000

ESTIMATED COSTS OF FABRICATION PORTION OF PLANT

For the fabrication portion of the integrated fuel-cycle facility, a breakdown of estimated capital costs is given in Table 4.10 and operating costs in Table 4.11. These capital costs do not include overhead items. For an alternative calculation of costs, see Appendix 6.4.

ESTIMATED COSTS OF WASTE-DISPOSAL PORTION OF PLANT

A breakdown of estimated capital costs for the waste-disposal portion of the integrated fuel-cycle facility is given in Table 4.13 and operating costs are shown in Table 4.14. These capital costs do not include overhead items.

TABLE 4.10. CAPITAL COST OF FABRICATION COMPONENTS AND LIFETIMES

	Total Cost, \$	Items of Various Lifetimes, \$		
		30 years	15 years	5 years
<i>General Facilities</i>				
Offices	150,000	60,000	90,000	
Warehouse	25,000	25,000		
Analytical laboratory	63,000	53,000	10,000	
Decontamination	10,000	4,000	6,000	
<i>Argon Cell</i>				
Argon cell	1,885,000	1,595,000	290,000	
Windows	325,000	325,000		
Manipulators	433,500		433,500	
Transfer locks	90,000	18,000	72,000	
Influent preparation	420,900	25,000	395,900	
Glove box	40,000		40,000	
<i>Process Equipment</i>				
Instrumentation	192,500		192,500	
Leadthroughs	76,000		76,000	
High-frequency power	149,000		149,000	
Fuel-pin and rod-fabrication equipment	1,765,000			1,765,000
<i>Auxiliary Equipment</i>				
	198,500		198,500	
Subtotals	5,823,400	2,105,000	1,953,400	1,765,000

TOTAL ANNUAL COSTS AND UNIT CHARGES FOR FUEL-CYCLE FACILITY

The cost components of the capital cost and total annual charges for the reference fuel-cycle facility are given in Tables 4.15–4.19. In the costs, overhead factors are used to include construction overhead, operating expense components, startup costs, and working capital. No explicit overall contingency factor is included, partly because the overhead factors compensate for the tendency to omit costs in grass-roots estimates, and partly because errors in estimation of individual cost components tend to cancel. The confidence interval of an estimate of this type is about

+30
-15 percent.^{4,20}

TABLE 4.11. ANNUAL OPERATING COSTS FOR FUEL FABRICATION WITH PARTIAL CAPACITY
(For Plant Operation Only at Rate Needed For One Reference Reactor)

Labor	
Direct operating labor (44 men at \$12,000/yr)*	\$ 528,000
Indirect labor and technical supervision†	264,000
Subtotal	\$ 792,000
Product material cost‡	2,041,800
Utilities	32,800
General operating and maintenance materials§	240,600
General overhead and administrative expense	293,100
Total	\$3,400,300

* The average allowance of \$12,000/yr includes wage escalation in the near future, plus all fringe benefits and additional payroll expenses.

† The number of men at indirect labor is prorated at $\frac{1}{3}$ the number at direct labor. The average annual labor allowance at indirect labor is taken as $\frac{3}{2}$ that at direct labor.

‡ A product material cost breakdown is as follows:

1. Tubing for jacket (cladding) V-20 w/o Ti	63.14%
2. Molds	17.45
3. Hardware for fuel assembly	12.4
4. Fuel-rod jacket and fittings	4.4
5. Crucibles	1.6
6. Pallets	1.0
7. Sodium	0.01
	100.00

§ This category is estimated as 20% of the annual labor charge plus 20% of the annual charges for replacement of all capital except construction overhead, and preoperation costs.

|| Taken as 15% of labor and 5% of total materials.

TABLE 4.12. SUMMARY OF CAPITAL AND OPERATING COSTS FOR WASTE DISPOSAL

	Partial-capacity Operation	Full-capacity Operation
Capital cost, \$	1,236,000	2,051,900
Annual operating cost, \$	165,900	386,200

TABLE 4.13. CAPITAL COST OF WASTE-DISPOSAL COMPONENTS AND LIFETIMES WITH PARTIAL CAPACITY
(For Plant Operation Only at Rate Needed for One Reference Reactor)

	Total Cost, \$	Items of Various Lifetimes, \$		
		30 years	15 years	5 years
<i>General</i>				
Stack	458,000	366,400	91,600	
Liquid storage tanks	370,100	351,600	18,500	
Waste evaporator	20,000	2,000	18,000	
Lagoon	29,500	29,500		
Canal*	331,000	304,000	25,000	
<i>Auxiliary Equipment</i>				
	27,400		27,400	
Total	1,236,000	1,053,500	180,500	

* Breakdown of cost of the initial cost of the cooling canal for interim storage of waste is as follows:

Excavation	\$ 2,600
Concrete	44,000
Epoxy lining	14,000
Partitions	25,000
Cranes	140,000
De-ionizer	2,400
Monitors	3,000
Building	80,000
Cooling system	20,000
	\$331,000

TABLE 4.14. ANNUAL OPERATING COSTS FOR WASTE FACILITY WITH PARTIAL CAPACITY
(For Plant Operation Only at Rate Needed for One Reference Reactor)

Labor	
Direct operating labor (3 men at \$12,000/yr)	\$ 36,000
Indirect labor*	18,000
Subtotal	\$ 54,000
Waste containers	11,800
Burial ground—slightly active waste	5,000
Shipping of waste to ultimate storage	24,700
Ultimate storage costs	43,700
General operating and maintenance†	14,600
General overhead and administrative expense‡	12,100
Total	\$165,900

* The number of men at indirect labor is prorated at $\frac{1}{3}$ the number at direct labor. The average annual labor allowance at indirect labor is taken as $\frac{3}{2}$ that at direct labor.

† This category is estimated as 20% of the annual labor charge plus 20% of the annual charges for replacement of all capital except construction overhead and preoperation costs. Includes utilities.

‡ Taken as 15% of total labor, materials, and utilities.

TABLE 4.15. TOTAL INVESTMENT AND ITS ANNUAL RETURN—PARTIAL CAPACITY*
(Depreciation-free Basis)

	Cost, \$			
	Pyrochemical	Fabrication	Waste Facility	Total
Plant facilities	4,344,100	5,823,400	1,236,000	11,403,500
Construction overhead†	1,086,000	1,455,800	309,000	2,850,900
Pre-operation and startup cost‡	280,000	850,100	41,400	1,171,500
Working capital§	441,500	971,200	95,000	1,507,700
Total investment	6,151,600	9,100,500	1,681,400	16,933,600
Annual charges on investment, 13% re- turn	800,000	1,183,100	218,600	2,201,700

* Capacity represented by rate needed for one reference reactor.

† Includes contractor's overhead, engineering, fees, insurance, and site improvement, the total calculated at 25% of the investment in facilities proper.

‡ A one-time cost estimated at 3 months total operating cost.

§ Includes inventories of supplies, spare parts, cash on hand, the total calculated at 5% of the initial plant facilities plus 20% of the annual operating cost.

|| Depreciation-free basis. Includes local taxes, federal income tax, property insurance, and 6.75% return on investment.

TABLE 4.16. TOTAL INVESTMENT AND ITS ANNUAL RETURN—FULL CAPACITY
(Depreciation-free Basis)

	Cost, \$			
	Pyrochemical	Fabrication	Waste Facility	Total
Plant facilities	4,631,400	5,823,400	2,051,900	12,506,700
Construction overhead*	1,158,000	1,455,800	513,000	3,126,800
Pre-operation and startup cost†	666,500	2,196,200	96,600	2,959,300
Working capital‡	764,800	2,048,200	179,800	2,992,800
Total investment	7,220,700	11,523,600	2,841,300	21,585,600
Annual charges on investment, 13% re- turn§	938,700	1,498,100	369,400	2,806,200

* Includes contractor's overhead, engineering, fees, insurance, and site improvement, the total calculated at 25% of the investment in facilities proper.

† A one-time cost estimated at 3 months total operating cost.

‡ Includes inventories of supplies, spare parts, cash on hand, the total calculated at 5% of the initial plant facilities plus 20% of the annual operating cost.

§ Depreciation-free basis. Includes local taxes, federal income tax, property insurance, and 6.75% return on investment.

TABLE 4.17. ANNUAL CHARGES FOR CAPITAL REPLACEMENT WITH PARTIAL CAPACITY

Depreciation Schedule, years	Initial or Replacement Cost of Depreciable Items, \$		
	Pyro-chemical	Fabrication	Waste Disposal
5	512,600	1,765,000	—
15	1,718,400	1,943,400	180,500
30*	3,479,100	4,410,900	1,055,500
Total annual charge†	197,600	436,500	22,900

* Includes building and equipment cost with 30-year life and construction overhead and preoperation cost from Table 4.15.

† Annual charges for sinking funds are based on 6.75% interest compounded annually.

TABLE 4.18. ANNUAL COSTS FOR FUEL-CYCLE FACILITY WITH PARTIAL CAPACITY
(For Plant Operation Only at Rate Needed for One Reference Reactor)

	Annual Operating Cost, \$	Annual Charges for Capital Replacement, \$	Annual Return on Total Investment, \$	Total Cost, \$
Processing	1,121,000	197,600	800,000	2,118,600
Fabrication	3,400,300	436,500	1,183,100	5,019,900
Waste facility*	165,400	22,900	218,600	406,900
Total	4,686,700	657,000	2,201,700	7,545,400

* Includes main exhaust air stack for integral fuel-cycle facility.

TABLE 4.19. ANNUAL COSTS FOR FUEL-CYCLE FACILITY WITH FULL CAPACITY

	Annual Operating Cost, \$	Annual Charges for Capital Replacement, \$	Annual Return on Total Investment, \$	Total Cost, \$
Processing	2,666,000	252,900	938,700	3,857,600
Fabrication	8,785,000	451,400	1,498,100	10,734,500
Waste facility*	386,200	38,500	369,400	794,100
Total	11,837,200	742,800	2,806,200	15,386,200

* Includes main exhaust air stack for integral fuel-cycle facility.

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5.0. Fuel-cycle Cost Analysis

5.1. ANALYSIS FOR THE REFERENCE REACTOR

Studies of the fuel cycle of the reference reactor were based on equilibrium conditions. The reactor, consisting of six identical modules in a ring arrangement, was refueled according to burnup requirements set for the core, inner-radial-blanket, and outer-radial-blanket regions. The core region has two zones, which are characterized by different concentrations of fissile plutonium. One-third of the core fuel is removed each time during refueling. This also removes the associated axial blankets, since the upper and lower axial-blanket fuel are in the same cladding tube as core fuel. The two radial-blanket zones surrounding each module are characterized by different volume fractions of uranium within each zone. One-sixth of the inner radial blanket is replaced at the core reloading time and one-ninth of the outer radial blanket is replaced at this time. The outer radial blanket consists of two rows of subassemblies; the outer row of this blanket progresses to the inner row as burnup is accrued. The spent fuel assemblies are transported to the processing-fabrication facility, which is located on the same site as the power-generating unit.

The equilibrium fuel-cycle costs were calculated using a model which accounted for the times of expenditure and of income. All the expenditure and income were then adjusted to a reference time. The purpose of using this approach was to make a detailed accounting of working capital. This detailed accounting is considered appropriate because one of the largest components of cost involved in the fuel cycle is due to working capital when an annual charge rate of 9% is used. The main component in the working-capital account is the value of the plutonium inventory. All plutonium is taken into account; this includes an equilibrium quantity of bred plutonium.

The model assumes that at the start of a cycle the operator is charged with the fuel "received in the reactor" from the previous cycle. The amount of fuel received in the reactor plus the amount purchased for insertion into reactor constitutes a reactor loading. The fuel received in the reactor is half burned up, i.e., one-half the cost of fabrication has been recovered. The plutonium in the blankets and core plutonium constitute an asset; the value is based on the unit sale value of Pu , but this value is altered by a processing cost.

In the scheme of presenting numbers, the plutonium losses are treated as physical losses, even though they may in part be only economic losses. Since the Pu loss is treated as a physical loss, plutonium bred is debited.

The reference design condition was based on the use of V-20 Ti cladding and 6 heavy atom % burnup of core fuel. The technical characteristics for this reference design are given in Table 5.1. Cost factors and unit energy costs are given in Table 5.2 for the various cases calculated. For an 80% capacity factor, 9% per annum charge on working capital and \$10/g fissile plutonium, the reference unit energy cost is 1.0 mill/kW-hr.

One of the items of note is that for the range of \$6-10/g in plutonium price, the cost of energy does not vary significantly for high capacity factors and 9% per annum charge on working capital. This is due to the combination of high breeding ratio and high specific power. For the cases of 4¾% per annum on working capital (this rate is used only to provide comparative information to some other 1000-MW(e) studies), the cost of energy decreases with increasing price of plutonium.

The first three columns in Table 5.2 show the influence of annual plant-capacity factor on energy cost without changing unit costs for fabrication or processing of fuel. Unit costs will, however, change, as illustrated by the estimate made in the fourth column; the magnitude of change is not large. These changes in unit cost come about because it is assumed that the fuel-processing facility is a captive of three reactor plants. The decrease in cost is estimated on the basis that throughput may be increased with the only charges being those assigned to labor and materials (these are held constant in terms of \$/kg of heavy atoms).

One of the interesting cost observations of this study results from considering incremental fuel costs. If this total facility were owned by a utility, the incremental cost of running the facility would be that assigned to labor and materials and would exclude charges on capital. In the present study, the processing-fabrication facility has sufficient capacity so that the equipment is used with about an 80% use factor. Calculations involving greater use of these facilities result in incremental fuel costs that are very small or sometimes, depending upon assumptions, negative.

In the fabrication cost, the cost of the beryllium

TABLE 5.1. CHARACTERISTICS OF REFERENCE REACTOR (6 a/o BURNUP) USING CLOSE-COUPLED PYROCHEMICAL PROCESSING FACILITIES

Reactor power, MW(t)	2510
Net output, MW(e)	1000
Net plant efficiency, %	39.8
U + Pu in core, metric tons	17.94
U in axial blanket, metric tons	16.50
U in inner radial blanket, metric tons	14.87
U in outer radial blanket, metric tons	47.4
U + Pu in reactor, metric tons	97.0
Pu _f in core (avg), metric tons	2.49
Pu _f in reactor (avg), metric tons	3.41
Reload increment of U + Pu, metric tons	19.2
Reload increment of Pu _f , metric tons	0.89
Reload interval at 80% plant factor, days	206
Core burnup of U + Pu (avg), a/o	6
Burnup of discharged fuel including blanket (avg), MWd/Mt	21,400
Pu _f bred for cycle (net), g/MWd	0.445
Breeding ratio	1.48
Pu _f losses, %	
of fuel charged	$\frac{3}{4}$
of fuel discharged	$\frac{3}{4}$
Core and axial-blanket reload fraction	$\frac{1}{3}$
Inner-radial-blanket reload fraction	$\frac{1}{6}$
Outer-radial-blanket reload fraction	$\frac{1}{6}$

TABLE 5.2. EQUILIBRIUM FUEL-CYCLE COSTS FOR REFERENCE CONDITIONS (6 a/o BURNUP; CLOSE-COUPLED PROCESSING FACILITIES SERVING THREE 1000-MW(e) PLANTS)

Annual capacity factor, %	70	80	90	90
Core and axial blanket fabrication cost, \$/kg	150	150	150	144
Inner radial blanket fabrication cost, \$/kg	150	150	150	144
Outer radial blanket fabrication cost, \$/kg	75	75	75	72
Chemical processing cost, \$/kg	60	60	60	58

For 9% Per Annum Charge on Working Capital, mills/kW-hr
(\$10/g Price of Fissile Plutonium)

Fabrication	0.628	0.628	0.628	0.604
Chemical processing	.292	.292	.292	.282
Pu _f credit	(0.466)	(0.466)	(0.466)	(0.466)
Working capital	.643	.565	0.508	0.508
Total	1.10	1.02	0.96	0.93
\$ 6/g, Price of Pu _f	1.05	1.00	0.96	0.93
\$10/g, Price of Pu _f	1.10	1.02	0.96	0.93
\$16/g, Price of Pu _f	1.17	1.05	0.96	0.93

For 4 $\frac{3}{4}$ % Per Annum Charge on Working Capital, mills/kW-hr

\$ 6/g Pu _f	0.85	0.83	0.81	0.78
\$10/g Pu _f	0.79	0.75	0.72	0.69
\$16/g Pu _f	0.70	0.64	0.59	0.56

used between the core and radial blanket is included. In terms of unit electric energy, this beryllium cost amounts to about 0.02 mill/kW-hr. Also, in the processing cost, the cost of waste handling and disposal, including permanent disposal, is included. This waste handling and disposal cost amounts to about 0.05 mill/kW-hr.

The calculations of fuel-cycle costs are based on equilibrium conditions. The beginning and end effects are neglected, as are variation in costs and use factor over the life of the plant. Using a model for estimating beginning and end effects consistent with the estimate of equilibrium costs results in an increase of 0.05 to 0.1 mill/kW-hr in the cost of energy.

From an engineering point of view, an estimate of reasonable limits for the fuel-cycle cost should be stated. This requirement, however, is difficult to satisfy, not only because of the many factors involved, but because of the many alternatives that are possible in design and in parametric relationships. Realistically, it appears that at this stage of development of fast reactors the meaningful limits are sufficiently far apart that no decision can be drawn from the statement of them.

5.2. ANALYSIS FOR ALTERED DESIGN CONDITIONS

Metallic fuels may be underrated in the reference design. In order to indicate the relation between fuel-cycle cost and design change, some additional calculations were made. Two cases are tabulated: an increase to 7 a/o burnup in the core, and a change in cladding material at 7 a/o burnup. In order to indicate the influence of another cladding material on the fuel-cycle costs, calculations were made for the case of stainless steel, which required that zirconium be added as an alloying agent for the fuel material. It should be noted that this design using stainless-steel cladding is not set forth as a well-defined engineering alternate, but is only to indicate the fuel-cycle costs if the fuel were capable of the specified performance. As engineering information becomes available from fuel experiments with stainless-steel cladding for metal alloys, it will be possible to define the engineering aspects. Some of the problems involved with the use of this cladding are the same regardless of fuel type, that is, radiation damage that could limit the attainable burnup for oxide fuels would also limit the burnup for metallic fuels. The limits might, however, be different for the two fuels.

Although some engineering aspects were considered, this section will mainly deal with costs. The technical characteristics of Case I, reference design using V-20 Ti cladding at 6 a/o burnup; Case II, using

V-20 Ti cladding at 7 a/o burnup, and Case III, using stainless-steel cladding at 7 a/o burnup, are given in Table 5.3. The results of the cost calculations are given in Table 5.4.

With 80% capacity factor, 9% per annum charge in working capital, and a \$10 per gram plutonium price, the cost of energy is decreased by 0.1 mill/kW-hr by increasing the burnup from 6 a/o to 7 a/o. Also, the cost of energy is decreased by 0.1 mill/kW-hr by exchanging the V-20 Ti cladding for stainless steel at 7 a/o burnup. Approximately, this indicates that if 7 a/o burnup were attainable from V-20 Ti and only 6 a/o burnup from stainless steel, the energy costs would be equal.

In Table 5.3, the doubling times for the various fissile plutonium inventories for the three cases are given. These geometric-equilibrium doubling times are, of course, only indicative on a relative basis. The more meaningful doubling time is that interval required to double the fast reactor capacity in an electrical system by the accumulation of sufficient bred plutonium; this would include start-up effects of some plants as well as equilibrium conditions for other

TABLE 5.3. CHARACTERISTICS OF METAL-FUELED PLANTS WITH NET PRODUCTION OF 1000 MW(e)

	Case 1	Case 2	Case 3
Core burnup (avg), a/o	6	7	7
Cladding material	V-20 Ti	V-20 Ti	SS
U + Pu + FP in core, MT	17.94	17.94	16.0
Core fuel alloy material	Ti	Ti	Zr
Alloy content of core fuel material, a/o	9	9	25
Expanded fuel density, g/cm ³	12	12	11.8
Core reload fraction	$\frac{1}{3}$	$\frac{1}{3}$	$\frac{1}{3}$
Reload interval at 80% C.F., days	206	240	213
Core conversion ratio	0.64	0.63	0.58
Breeding ratio	1.48	1.46	1.40
Average Pu _f in core, kg	2490	2520	2430
U + Pu + FP in axial blanket, MT	16.5	16.5	16.0
Axial blanket alloy material	None	None	Zr
Axial blanket alloy content, a/o	—	—	10
U + Pu + FP in radial blanket, MT	62.3	62.3	60.5
Radial blanket alloy material	None	None	Zr
Radial blanket alloy content, a/o	—	—	10
Average Pu _f in reactor, kg	3407	3550	3310
Reactor geometric doubling time at 80% C.F., yr.	7.4	8.1	8.8
System geometric doubling time at 80% C.F., yr.	8.6	9.2	10.3

TABLE 5.4. EQUILIBRIUM FUEL-CYCLE COSTS FOR METAL-FUELED REACTORS WITH CLOSE-COUPLED PROCESSING FACILITIES SERVING THREE 1000-MW(e) PLANTS

Fixed Parameters Annual capacity factor, % Chemical processing costs Annual charge on working capital, %	80 \$60/kg of heavy atoms 9		
	Case 1	Case 2	Case 3
	6 V-20 Ti 130	7 V-20 Ti 130	7 SS 90
Core Burnup (avg), a/o			
Cladding material			
Average fabrication cost, \$/kg			
Unit Energy Cost, mills/kW-hr			
Fissile Pu price \$10/g			
Fabrication	0.628	0.540	0.392
Chem. processing	.292	.250	.267
Pu _f credit	(0.466)	(0.447)	(0.379)
Working capital	.565	.575	.520
Total	1.02	0.92	0.80
\$ 6/g Pu _f	1.00	0.88	0.75
\$10/g Pu _f	1.02	0.92	0.80
\$16/g Pu _f	1.05	0.97	0.87

plants. Albeit, the geometric doubling times in the table show that there is a noticeable increase in doubling time from Case 1 to Case 3.

The costs of the stainless-steel cladding and the V-20 Ti cladding were estimated using the same method. This general method is contained in Ref. 5.1. These unit costs are based on a well-developed fast reactor industry but on a throughput scale associated with three 1000-MW(e) reactor plants.

At this point, it should be re-emphasized that no optimization was involved in the selection of these alternatives and very little, in fact, for the reference design. Therefore, no strong cost conclusions or cost trends in relation to specific design parameters can be drawn. If fission gases are released by the metallic fuels, as is indicated by some recent experiments, reactor designs having burnups of 8 to 10 a/o in the core fuel may be possible without major changes in core composition or size. Thus, metallic fuels may meet or better competitive fuel types, from a standpoint of fuel-cycle costs, while offering the performance characteristics to satisfy plant-operation requirements and safety criteria.

REFERENCES

- 5.1. J. E. Ayer, D. A. Jones, D. D. Ebert, T. A. Buczwinski, and J. V. Sana, *A Cost Estimate for Remote Refabrication of Metallic Fuels*, ANL-6950 (to be published).

6.0. Appendices

6.1. PHYSICS CALCULATIONS

6.1.1. Auxiliary Spherical Calculations

Calculations of some of the neutronic characteristics of the systems using spherical geometry were made to further confirm results of cylindrical calculations and to study additional effects. Breeding characteristics, sodium void, Doppler coefficients, various reactivity coefficients of expansion, and effects of varying constituents of the system—including the fuel-alloy composition and varying blanket geometries—were determined.

The spherical calculations were performed using the 840-liter core volume and the same thicknesses of blanket regions as in the cylindrical studies. Compositions are as in Table 6.1.1 except that, where indicated, titanium atoms replace fissium atoms and zirconium is removed.

BREEDING CHARACTERISTICS

The breeding ratio of the modular systems fueled with titanium alloy and without beryllium at the core-blanket interface was estimated. Calculations in spherical geometry were performed for cores separately surrounded by radial and by axial blankets (Table 6.1.1, Examples 54 and 57). A third calculation with a thick blanket was made (Example 51). Weighting each of these by the area of the core surface facing each (37 % axial, 31.5 % radial, thin blanket, 31.5 % radial, thick blanket), the overall breeding ratio is 1.62, which compares favorably with values obtained for cylindrical geometry.

The effects of using different diluents in the fuel alloy were studied; the atom densities of uranium and plutonium were not varied. Example 48 shows the effect of using the same atom fraction of fissium instead of titanium—a decrease in breeding ratio of .07. Another variation was to increase the titanium concentration of the fuel from 8.9 to 30 a/o at the expense of void space, as might have to be done to improve the burnup capability. This change has only a slight effect on the breeding (Example 61). A third variation was to replace the 8.9 a/o titanium by 22 a/o zirconium (Example 62). This also produces a negligible change. In Example 60, carbon was added in a ratio to heavy atoms of 1:1. This shows that the use of carbide fuel would decrease breeding by .10.

The effect of variations of blanket thickness was

studied by examples in which the outer radial blanket was increased to 46.0 cm (Examples 51, 52, and 53). This provides an estimate of the breeding that can be expected in the regions where the modules are coupled, and it shows the improvement in breeding characteristics resulting from the coupling.

SODIUM-VOID COEFFICIENTS

The results of voiding 40 % of the core sodium are shown for the examples in Table 6.1.1. Comparison of Examples 54 and 48 shows that the use of fissium in the fuel alloy rather than titanium results in a sodium-void coefficient that is substantially less negative. The difference presumably results from the increased change in fissium captures due to spectral shift, and it therefore indicates that with fissium present the positive contribution from the central portion of the core is significantly larger than with titanium.

DOPPLER COEFFICIENTS

The Doppler coefficients were computed by substituting cross-sections for U^{238} corresponding to 1500°K for the usual ones corresponding to 750°K in the core. The resulting changes in k were converted to $T(\delta k/\delta T)_{Doppl}^{102}$, assuming that this quantity is independent of temperature. The results are listed for most of the examples in Table 6.1.1.

The reference system without beryllium (Example 54) has a relatively small negative Doppler coefficient.

The enhancing of this prompt negative reactivity feedback by regions of beryllium was also studied. As in the cylindrical studies, it was found that a zone of beryllium between the core and blanket would enhance the Doppler effect (compare Example 48 to Example 46) and have little effect on the sodium-void coefficient. In the thickness used in Example 46 it also produces a desirable power flattening at the edge of the core. Other calculations showed that the beryllium also would reduce the net leakage from the blanket, but at the same time it would reduce the overall breeding ratio.

The homogeneous dispersal of beryllium in the core to increase the Doppler effect was also calculated using the concentration necessary to obtain the same Doppler coefficient as in Example 46. As the beryllium was added the core fuel concentration was decreased, volume for volume, but the total core volume increased to retain the same number of heavy atoms as in Example 46. The results of doing this are shown in Example 81.

TABLE 6.1.1. AUXILIARY SPHERICAL-GEOMETRY CALCULATIONS

	Inner Be	Inner Blkt	Outer Blkt	Be Re- flector	SS Re- flector	Crit* Conc	C.C.R.†	T.B.R.‡	Na§	Doppler	Leakage from Blanket	Max/avg Core- Source Ratio
	cm	cm	cm	cm	cm							
46 Reference with radial Be	5.3	6.1	23.0	15.0	37.0	.18	.67	1.40	-.17	-.45	.03	1.58
47 Axial blanket			33.0		55.0	.18	.62	1.40			.08	1.77
			Axial		Axial							
48 Radial blkt (no inner Be)		6.1	23.0	15.0	37.0	.18	.62	1.57	-.08	-.17	.05	1.77
49 No. 46 with void instead of Fissium	5.3	6.1	23.0	15.0	37.0	.17	.73	1.48			.03	1.51
50 No. 46 with Ti instead of Fissium	5.3	6.1	23.0	15.0	37.0	.17	.73	1.48	-.32	-.65	.03	1.52
52 No. 48 with 46 cm blkt		6.1	46.0	15.0	37.0	.18	.63	1.70			.01	1.76
53 No. 46 with 46 cm blkt	5.3	6.1	46.0	15.0	37.0	.18	.67	1.46			.01	1.58
54 No. 48 with Ti instead of Fissium		6.1	23.0	15.0	37.0	.17	.67	1.64	-.30	-.24	.05	1.76
55 No. 48 with U + Pu instead of Fissium		6.1	23.0	15.0	37.0	.16	.73	1.67	-.17	-.23	.05	1.78
57 Axial blkt: Ti instead of Fissium			33.0		55.0							
			Axial		Axial	.17	.67	1.47			.08	1.76
56 No. 50 with twice normal F. P. pairs	5.3	6.1	23.0	15.0	37.0	.17	.71	1.45	-.27	-.56	.03	1.55
59 No. 54 with no outer Be		6.1	23.0		37.0	.17	.67	1.55			.08	1.75
60 No. 54 with carbon in core; ratio to U + Pu, 1:1		6.1	23.0	15.0	37.0	.17	.72	1.54	-.16	-.50	.04	1.87
61 No. 54 with 30 a/o Ti in fuel		6.1	23.0	15.0	37.0	.17	.68	1.61	-.26	-.29	.05	1.80
62 No. 54 with 22 a/o Zr in fuel		6.1	23.0	15.0	37.0	.17	.68	1.62	-.18	-.24	.05	1.81
51 No. 54 with 46.0 cm blkt		6.1	46.0	15.0	37.0	.17	.67	1.77	-.27	-.24	.01	1.76
80 9 v/o Be homogenized in core		6.1	23.0	15.0	37.0	.19	.66	1.35	-.37	-1.01	.04	1.92
81 5 v/o Be homogenized in core		6.1	23.0	15.0	37.0	.19	.68	1.48	-.31	-.63	.04	1.85
82 No. 54 with 1500°K U ²³⁸		6.1	23.0	15.0	37.0	.17	.68	1.64	-.25			1.76

$$* \text{ Crit Conc} = \frac{(\text{total Pu}) \text{ in core}}{(\text{total Pu} + \text{U}^{238}) \text{ in core}}$$

$$\dagger \text{ C.C.R.} = \frac{(\text{captures in U}^{238}, \text{Pu}^{240} \text{ and Pu}^{242}) \text{ in core}}{(\text{absorptions in Pu}^{239} \text{ and Pu}^{241}) \text{ in core}}$$

$$\ddagger \text{ T.B.R.} = \frac{(\text{captures in U}^{238}, \text{Pu}^{240} \text{ and Pu}^{242}) \text{ in core and blanket}}{(\text{absorptions in Pu}^{239} \text{ and in Pu}^{241}) \text{ in core and blanket}}$$

§ Reactivity resulting from a 40% reduction in core sodium.

|| Based on reactivity resulting from U²³⁸ Doppler effect in core for temperature change of 750°K of U²³⁸ core cross-section parameters.

6.1.2 Neutronics Calculations Assuming 25 a/o Instead of 10 a/o Ti or Zr in Fuel Alloy

The relative neutronics effects of higher titanium or zirconium content in the fuel alloys have been investigated.

These analyses assumed the 840-liter spherical geometry analogous to those described above, except as indicated below. The regional compositions and dimensions are identical with the Cases No. 50 and 54 of Table 6.1.1. No. 50 differed from No. 54 by having a Be region between core and breeding blankets. The expanded density of the Ti fuel alloy in these two cases was 11.7 g/cm³, with about 8.9 a/o being Ti. For the

present comparison calculations the same expanded densities are not retained and neither are the same atomic percent of Ti. The previously used V-Ti cladding has however been retained and also the time average core burnup fraction (i.e., 2.75% of the heavy atoms are fission-product pairs) for ease of comparison.

The expanded fuel density for the 10 a/o Ti or Zr alloys has been assumed to be 13.2 g/cm³. The alloys containing 25 a/o Ti or Zr have been assumed to have an expanded density of 11.8 g/cm³. For the 10 a/o cases this corresponds to 2.2 w/o and 4.1 w/o for Ti and Zr respectively. For the 25 a/o cases this corresponds to 6.3 w/o and 11.3 w/o Ti and Zr respectively in the alloys.

TABLE 6.1.2. SPHERICAL GEOMETRY CALCULATIONS INTERCOMPARING 10% AND 25% Ti OR Zr ALLOYS

Case	Inner Be	Inner Blkt	Outer Blkt	Be Re-flector	SS Re-flector	Crit.* Conc	C.C.R.†	T.B.R.‡	Na§	Doppler
87 No. 50 with 10 a/o Ti in 13.2 g/cm ³ expanded fuel	5.3	6.1	23.0	15.0	37.0	0.16	0.80	1.51	-0.17	-0.62
88 No. 54 with 10 a/o Ti in 13.2 g/cm ³ expanded fuel	—	6.1	23.0	15.0	37.0	0.16	0.74	1.66	-0.13	-0.25
89 No. 50 with 25 a/o Ti in 11.8 g/cm ³ expanded fuel	5.3	6.1	23.0	15.0	37.0	0.18	0.71	1.45	-0.35	-0.66
90 No. 54 with 25 a/o Ti in 11.8 g/cm ³ expanded fuel	—	6.1	23.0	15.0	37.0	0.18	0.65	1.61	-0.33	-0.28
91 No. 88 with 3054-liter core instead of 840 liter	—	6.1	23.0	15.0	37.0	0.12	1.07	1.65	+1.04	-0.42
92 No. 90 with 3054-liter core instead of 840 liter	—	6.1	23.0	15.0	37.0	0.13	0.97	1.58	+0.86	-0.49
93 No. 50 with 10 a/o Zr in 13.2 g/cm ³ expanded fuel	5.3	6.1	23.0	15.0	37.0	0.16	0.78	1.50	-0.16	-0.61
94 No. 54 with 10 a/o Zr in 13.2 g/cm ³ expanded fuel	—	6.1	23.0	15.0	37.0	0.16	0.73	1.66	-0.11	-0.24
95 No. 50 with 25 a/o Zr in 11.8 g/cm ³ expanded fuel	5.3	6.1	23.0	15.0	37.0	0.18	0.68	1.43	-0.32	-0.63
96 No. 54 with 25 a/o Zr in 11.8 g/cm ³ expanded fuel	—	6.1	23.0	15.0	37.0	0.19	0.63	1.59	-0.32	-0.25

$$* \text{ Crit Conc} = \frac{(\text{total Pu}) \text{ in core}}{(\text{total Pu} + \text{U}^{238}) \text{ in core}}$$

$$† \text{ C.C.R.} = \frac{(\text{captures in U}^{238}, \text{Pu}^{240} \text{ and Pu}^{242}) \text{ in core}}{(\text{absorptions in Pu}^{239} \text{ and Pu}^{241}) \text{ in core}}$$

$$‡ \text{ T.B.R.} = \frac{(\text{captures in U}^{238}, \text{Pu}^{240} \text{ and Pu}^{242}) \text{ in core and blanket}}{(\text{absorptions in Pu}^{239} \text{ and in Pu}^{241}) \text{ in core and blanket}}$$

§ Reactivity resulting from a 40% reduction in core sodium.

|| Based on reactivity resulting from U²³⁸ Doppler effect in core for temperature change of 750°K of U²³⁸ core cross section parameters.

The results are listed in Table 6.1.2.

For the 10 and 25 a/o Ti alloy, analyses were also carried out for spherical core volumes of about 3000 liters. In these cases the same compositions and blanket thicknesses were retained as for the 840-liter core module cases. Criticality was achieved by varying the enrichment.

6.2 DESIGN CALCULATIONS

6.2.1. Thermal and Hydraulic Analysis

The analysis was based on the reference design described in this report, although some dimensions and compositions have changed slightly since the analysis. The thermal and hydraulic analysis was based on a power production of 2520 MW(t) in a ring-shaped, six-module reactor configuration, requiring a sodium mass flow rate of 1.01×10^8 lb/hr, with average inlet and outlet temperatures of 720°F and 1000°F, respectively. The regional power distribution per module (average equilibrium condition) and other thermal-hydraulic data are summarized in Table 6.2.1.

The core and integral axial blankets of each module

contain 61 hexagonal subassemblies. Each of the 331 pins per subassembly consists of a 34-in. long, 0.150-in. OD U-Pu-Ti alloy pin located between two 13-in.-long depleted uranium pins in a 0.210-in. OD, 0.018-in. wall V-20 w/o Ti tube, in which a measured amount of sodium is added to provide a thermal bond between the fuel and blanket pins and the cladding. The pins are held on 0.270-in. centers with split-tube spacers.

The inner region of the core, fueled with U-13.5 w/o Pu-1.9 w/o Ti, includes the central 37 subassemblies, with the central portion in 13 of these subassemblies required for control purposes. The peripheral row of 24 subassemblies, fueled with U-17.6 w/o Pu-1.9 w/o Ti, comprises the outer region of the core. Beryllium occupies a portion of the area within each of the 24 outer-core and the 30 inner-radial-blanket subassemblies, limiting these subassemblies to a loading of 276 pins, and producing a radial beryllium zone approximately 1.7 in. thick. The pins in the inner-radial-blanket subassemblies have the same pitch-to-diameter ratio as the pins in the core and axial-blanket subassemblies, but are spiral-wire wrapped to promote coolant mixing.

The outer radial blanket is two subassemblies thick

TABLE 6.2.1. REFERENCE REACTOR DATA

Parameter	Core	Axial Blankets	Inner Radial Blanket	Outer Radial Blanket
Power production, %	85.0	4.2	6.4	4.4
MW(t)/module	357.0	17.5	26.8	18.8
No. subassemblies/module	61*	61*	30	62
Pins/subassembly	331	331	276	61
Pin diameter, in.	0.210	0.210	0.210	0.580
Cladding thickness, in.	0.018	0.018	0.018	0.040
Pitch/diameter	1.29	1.29	1.29	1.09
Height, in.	36	13	51.2	43.3
Total pin length, ft	5.42×10^4 *	1.96×10^4 * each	3.53×10^4	1.36×10^4
Heat-transfer surface, sq ft	2.98×10^3 *	1.08×10^3 * each	1.94×10^3	2.07×10^3
Flow area/subassembly, sq in	9.58	9.58	9.37	5.27
Maximum coolant velocity, fps	24.0	24.0	4.0	2.9
Average thermal flux, Btu/(hr) (sq ft)	4.09×10^5 *	2.77×10^4 *	$4.71 (10^4)$	3.10×10^4
Maximum thermal flux, Btu/hr) (sq ft)	6.33×10^5 *	1.71×10^5 *	$1.29 (10^5)$	1.32×10^5
Average linear power, kW/ft	6.59*	0.45*	0.76	1.38
Maximum linear power, kW/ft	10.19*	2.77*	2.08	5.87

* Analysis based on 54.6 power-producing subassemblies in the core of each module.

around the hexagonal periphery of each module except on the two sides of each module that join the adjacent modules. Between modules the outer radial blanket is one subassembly thick. Each of the 62 subassemblies in the outer radial blanket contains 61 pins, which have a cladding OD of 0.580 in., 0.040-in. wall thickness, and a pitch-to-diameter ratio of 1.09 maintained with spiral-wrapped spacer wires.

HEAT-TRANSFER BASIS

1. All heat is generated within the fuel and blanket pins.
2. The heat-generation rate per unit volume within the fuel and blanket pins is uniform across the cross section.
3. Heat transfer from the fuel and blanket pins and through the cladding is by radial conduction only.
4. The U-Pu-Ti fuel and the depleted uranium blankets are fully expanded (in the radial direction) within the cladding.
5. The conductance at the interface between the fully expanded fuel and blanket pins and the cladding is 40,000 Btu/(hr)(sq ft)(°F/ft).
6. A conservative convection coefficient is obtained by applying the Lubarsky-Kaufman correlation, $Nu = 0.625 [RePr]^{0.4}$, to the parallel sodium flow through the core and blanket tube bundles. Considerable effort has been devoted in recent years to the experimental and theoretical investigation of heat transfer for the parallel, turbulent flow of single-phase liquid metals through tube bundles. The results indicate that the heat-transfer coefficients are strongly dependent upon the geometries involved and that the coefficients obtained are considerably higher than for similar conditions within a

circular tube. Because the lower geometrical limitations for most of these correlations is at a pitch-to-diameter ratio larger than those describing the core and blanket regions of this reactor, and because rod bowing and other geometrical asymmetries have been shown to have a very deleterious effect on the average heat-transfer coefficients, the Lubarsky-Kaufman correlation was utilized.

THERMAL PROPERTIES

1. *Sodium coolant.* All sodium properties were obtained from Sittig.^{6.2.1}
2. *V-20 w/o Ti cladding.* Thermal conductivity of the V-20 w/o Ti cladding is given by $K = 10.8 + (4.6 \times 10^{-3})T$, where K has units Btu/(hr) (ft) (°F) and T is in °F.
3. *U-Pu-Ti fuel.* Because high-temperature thermal conductivity data for U-Pu-Ti alloys are quite limited, the thermal conductivity utilized in this analysis was obtained from values reported for U-16 w/o Pu-8 w/o Ti.^{6.2.3} The resulting thermal conductivity of the as-cast fuel is $K = 0.9 + 0.0108 T$.
4. *Blanket material.* The thermal conductivity utilized for the uranium blankets was conservatively determined for a composition of U-10 w/o Pu-3 w/o Fz by approximating its value from values reported for U-10 w/o Pu-10 w/o Fz^{6.2.4} and for U-10 w/o Pu.^{6.2.5} The resulting thermal conductivity for as-cast U-10 w/o Pu-3 w/o Fz is:

$$K = 9.1 + (9.5 \times 10^3) T.$$

5. *Thermal conductivity of expanded fuel and blanket material.* Thermal conductivities of the fully expanded fuel- and blanket-pin materials were related to the as-cast values by the relation,^{6.2.6, 6.2.7}

$$K_{\text{expanded}} = K_{\text{as-cast}} \left[\frac{1 - \mathcal{V}}{1 + \frac{\mathcal{V}}{2}} \right],$$

which was derived for the case where the void volume has negligible thermal conductivity. The void fraction, $\mathcal{V}$, or porosity, is defined as:

$$\begin{aligned} \mathcal{V} &= \frac{V_{\text{expanded}} - V_{\text{as-cast}}}{V_{\text{expanded}}} \\ &= \frac{\rho_{\text{as-cast}} - \rho_{\text{expanded}}}{\rho_{\text{as-cast}}}, \end{aligned}$$

where V is volume and ρ is density. Thus, the thermal conductivity utilized for the expanded fuel ($\mathcal{V} = 0.28$) and the expanded blanket-pin material ($\mathcal{V} = 0.18$) are, respectively:

$$K_{\text{fuel}} = 0.57 + (6.8 \times 10^{-3}) T$$

and

$$K_{\text{blanket}} = 6.85 \times (7.1 \times 10^{-3}) T.$$

CORE AND AXIAL BLANKETS

Figure 6.2.1 illustrates the relative radial power distribution in the core region of each module. The radial maximum-to-average power density ratio, $(\text{ave}_{\text{sub}}/\text{ave}_{\text{core}})_{\text{radial}}$ and radial power density gradient, $(\text{max}_{\text{sub}}/\text{ave}_{\text{sub}})_{\text{radial}}$ in each of the five radial core zones are listed in Table 6.2.2. The maximum-to-average power density ratio of the computed axial power distribution is 1.24, producing a maximum peak-to-average power density ratio of 1.46, which does not include power variations resulting from the changing Pu concentration. The axial power distribution was closely approximated, for the thermal analysis, by a chopped cosine.

Radial and axial maximum-to-average power-density ratios in the upper and lower axial blankets are 1.7 and 2.9, respectively. The axial power distribution was treated as an exponential of decreasing magnitude with increase in distance from the core-blanket interface.

FIG. 6.2.1. Equilibrium Core Radial Power Distribution.

TABLE 6.2.2. RADIAL POWER DISTRIBUTION; CORE AND AXIAL-BLANKET COOLANT REQUIREMENTS

	$\left(\frac{Ave_{sub}}{Ave_{core}}\right)$ Radial	$\left(\frac{Max_{sub}}{Ave_{sub}}\right)$ Radial	$\left(\frac{Max_{sub}}{Ave_{core}}\right)$ Radial	Sodium Velocity, fps	(T_{max}) Clad. Fresh Fuel, °F	"Adjusted" Velocity, fps	(T_{max}) Clad. Fresh Fuel, °F
Center subassembly	1.18	1.00	1.18	26.3	1045	24.0	1071
First row	1.14	1.03	1.17	25.2	1054	23.8	1071
Second row	1.04	1.06	1.10	22.8	1063	22.2	1071
Third row	0.90	1.08	0.97	19.6	1065	19.3	1071
Fourth row	1.02	1.06	1.08	22.2	1072	22.3	1071
				Orificed to produce an average outlet temperature of 1000°F from each subassembly at midlife		Orificed to produce a uniform maximum cladding temperature in fresh subassemblies	

The refueling program proposed for this reactor entails the replacement of $\frac{1}{3}$ of the core subassemblies, $\frac{1}{6}$ of the inner-radial-blanket subassemblies, and $\frac{1}{6}$ of the outer-radial-blanket subassemblies during each refueling shutdown. Because the position of the core subassemblies remains unchanged during their lifetimes the coolant distribution to the core must be adjusted to correspond to less than the maximum power production from each subassembly in order to provide sufficient coolant for the radial blankets. The coolant velocity required to produce an average outlet temperature of 1000°F from the core subassemblies at mid-life is 22.2 fps in the outer region of the core and ranges from 26.3 to 19.6 fps in the inner region of the core. Maximum-to-average values of the power production resulting from the decreasing Pu concentration in the core subassemblies are 1.06 and 1.08 in the inner and outer regions of the core, respectively, and produce an overall peak-to-average power density ratio of 1.55 in the reactor core. No coolant mixing occurs in the core and axial blanket regions; thus the maximum coolant outlet temperature from fresh subassemblies in the core would range from 1012°F in the center subassembly to 1039°F in the outer-core-region subassembly. Maximum cladding temperatures for these power and coolant flow conditions would be 1045°F and 1071°F, respectively, as indicated in Table 6.2.2.

Because the power density gradients, and thus the maximum cladding temperatures, are more severe in the radial-blanket regions than in the core, the core must be over-orificed to provide the additional coolant flow necessary to maintain the maximum cladding temperature in the radial within reasonable limits. The maximum cladding stress in the radial-blanket regions will be considerably smaller than that in the core, owing to the lower burnup and lower pin temperatures. Consequently a higher maximum temperature can be tolerated in the radial blankets than in the core.

The guideline for coolant redistribution within the

reactor was that the maximum cladding temperature in the radial blankets should not exceed the maximum core cladding temperature by more than 40°F. This requirement was met by reducing the coolant velocities within the inner core region to produce a uniform maximum cladding temperature in the core of 1071°F and a maximum sodium outlet temperature of approximately 1039°F. The axial temperature profiles in the hot channel of the core (subassembly with maximum pin center-line temperature) are illustrated in Fig. 6.2.2. With nominal 24 fps flow through the hot channel, the maximum temperatures of the cladding and fuel pin are 1071°F and 1340°F, respectively.

INNER RADIAL BLANKET

The maximum power density in the blanket regions is determined by the combination of the radial and axial effects of flux distribution and the power variation resulting from the increasing Pu concentration. The radial and axial maximum-to-average power density ratios in the inner radial blanket are 1.45 and 1.4, respectively. The maximum-to-average power density ratio associated with the changing Pu concentration is 1.35 for the fraction of subassemblies that have reached discharge (end of lifetime). Thus, the overall maximum-to-average power-density ratio in the inner radial blanket is 2.74.

The pins in the radial blanket subassemblies are spiral-wire wrapped to promote coolant mixing. The effectiveness of coolant mixing was assumed to be sufficient to limit the maximum local coolant temperature to the average local coolant temperature plus 40 % of the amount by which the maximum temperature would exceed the average temperature if no mixing occurred. Temperatures and temperature differences determined on this basis are designated by the notation "60 % mixing", i.e.,

$$T_{\text{"60% mixing"}} (X/L) = T_{\text{aver.}} (X/L) + 0.40 [T_{\text{max}} (X/L) - T_{\text{aver.}} (X/L)]$$

FIG. 6.2.2. Axial Temperature Profiles for Core and Axial Blankets.

The axial temperature profiles in the hot channels of the inner radial blanket are illustrated in Fig. 6.2.3 for nominal flow and "60 % mixing". A coolant velocity of 4 fps is required to limit the maximum cladding temperature to not more than 40°F above the maximum cladding temperature in the core. With nominal flow in the hot channel the maximum temperatures of the cladding and blanket pin are 1111°F and 1122°F, respectively.

OUTER RADIAL BLANKET

The radial maximum-to-average power density ratio in the outer radial blanket is 2.2. Maximum power density gradients of 1.4, 1.7, and 1.3 exist within individual subassemblies in the region where the outer radial blanket is one subassembly thick and in the inner and outer rows of subassemblies, respectively. In the axial direction the 1.55 maximum-to-average power density ratio was treated as a cosine distribution in the 43.3-in. blanket height.

At each refueling shutdown $\frac{1}{9}$ of the subassemblies in the outer radial blanket are replaced. The discharged subassemblies are all removed from the innermost row of the outer radial blanket. These vacancies are filled with subassemblies transferred from the outer row and the fresh subassemblies are then placed in the vacancies

in the outer row. On the average, a subassembly will reside in the peripheral row of the outer radial blanket the first $\frac{4}{9}$ of its lifetime and will then be transferred into the inner row for the remainder of its lifetime. If the position of each outer-radial-blanket subassembly remained unchanged during its lifetime, it would experience a maximum-to-average power density variation of 1.45 due to the increasing Pu concentration. The effect of this blanket management is to reduce the power variations to 1.2 and 1.3 in the outer and inner rows of the outer radial blanket, respectively, which correspondingly reduces the coolant requirements of the outer radial blanket.

Nominal coolant velocities of 2.9, 2.7, and 0.8 fps are required in the single row, inner, and outer row subassemblies, respectively, to limit the end-of-lifetime maximum cladding temperature to 1111°F. The axial temperature profiles in the hot channel of the outer radial blanket are illustrated in Fig. 6.2.4 for nominal flow and "60 % coolant mixing". The maximum temperatures of the cladding and blanket pin are 1111 and 1163°F, respectively.

HOT SPOTS

The hot-spot factors compiled in Table 6.2.3 reflect the maximum effect the variation in each parameter

FIG. 6.2.3. Axial Temperature Profiles for Inner Radial Blanket.

FIG. 6.2.4. Axial Temperature Profiles for Outer Radial Blanket.

TABLE 6.2.3. HOT-SPOT FACTORS

	ΔT				
	Coolant	Film	Cladding	Bond	Pin
Pu variation	1.035	1.035	1.035	1.035	1.035
Density					
Concentration					
Neutron and gamma flux variation	1.10	1.10	1.10	1.10	1.10
Measurement and control of power level	1.05	1.05	1.05	1.05	1.05
Geometrical deviations					
Coolant	1.065				
Film		1.07			
Cladding			1.06		
Bond				1.01	
Pin	1.01	1.01	1.01	1.01	1.01
Statistical factor	1.134	1.137	1.132	1.118	1.118
Coolant maldistribution	1.10				
Plenum flow					
Orifice sizing					
Maldistribution within subassembly					
Heat-transfer coefficient		1.10			
Cladding thermal conductivity			1.10		
Sodium bond conductance				1.15	
Fuel thermal conductivity					1.20
Hot-channel factors	1.25	1.25	1.25	1.29	1.34

would exert on the temperature differences, if manifested independently of all others. The geometrical deviations were calculated on the basis that the fuel and blanket pins would be cast with a diametral tolerance of ± 0.001 in. and that the cladding would be produced with a tolerance of ± 0.001 in. in the ID and a maximum variation of ± 0.001 in. in the wall thickness.

Based on a constant pressure drop,

$$\Delta p = f \frac{L}{D_{eq}} \frac{\rho V^2}{2g_c},$$

through channels of nominal and maximum dimension, with friction factor

$$f = \frac{K}{Re^{0.2}}$$

and convection coefficient

$$h = \frac{K'}{D_{eq}} [RePr]^{0.4},$$

the effects of geometrical deviation on the coolant temperature rise (enthalpy rise) and on the film temperature difference are given by:

$$\begin{aligned} F_{coolant \Delta T} &= \frac{\Delta h_{max}}{\Delta h_{nom}} \\ &= \left(\frac{A_{nom}}{A_{min}} \right) \left(\frac{D_{eq,nom}}{D_{eq,min}} \right)^{2/3} \end{aligned}$$

and

$$\begin{aligned} F_{film \Delta T} &= \frac{(hA)_{nom}}{(hA)_{min,local}} \\ &= \left[\left(\frac{D_{eq,nom}}{D_{eq,min}} \right)^{2/3} \right]^{0.4} \left(\frac{D_{eq,max}}{D_{eq,nom}} \right)^{0.6} \left(\frac{A_{nom}}{A_{min}} \right) \end{aligned}$$

Several of the individual hot-spot factors, including the variation in heat-transfer coefficient and the variation in fuel- and blanket-pin thermal conductivities, reflect the significant conservatism in the values utilized in the thermal analysis and are smaller than might otherwise be considered appropriate. The power production in the hot channel of the core was based on the power produced in a fresh subassembly and the power production in the hot channels of the inner and outer radial-blanket regions was based on the discharge (end-of-lifetime) Pu concentration, thus accounting for the burn-up variation.

The Pu variation, neutron and gamma-flux variation, measurement and control of power level, and geometrical deviation hot-spot factors for each of the five temperature differences were combined in a statistical manner because of the low probability associated with their combined occurrence at one location. Because the uncertainties associated with the coolant maldistribution, heat-transfer coefficient, and the material conductivities are not of a statistical nature, strictly speaking, these values were combined with the statistically combined factors in a multiplicative manner. The resulting overall hot-channel factors for the core and

radial blanket regions are indicated at the bottom of Table 6.2.3.

Figures 6.2.2, 6.2.3, and 6.2.4 illustrate the axial temperature profiles in the hot channels of the core, inner radial blanket, and outer radial blanket, respectively, for nominal flow, with and without hot-channel factors applied. Also indicated in each figure are the cladding and pin hot-spot temperatures determined statistically. It can be seen that the application of the hot-channel factors in Table 6.2.3 yields hot-spot temperature corrections approximately 50 to 60 % larger for the cladding hot spot and approximately 55 to 85 % larger for the pin hot spot than those determined in a statistical manner. Maximum nominal and hot-channel temperatures in the core, inner radial blanket, and outer radial blanket regions of the reactor are summarized in Table 6.2.4.

HYDRAULIC ANALYSIS

The maximum pressure drop within the reactor is determined by the coolant requirements of the hot channel in the core.

Pressure drop through the tube bundle was determined by considering the two distinct flow areas, denoted in Fig. 6.2.5 as A_{primary} and A_{spacer} , as parallel flow channels and applying the following relations:

$$\Delta p = f_{\text{primary}} \left(\frac{L}{D_{\text{eq primary}}} \right) \frac{\rho \bar{V}_{\text{primary}}^2}{2g_c}$$

$$= f_{\text{spacer}} \left(\frac{L}{D_{\text{eq spacer}}} \right) \frac{\rho \bar{V}_{\text{spacer}}^2}{2g_c}$$

and

TABLE 6.2.4. MAXIMUM NOMINAL AND HOT-CHANNEL TEMPERATURES

	Core and Axial Blankets	Inner Radial Blanket "60% Mixing"	Outer Radial Blanket "60% Mixing"
Maximum coolant velocity, fps	24.0	4.0	2.9
Maximum coolant outlet temp, °F			
Nominal flow			
Nominal design	1039	1107	1106
Hot-channel factors applied	1118	1203	1202
Cladding ID			
Nominal flow			
Nominal design	1071	1111	1111
Hot-channel factors applied	1159	1209	1209
Statistical hot spot	1126	1176	1177
Pin center line			
Nominal flow			
Nominal design	1340	1122	1163
Hot-channel factors applied	1523	1223	1280
Statistical hot spot	1439	1187	1227

Fig. 6.2.5. Configuration of Fuel Pins and Spacers.

$$w_{\text{total}} = \rho A_T \bar{V}$$

$$= \rho (A_{\text{primary}})_{\text{total}} \bar{V}_{\text{primary}}$$

$$+ \rho (A_{\text{spacer}})_{\text{total}} \bar{V}_{\text{spacer}}$$

Friction factors for smooth tubes, from Bonilla,^{6.2.8} were increased by 5 % to account for surface roughness of the drawn tubing and correspond to a relative roughness, ϵ/D_{eq} , of approximately 0.0003. Based on a mass flow rate of 3.03×10^5 lb/hr through the core hot channel, with $\bar{V} = 24.0$ fps, $D_{\text{eq primary}} = 0.093$ in., $D_{\text{eq spacer}} = 0.089$ in., $(A_{\text{primary}})_{\text{total}} = 7.5$ sq. in., and $(A_{\text{spacer}})_{\text{total}} = 2.0$ sq. in., the average velocities in the primary and spacer tube flow areas were determined to be 24.2 fps and 23.5 fps, respectively, and correspond to a frictional pressure drop of 83 psi in the 111-in. height between the fuel-element support grid and the upper end of the core and axial-blanket pins.

The core and axial-blanket subassemblies mate with hexagonal-to-circular transition pieces, or adapters, that protrude into the high-pressure plenum. Coolant enters each adapter through the end of the circular extension and also through slots in the wall of the extension. The plenum openings are fitted with sections of pipe of varying height, according to the radial position of the openings, which also extend into the high-pressure plenum. These pipe sections, or skirts, are provided to adjust the flow area of the slots in the subassembly adaptors and thus properly distribute the core and axial blanket coolant.

Plenum-to-lower-axial-blanket pressure drop in the hot channel was determined by considering that the skirts at the central subassembly plenum openings are sufficiently short that their effect on the inlet pressure drop is negligible. The maximum plenum-to-plenum pressure drop in the reactor is approximately 93 psi. The distribution of this pressure loss, in psi, is:

Plenum to lower axial blanket	17
Lower axial blanket	10
Core	27
Upper axial blanket	10
Upper axial blanket to plenum	49
	—
	93

SUMMARY

The principal limitation on the thermal performance of the reference U-Pu-Ti fueled, fast breeder reactor is the allowable cladding temperature, which is a necessary compromise between the competing requirements of high burnup and high coolant outlet temperature. The maximum cladding temperature in the core and radial blanket regions is approximately 1071°F and 1111°F, respectively, at 100% power and nominal flow. The maximum cladding temperature in each region is strongly dependent upon the regional radial power density gradients and the degree of coolant mixing. In the core, inner, and outer radial blanket regions the maximum radial power density gradients are 1.08, 1.45, and 1.7, respectively. The maximum radial power density gradients in the radial blankets are sufficiently severe that the core must be over-orificed to provide the additional coolant necessary to maintain an acceptable maximum cladding temperature in the radial blanket regions.

Average and maximum linear power densities in the reactor core are 6.59 kW/ft and 10.19 kW/ft respectively, at 100% power, with the maximum coolant, cladding, and fuel-pin temperatures in the core hot channel 1039, 1071, and 1340°F, respectively. The fuel-pin temperature at the core hot spot is 1523°F. The average coolant velocity in the core hot channel is 24 fps, producing a plenum-to-plenum pressure drop of approximately 93 psi.

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6.2.2 Stress Considerations for Fuel and Radial-blanket Elements

Design of the U-Pu-Ti fueled, V-20 w/o Ti-clad fuel and axial-blanket elements to achieve high fuel burnup is based on the "strong clad, weak fuel" model, which is described in some detail in Section 6.3.3 and by Blake.^{6.2.9} It is postulated that, as the gaseous and nongaseous fission-product atoms accumulate, the fuel undergoes radial and axial deformation, displacing bond sodium past the upper axial-blanket pin and into the sodium expansion reservoir. Expansion of the fuel continues until the radial (cladding ID) and axial restraints are reached. The pressure transmitted to the cladding then rises at an increasing rate as the volume occupied by the increasing quantity of gaseous fission products shrinks with accumulation of the nongaseous fission products.

The ability to achieve high fuel burnup depends upon the mechanical and physical properties of the cladding. Some properties of the V-20 w/o Ti alloy are indicated in Table 6.2.5.

Few data for the U-Pu-Ti alloys are available. Initial work with a U-Pu-11.8 w/o Ti alloy indicates that this alloy is extremely strong and retains an appreciable strength above 600°C, also becoming appreciably ductile above 500°C. Good compatibility with the V-20 w/o Ti alloy is indicated. In the temperature range 25°C to 750°C the coefficient of thermal expansion for the U-Pu-8.9 w/o Ti alloy is estimated to be $20 \times 10^{-6}/^{\circ}\text{C}$.^{6.2.12, 6.2.13}

Where adequate data are not available, estimates are made:

1. The creep rate of V-20 w/o Ti at 1100°F, as a function of stress, is estimated to be

$$\dot{\epsilon} = 3 \times 10^{-7} \exp (0.23 \times 10^{-3} \sigma),$$

where $\dot{\epsilon}$ is the creep rate, % per hour; σ is stress, psi. This relationship is obtained by interpolation from

TABLE 6.2.5. SELECTED PROPERTIES OF V-20 w/o Ti ALLOY

Tensile strength, psi ^{6.2.10}	Room temp	100,000
	650°C	94,000
	750°C	65,800
0.2% Yield strength, psi ^{6.2.11}	Room temp	90,000
	650°C	65,000
	750°C	54,400
Modulus of elasticity, psi ^{6.2.12}	600°C	17×10^6
Coefficient of thermal expansion, ^{6.2.13} $^{\circ}\text{F}^{-1}$	25°C to 600°C	5.8×10^6
Uniform elongation, % ^{6.2.11}	650°C	15.6

TABLE 6.2.6. MAXIMUM NOMINAL AND HOT-CHANNEL TEMPERATURES (CORE)

	Fresh Subassembly	Subassembly at Discharge
Maximum coolant velocity, fps	24.0	24.0
Maximum coolant outlet temp, °F		
Nominal design	1039	1016
Hot-channel factors applied	1118	1090
Cladding ID		
Nominal design	1071	1039
Hot-channel factors applied	1159	1119
Statistical hot spot	1126	1089
Pin Centerline		
Nominal design	1340	1288
Hot-channel factors applied	1523	1456
Statistical hot spot	1439	1378

data for creep tests performed at 650°C^{6.2.14} plus a datum point at 550°C.^{6.2.15}

2. Exposure of the V-20 w/o Ti cladding to the flowing sodium coolant for 15,000 hr is assumed to produce a loss of strength and depth of corrosion equivalent to the loss of 0.001 in. in cladding-wall thickness.

Among the preliminary guidelines for the core design were (1) a maximum cladding temperature of 1100°F; and (2) a maximum strain due to creep of approximately 1% in the limiting design case. The core hot-channel temperatures for a fresh fuel subassembly and the same subassembly at discharge are given in Table 6.2.6.

Data for the resulting design of the core and axial blanket elements are given in Table 6.2.7. At fuel discharge the maximum cladding temperature exceeds the guideline maximum by less than 20°F. Subsequent calculations indicate that the limiting design creep strain of approximately 1% has also been closely adhered to.

This design provides for a nominal fuel expansion of 6% in the axial direction and 16% (diametral change) in the radial direction, for a total void equal to approximately 40% of the initial volume of a fuel pin. The sodium-expansion volume provided above the upper

axial blanket is approximately a third of the total volume within the element; thus the internal pressure resulting from the inert gas and the hydrostatic head of the bond sodium displaced by the expanded fuel is insignificant.

DEVELOPMENT OF THE MATHEMATICAL MODEL

Approximately 0.27 stable atom of krypton, iodine, and xenon is reduced per fast fission in Pu²³⁹.^{6.2.16} This corresponds to 4.63 cm³ of fission gas at STP per a/o burnup for each cm³ of fuel of 19 g/cm³ density. The pressure-volume relationship for the fission gas is:

$$P_{\text{fission gas}} = \frac{7.86 \times 10^{-5} \rho_{\text{RT}} V_{\text{RT}} (HA) (Bu) T}{V_{\text{fission gas}}}$$

where

- $P_{\text{fission gas}}$ = fission-gas pressure, psia
- $V_{\text{fission gas}}$ = fission-gas volume, cu in.
- ρ_{RT} = density of fuel alloy, g/cm³
- V_{RT} = volume of fuel alloy, cu in.
- HA = % of heavy atoms in fuel alloy
- Bu = % burnup of heavy atoms
- T = fission-gas temperature, °R

The volume occupied by the fission gas, at temperature, can be written as:

$$V_{\text{fission gas}} = V_{\text{cladding}} - V_{\text{fuel alloy}} - V_{\text{nongaseous fission products}}$$

The increase in fuel-alloy volume due to the accumulation of nongaseous fission products is approximately 2.5% per a/o burnup, based on a fuel density of 19 g/cm³. Included as a part of the fuel-alloy volume is a 2% allowance, based on a fuel density of 19 g/cm³, for phase changes and any anisotropic effects that might be manifested in the fuel between 25°C and 750°C. The 19 g/cm³ basis for the effects of phase change and accumulation of nongaseous fission products corresponds to a fuel-alloy density of 18 g/cm³. The fission-gas volume at temperature then is given by:

$$V_{\text{fission gas}} = \frac{\pi}{4} (ID_c)^2 (L_c)_{\text{RT}} (1 + \alpha_c \Delta T_c)^3 - (1.02 + 0.025 Bu) \left(\frac{\rho_{\text{RT}}}{18} \right) \left[\frac{\pi}{4} (OD_f)^2 (L_f)_{\text{RT}} (1 + \alpha_f \Delta T_f)^3 \right],$$

where

- ID_c = inside diameter of cladding
- OD_f = as-cast diameter of fuel pin
- L_c = original length of the cladding
- L_f = original length of fuel pin
- α_c = cladding coefficient of thermal expansion

TABLE 6.2.7. ELEMENT DESIGN DATA

<i>Cladding: V-20 w/o Ti</i>	
Nominal OD, in.	0.210
Inside diameter, in.	0.174 ± 0.001
Wall thickness, in.	0.018 ± 0.001
Time in reactor, hr	15,000
<i>Fuel: U-Pu-8.9 a/o Ti</i>	
Initial diameter, in.	0.150 ± 0.001
Initial length, in.	34 ± 1/8
Nominal final length, in.	36
Theoretical density (RT), g/cm ³	17.5
Initial density (RT), g/cm ³	17.0 ± 0.3
Expanded density (RT), g/cm ³	12

α_f = fuel coefficient of thermal expansion

ΔT_c = temperature rise of cladding

ΔT_f = temperature rise of fuel

The combined pressure-volume relationship for the fission gas is:

$$P_{\text{fission gas}} = \frac{7.16 \times 10^{-3} (Bu) T}{\frac{1}{\rho_{RT}} \left(\frac{ID_c}{OD_{fRT}} \right)^2 \left(\frac{L_c}{L_{fRT}} \right) (1 + \alpha_c \Delta T_c)^3 - \frac{1}{18} (1.02 + 0.025 Bu) (1 + \alpha_f \Delta T_f)^3}$$

ANALYSIS

This analysis is based on a 100% retention of the fission gas within the fuel, and assumes that all gas atoms agglomerate, obeying the perfect-gas relationship. It is further assumed that the fission-gas pressure is transmitted undiminished from the fuel to the cladding. The reported stress levels in the cladding are the tangential or hoop stress values, which are maximum at the cladding ID, as given by:

$$\sigma = P_{\text{fission gas}} (1 + \beta^2) / (1 - \beta^2),$$

where

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

$$\beta = ID_c / OD_c$$

Stress levels in the cladding are calculated for three cases:

1. Average end-of-life condition for all elements at discharge
2. "Homogeneous" condition of element at high fuel burnup
3. "Local" condition of element at high fuel burnup

The stress levels in the cladding of the inner and outer radial blankets for the "local" condition at high burnup are also indicated.

Case 1. Average End-of-Life Condition for all Elements at Discharge

All fuel elements at discharge are treated as one, with no burnup variations recognized between or within the elements. As such, this represents no element within the core but indicates the average stress level in the cladding for the following nominal conditions:

Burnup, heavy a/o	6
ρ_{RT} , g/cm ³	17.0
(ID _c) _{RT} , in.	0.174
(OD _c) _{RT} , in.	0.210-0.002
(OD _f) _{RT} , in.	0.150
(L _c) _{RT} , in.	36
(L _f) _{RT} , in.	34
Average cladding temperature, °F (°C)	905 (485)
Average fuel temperature, °F (°C)	1048 (565)

At the end of life the fission-gas pressure in an average

fuel element of nominal dimensions is 3600 psi, which corresponds to a cladding stress of 20,500 psi.

Case 2. "Homogeneous" Condition of Element at High Fuel Burnup

The "homogeneous" condition of the fuel at high burnup implies uniform axial burnup and uniform fission-gas pressure within the fuel cladding. The maximum temperature of the cladding, occurring at the upper end of the core, will dictate the cladding characteristics and limitations. For this case the fuel pin is assumed to have maximum dimensions and the cladding to have the minimum wall thickness, as follows:

Burnup, heavy a/o	7.1
ρ_{RT} , g/cm ³	17.0
(ID _c) _{RT} , in.	0.174
(OD _c) _{RT} , in.	0.206
(OD _f) _{RT} , in.	0.151
(L _c) _{RT} , in.	36 - 1/8
(L _f) _{RT} , in.	34 + 1/8
Average cladding temperature, °F (°C)	910 (488)
Maximum cladding temperature, °F (°C)	1119 (604)
Average fuel temperature, °F (°C)	1075 (579)

Excluding consideration of elastic strain and strain resulting from primary and secondary creep, the uniform fission-gas pressure for this case is approximately 5350 psi, producing a cladding stress of 32,000 psi at the end of life. At the cladding hot spot the total secondary creep strain, from the stress-strain relationship previously mentioned, is approximately 0.9%.

Case 3. "Local" Condition of Element at High Fuel Burnup

Estimation of the limiting design burnup can be approached on a "local" basis. In this case, a small portion of a fuel element is examined for the local allowable burnup, rather than considering the gross behavior of an entire fuel element. The "local" analysis required a detailed knowledge of the fuel behavior during burnup, but because knowledge of the "local" behavior of the fuel and cladding is not sufficient to precisely define the "local" maximum condition, approximations have been made.

The fuel-element design provides a sodium-expansion volume sufficiently large that the internal pressure resulting from the inert gas and displaced bond sodium is insignificant. Thus, the fuel is relatively free of both radial and axial restraining forces and is free to expand equally in both the radial and axial directions. A fuel element of minimum cladding geometry and maximum fuel-pin geometry permits an axial fuel expansion of 5% and a radial (diametral) expansion of 15%. With directional equality of fuel expansion, significant local movement of fuel can occur in the axial direction.

It is assumed that the conditions at the location where the expanding fuel first contacts the cladding determines the maximum allowable burnup. Considering no mobility of the fission-gas atoms, the relative distribution of the fission-gas pressure is approximated by:

$$\overline{T_{\text{fuel}}(x)} \cos \frac{\pi x}{2(21.955)},$$

where the cosine term describes the axial power variation in the core. This quantity has a maximum midway between the core center plane and the position of maximum fuel central temperature. At this location the cladding- and fuel-average hot-channel temperatures are 995°F and 1252°F, respectively. It is also assumed for the "local" case that the cladding and pin tolerances are accumulated in the most adverse manner and that the fuel has maximum density.

Burnup, heavy a/o	8.3
ρ_{RT} , g/cm ³	17.3
(ID _c) _{RT} , in.	0.173
(OD _c) _{RT} , in.	0.205
(OD _f) _{RT} , in.	0.151
Cladding temperature, °F (°C)	995 (535)
Fuel temperature, °F (°C)	1252 (678)

The fission-gas pressure at the end of life for the "local" condition is approximately 5700 psi, producing a maximum cladding stress of 34,000 psi, neglecting the effects of elastic or creep strain. The total cladding strain due to secondary creep for an end-of-life stress of 34,000 psi in 15,000 hours is approximately 1.1%. The effect of the 1.1% creep strain is to reduce the maximum stress by 10%, indicating the importance of the radial void volume in the design of elements for high fuel burnup. A more realistic appraisal of the end-of-life condition, considering the stress-strain interaction during burnup, yields 32,000 psi for the maximum stress at a total secondary creep strain of 0.8%. In the subsequent discussion the value of 34,000 psi is considered as the maximum pressure stress in the cladding at the limiting fuel burnup.

The radial stress distribution in the cladding, determined from the pressure-tangential stress relationship for a thick-walled cylinder, is indicated in Table 6.2.8. Also indicated is the tangential thermal stress developed in the cladding and the resulting combined stress.

The maximum stress level, appreciably below the yield strength of the V-20 w/o Ti alloy, is relatively unaffected by the thermal stress, or degree of thermal-stress relaxation. Effect of the thermal stress is to shift the maximum stress to the cladding OD, which is approximately 75°F cooler than the cladding ID near the core center plane.

Because the "strong clad, weak fuel" model postulates

TABLE 6.2.8. RADIAL DISTRIBUTION OF TANGENTIAL (HOOP) STRESS

Radius, in.	Pressure Stress, psi	Thermal Stress, psi	Combined Stress, psi
0.0865	34,000	-5,660	28,340
0.0897	32,600	-3,210	29,390
0.0929	31,350	-940	30,410
0.0961	30,200	1180	31,380
0.0993	29,200	3170	32,370
0.1025	28,300	5040	33,340

TABLE 6.2.9. "FUEL-STRENGTH" EFFECT ON THE MAXIMUM CLADDING PRESSURE STRESS

Gaseous Behavior of Fission Gas, % of Theoretical	Maximum Cladding Pressure Stress, psi
100	34,000
90	30,900
80	27,800
70	24,600
50	17,900

no strength of fuel with respect to restraint of the fission gas, the results for the limiting design condition must be regarded as upper limits on the cladding stress level and total secondary creep, subject to downward escalation as additional data on long-term fuel-cladding behavior permit a more realistic appraisal of the limiting condition.

Unlike comparable U-Pu-Fz alloys, the U-Pu-Ti alloys are expected to retain appreciable strength at operating temperatures. Consider that the effect of fuel strength on the transmittal of fission-gas pressure to the cladding and on the cladding pressure stress can be described as an apparent nongaseous behavior of the fission gas. The effect of "fuel strength" on the maximum cladding pressure is indicated in Table 6.2.9.

Inner Radial Blanket: "Local" Condition of Blanket at High Burnup

The "local" condition for the inner and outer radial blankets, like that in the fuel element, assumes combination of the cladding and pin tolerances in the most adverse manner, with fission-gas restraint by cladding of minimum wall thickness, and directional equality of pin expansion.

Burnup, heavy a/o	3.2
ρ_{RT} , g/cm ³	19.0
(ID _c) _{RT} , in.	0.173 (0.174 in. nominal)
(OD _c) _{RT} , in.	0.205 (0.210 in. nominal)
(OD _f) _{RT} , in.	0.157 (0.156 in. nominal)
Cladding temperature, °F (°C)	1088 (587)
Pin temperature, °F (°C)	1125 (607)
Time in reactor, hr	30,000

For this condition, at the end of a 30,000-hour life the maximum pressure stress is 18,000 psi, with a total secondary strain of less than 0.2 %.

Outer Radial Blanket: "Local" Condition of Blanket at High Burnup

Burnup, heavy a/o	2
ρ_{RT} , g/cm ³	19.0
(ID _c) _{RT} , in.	0.499 (0.500 in. nominal)
(OD _c) _{RT} , in.	0.575 (0.580 in. nominal)
(OD _f) _{RT} , in.	0.446 (0.445 in. nominal)
Cladding temperature, °F	1115
Pin temperature, °F	1200
Time in reactor, hr	45,000

The end-of-life maximum stress in the outer-radial-blanket cladding is 11,000 psi, with a total secondary strain of less than 0.1 %. Thermal stress in the relatively thick-walled cladding has a maximum of less than 3000 psi.

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6.2.3. Some Design Considerations and Features of Radial Fuel Movement in EBR-II

The success in operating EBR-II, including fuel changing, means that the basic idea of radial self-locking of subassemblies by suitable choices of their fits and differential expansion is sound. Since the modular design of the reactor considered in this report is an extension of radial self-locking to the general case of materials with different coefficients of expansion, it is relevant to discuss briefly the features of the mono-coefficient case.

The EBR-II is a 73-liter core reactor with a core equivalent radius of 10 in. and blanket radius of 31 in. This system consists of a uniform array of hexagonal tubes, each 2.29 in. across flats and of 0.040-in. wall

thickness. The helical-wire-spaced fuel elements are assembled in the hexagonal tubes with an essentially zero clearance based on undeformed pin-cladding diameter at lines of spacer contact. Blanket elements are assembled in their tubes without spacers. Cladding tubes, helical wire spacers, and hexagonal tubes have the same coefficient of expansion. The "solid" pack of elements is utilized to transmit uniform motion radially through contact between hexagonal tubes at the center of each flat near the core midplane. This localized contact is achieved by buttons a nominal 0.014 in. high. Thus, if the buttons are in contact, the thermal expansion of fuel pin assemblies provides a negative power coefficient of reactivity.

A low power level, which is sufficient to cause slight inward bowing of fuel subassemblies due to the temperature profile radially across the subassemblies, affords contact of the core-subassembly buttons. As power level is increased, the temperature rise of the sodium coolant increases and causes significant differential expansion between core midplane and the subassembly support structure. Thus, the combination of radial coolant-temperature profile and axial temperature rise causes the subassemblies to close the nominal isothermal gap clearance at the buttons of 0.002 in. and take up the clearance in the fit of the subassembly bottom pieces in the lower plate of the support structure; the subassemblies pivot in their seats in the upper plate of this structure. An added factor in determining the effect of the subassembly fit in the lower plate is the slight downward bowing of the large support structure.

Refueling is performed at nearly isothermal conditions of subassemblies and plenum-support structure. Since all these components have the same coefficient of expansion, the uniform temperature level is not critical. The subassemblies are free standing; the one being replaced has a nominal clearance of 0.030 in. after an upward movement of about 3/8 in. corresponding to button diameter.

The location of the buttons at the middle of each flat is a special case of negligible bowing of flats achieved through a combination of hexagonal tube dimensions and a pressure differential of the order of 20 psi. In the general case of higher pressure and larger, relatively thinner-wall hexagonal tubes, buttons, or bosses, near corners would be required to define subassembly radial contact for self locking.

6.2.4. Example of Design for Radial Restraint of Subassemblies

As discussed in the topic on Subassembly Radial Location Plates, the fuel-subassembly centerlines are tilted toward the core centerline by differential ex-

pansion between a pivot plate of titanium and a driver plate of stainless steel. This is to establish positive contact among the fuel subassemblies before the search for criticality begins and to maintain this locking feature through all modes of temperature distribution during reactor operation. As the module is brought up to full power, the unrestrained subassembly hexagonal tubes bow according to the individual patterns of temperature rise of adjacent coolant, and the tubes undergo additional curvature according to their flexures caused by regions of contact. Reduction of curvature may be desirable; the tilt of blanket subassemblies is available for this purpose. It is necessary to know for all temperature regimes what the resultant tube curvatures are in order to establish control-rod and guide diameters for suitable minimum clearances. The forces associated with flexures are required to design welds of suitable area between the hexagonal tubes and the lower adapters contacting the pivot plate and driver plate.

To illustrate the feasibility of radial locking of fuel subassemblies by a pivot and driver-plate combination, the results of an example are given. A set of subassemblies along a radial was chosen for analysis of the effect of increase in tilt with radius and to analyze subassembly curvatures. The radial chosen is along a line of subassemblies that engage at hexagonal tube flats and include the outer control-rod position. The outer rods are symmetrically spaced; thus, the radial chosen is actually one of six with a control rod position and with identical subassembly engagement. Fig-

FIG. 6.2.6. Pattern of Subassemblies in Radial Locking Arrangement.

ure 6.2.6 is a plan view of the subassemblies included in radial locking. Figure 6.2.7 is an elevation view of the arrangement of locking plates and subassembly group considered in the example.

Rather arbitrarily, 420°F was chosen as the temperature at which the centerline points of the driver plate holes would match those in the pivot plate. A set of distances to the core centerline is given in Table 6.2.10 for the matching condition. Also given is the corresponding set for the pivot plate at the coolant inlet temperature for reactor operation of 720°F; this set is used with subassembly tilt to find the dimensions of subassembly centerline to core centerline at core midplane, 3H. In addition, a set of dimensions for the hexagonal tubes at the levels of radial contact has been selected, as shown on Fig. 6.2.8. The subassembly dimension chosen affords a nominal 0.010 in. total clearance at the bosses at the refueling temperature.

With the above sets of dimensions, the coefficients of expansion and a selection of the clearance of the subassembly adapter in the plate holes, an idealized comparison of the subassembly centerline locations can be made at the level of first contact above the pivot plate for different temperature patterns. This level was chosen to be three times the pivot- and driver-plate spacing and at the core midplane. Thus, if a set of temperatures is chosen, the conditions are fixed for obtaining the difference between the dimension found by tilt and that found by subassemblies in contact. If the dimension found by tilt is smaller, then subassembly flexure occurs. Outward thermal bowing reduces the deflection and therefore reduces the associated radial force.

The tilt at the core midplane (3H) for 300°F increases in pivot and driver plate temperature is given in Table 6.2.11 using 10^{-3} in./in. as the differential expansion of the two plates. The tilt with clearance is referred to the pivot-plate-hole centerline so that tilt can be subtracted from this reference (Table 6.2.10) to find subassembly centerline distance to core centerline.

The tilt at several heights has been found using the 0.003 diametral clearance. Distances to core centerline are summarized in Table 6.2.12.

In Table 6.2.13 are the fuel-subassembly centerline distances to core centerline for contact. From previous tables, the distances based on tilt have been obtained and subtracted from contact distance to obtain the deflections based on 0.003 diametral clearance. Deflections with thermal bowing are also given in the table.

The bowing of F_1 , F_2 , and F_3 is based on the temperature difference of streams of sodium flowing adjacent to the hexagonal tube wall, assuming equal volumetric flow of the streams. The variation in temperature rise is due to the difference in fuel heat genera-

FIG. 6.2.7. Example of Radial Locking of Subassemblies: Elevation View and Dimensional Basis (Dimensions in Inches).

TABLE 6.2.10. DISTANCE FROM PLATE-HOLE CENTERLINES TO CORE CENTERLINE (INCHES)

Centerline Designation	Pivot- and Driver-Plate Hole Centerline Dimensions for 420°F	Pivot-Plate Hole Centerline Dimensions for 720°F
F ₁	5.200	5.210
F ₂	10.400	10.421
F ₃	15.600	15.631
F ₄	20.800	20.842
B ₁	26.000	26.052
B ₂	31.200	31.262

FUEL SUBASSEMBLY SIZE ACROSS
BOSSSES AT CORE MIDPLANE (3H)

FIG. 6.2.8. Dimensions for Hexagonal Tubes at Levels of Contact.

TABLE 6.2.11. PLATE-HOLE CENTERLINE DIFFERENCES AND TILTS

Centerline Designation	Hole Centerline Difference	Tilt at 3H	
		Zero clearance	0.003 diametral clearance
F ₁	0.005	0.015	0.005
F ₂	0.010	0.030	0.020
F ₃	0.016	0.048	0.038
F ₄	0.021	0.063	0.053
B ₁	0.026	0.078	0.068
B ₂	0.031	0.093	0.083

TABLE 6.2.12. SUBASSEMBLY TILT CENTERLINE DISTANCE TO CORE CENTERLINE

Subassembly Designation	Vertical Location			
	2H	3H	5H	6-2/3 H
F ₁	5.207	5.205	5.201	5.198
F ₂	10.407	10.401	10.387	10.375
F ₃	15.606	15.593	15.567	15.545
F ₄	20.807	20.789	20.753	20.723
B ₁	—	25.984	25.938	25.900
B ₂	—	31.179	31.123	31.076

TABLE 6.2.13. SUBASSEMBLY CENTERLINE POINT LOCATIONS AND DEFLECTIONS

Subassembly Designation	Subassembly Centerline at 3H-Bosses Touching		Deflections at 3H		
	720°F	860°F	720°F	860°F	
				No bowing	With Bowing
F ₁	5.204	5.208	-0.001	0.003	0.0025
F ₂	10.408	10.416	0.007	0.015	0.013
F ₃	15.612	15.624	0.019	0.031	0.029
F ₄	20.816	20.832	0.027	0.043	0.041

TABLE 6.2.14. BOWING OF SUBASSEMBLIES

	Bowing		
	F ₁	F ₂	F ₃
Power generation relative to core center			
Near inner wall of subassembly	0.995	0.950	0.800
Near outer wall of subassembly	0.950	0.800	0.685
Temperature difference between inner and outer streams for 280°F average rise	12	48	~48
Bowing as departure from subassembly tilt centerline, in.			
At core midplane, 3H	0.0005	0.002	0.002
At 30 in. higher, 5H	0.008	0.032	0.032
At top of hexagonal tube 54 in. above midplane, 6-2/3H	0.022	0.087	0.087

tion across the subassembly. The difference in heat generation, the temperature difference, and bowing as departure from the tilt centerline are given in Table 6.2.14. The basis for estimating bowing is summarized in Figure 6.2.9. The same magnitude and direction of bowing is assumed for the F_4 position as for F_2 and F_3 ; here the difference in temperature rise of sodium streams is attributed to the imprecision in adjusting flow rates to account for high heat generation in fuel elements on the inside and low heat generation in beryllium on the outside.

The total displacement at 5H and 6-2/3H from subassembly tilt centerline is the sum of the thermal bowing and of the displacing due to deflection at 3H. (Blanket subassembly contact is accounted for later.) The added cantilever beam displacement, D , at 5H and 6-2/3H is equal to the deflection at 3H, f_1 , and 1.8 times f_1 , respectively as given on Fig. 6.2.10. The total displacements are given in Table 6.2.15. The displacement at the top of the hexagonal tube, 6-2/3H, is practically the maximum for a scheme in which the shielding pole is curved toward the core centerline just above the transition with the tube. This compensating movement can be achieved with the shield piece fitting

HEIGHT	3H						5H	6-2/3H
Δ	1	2	3	4	5	6	7	8 + ... 12
$\Delta t (^{\circ}\text{F})$	8	16	24	32	40	48	48	48
B for F_3 (mils)	2						32	87

FIG. 6.2.9. Thermal Bowing of Subassembly Hexagonal Tube.

around the pole with, say, 0.005 radial clearance and with the shield piece radially restrained at the upper end by a grid attached to the underside of the top shield.

The displacement from subassembly tilt centerline is used to find the subassembly centerline at four points with respect to a vertical line, e.g., through the center of the pivot-plate hole. The deviation from this reference vertical is given in Table 6.2.16. Also shown in the table are deviations from vertical lines that minimize the extreme deviation. Negative values are

TABLE 6.2.15. DISPLACEMENT OF SUBASSEMBLY

	Displacement			
	F_1	F_2	F_3	F_4
Displacement at 5H				
720°F at 3H	—	0.014	0.038	0.054
860°F at 3H	0.013	0.058	0.090	0.114
Displacement at 6-2/3H				
720°F at 3H	—	0.020	0.053	0.076
860°F at 3H	0.029	0.123	0.168	0.202

TABLE 6.2.16. DEVIATIONS OF SUBASSEMBLY CENTERLINES FROM VERTICAL LINES
(No Reaction with Blanket Subassemblies)

Temperature at Core Midplane, °F	Vertical Location of Point	F_1		F_2		F_3		F_4	
		From pivot-plate hole*	From average of extremes	From pivot-plate hole*	From average of extremes	From pivot-plate hole*	From average of extremes	From pivot-plate hole*	From average of extremes
720	2H near bottom of control guide	-0.003	+0.005	-0.010	+0.008	-0.015	+0.009	-0.021	+0.011
860		-0.002	-0.008	-0.006	-0.042	-0.011	-0.047	-0.015	-0.049
720	3H, core midplane	-0.006	+0.002	-0.013	+0.005	-0.019	+0.005	-0.026	+0.008
860		-0.002	-0.008	-0.005	-0.041	-0.007	-0.043	-0.010	-0.044
720	5H	-0.010	-0.002	-0.020	-0.002	-0.026	-0.002	-0.035	-0.003
860		+0.004	-0.006	+0.024	-0.012	+0.026	-0.010	+0.025	-0.009
720	6-2/3H, top of hexagonal tube	-0.013	-0.005	-0.026	-0.008	-0.033	-0.009	-0.043	-0.011
860		+0.017	+0.007	+0.077	+0.041	+0.082	+0.046	+0.083	+0.049

* Centerline.

for the subassembly centerline points between the vertical line and the core centerline. For the control-rod-bearing fuel subassembly position, F_3 , the extreme deviation is 0.009 in. at zero power and 720°F and 0.046 in. at full power. The radial difference between the vertical lines which give these deviations is 0.060 in. This radial outward movement of the "average" centerline of the control-rod position can be rather closely matched by the top grid expansion. A structure of type 300 series stainless steel locating the control-rod guides at the level of the sodium in the reactor tank expands about 0.045 in. at the F_3 radius of 15 in. from the arbitrary centerline matching the core centerline defined by the center of the pivot plate for a temperature change of 280°F.

The radial distance of the F_2 fuel subassembly closely matches the inner control-rod position with respect to the core centerline even though it is not in the rows considered in the example. Tilt, flexure, and bowing of this control-rod-bearing subassembly can be approximated by the F_2 position. Therefore, curvature and average centerline movement are given below and compared with F_3 values.

	F_2	F_3
Extreme deviation		
Zero power (720°F)	0.008	0.009
Full power	0.041	0.046
Radial movement of centerlines:		
Vertical line through average of extreme points of subassembly centerline	0.054	0.060
Stainless-steel grid at sodium level	0.030	0.045

From the above consideration, it is concluded that the average radial movement of the control-rod position with temperature does not strongly affect the choices of rod-to-guide clearances and straightness tolerances. From the extreme deviation of 0.044, it appears that a radius clearance of 0.060 in. is acceptable even without the subassembly straightening effect of blanket-subassembly tilt.

The example of radial locking of subassemblies by a combination of pivot and driver plate is continued with consideration of the contribution of the first two rows of blanket subassemblies to the straightness of the control-rod-bearing subassembly, F_3 . The arrangement chosen is a set of bosses 30 in. above the core midplane. The bosses on the blanket subassemblies are identical to those on fuel hexagonal tubes at the midplane but those at the higher level on fuel subassemblies are 0.005 in. less. The blanket hexagonal tubes are considered to be straight before contact at bosses. (Thermal bowing is expected to be small for fresh blanket units; increased inward bowing of the beryllium-bearing subassembly will occur as fissioning

of bred plutonium heats the outer wall of the hexagonal tube.)

Three of the fuel subassemblies react against the two blanket units with deflections at 5H as given in Table 6.2.17. The basis for estimating this interaction is given on Fig. 6.2.10. The load per unit deflection of the fuel subassemblies is 7.5 lb per mil compared to 1.07 for the blanket subassemblies. The greater deflection of the latter for the long moment arm is consistent with the greater tilt for the large radii of these subassembly locations.

The inward deflection of F_3 of 0.013 in. becomes about 0.026 in. at the top of the subassembly (Fig. 6.2.10) so that the vertical line averaging extreme deviation is moved inward 0.013 in. The new values of deviations and radial movement of centerline between zero and full power conditions are:

	F_2	F_3
Extreme deviation		
Zero power (720°F)	0.008	0.009
Full power	0.033	0.033
Radial movement of centerlines from zero to full power:		
Vertical line through average of extreme points of subassembly centerline	0.048	0.047
Stainless-steel grid at sodium level	0.030	0.045

The results are also summarized in Fig. 6.2.11, which shows the deviation of the F_3 subassembly centerline from the reference vertical line through the pivot plate hole center for four vertical points, 2H, 3H, 5H, 6-2/3H. The clearance check points are also indicated at 3H and 6-2/3H among the subassemblies contacting at 5H. The deflection of the first row of blanket units is large enough so that these subassemblies do not touch circumferentially.

The results of the example indicate that a variation of 10% in the subassembly tilt centerlines will increase the extreme deviation from the "average" vertical centerline by no more than 0.005 in. for the F_3 position. It appears reasonable to set the tolerances in

TABLE 6.2.17. INTERACTION OF FUEL AND BLANKET SUBASSEMBLIES AT 5H

Subassembly Designation	Deflection	Reaction, lb
F_2	$10.445 + 5.207 - 15.644 = 0.008$	278
F_3	$15.657 - 15.644 = 0.013$	
F_4	$20.867 - (15.644 + 5.207) = 0.016$	
	0.037	
B_1	$25.938 - (15.644 + 10.417) = -0.123$	
B_2	$31.123 - (15.644 + 15.629) = -0.150$	-292
	-0.273

FIG. 6.2.11. Example of Radial Locking of Subassemblies: Four Centerline Point Locations at Full Power for Control-rod-bearing Subassembly, F3.

terms of, say, 10% variation in tilt. For this variation, the tolerances are of the order of 0.001 in. for locating holes in the pivot and driver plates and 0.0005 in. for the diameters of holes in these plates and for the diameters of the lower adapters of subassemblies. The interlocking effect of an array of 127 uniformly spaced and dimensioned hexagonal tubes is to average out variations so that tolerances would likely be more generous than these.

Several additional considerations are related to the concept of radial locking of subassemblies. One is the temperature at which the midplane bosses on fuel subassemblies first touch, since the reactor must be brought up to this temperature by a combination of decay heat of residual fission products, pump power energy, and auxiliary heating. The outside row of subassemblies, typified by F_4 , tilt sufficiently to cause the bosses to touch for a temperature rise of about 150°F (from 420°F to 570°F). Thus, for the bases chosen in the example the reactor power may be used for about half of the temperature rise between refueling and inlet temperature for power operation.

Another consideration is how to minimize thermal bowing. Lateral mixing of sodium by spiral spacers on fuel elements is a popular technique for enhancing the heat removal from a subassembly in a radial heat-generation gradient. A less familiar technique is to wrap spirally an entire assemblage of elements to provide nearly uniform temperature on opposite sides of the hexagonal shroud tube. This feature might be employed above the core over the height of the blanket and gas plena in individual fuel elements to reduce bowing or in the beryllium-bearing fuel subassemblies, F_4 , to minimize bowing.

A further consideration is the variation in stiffness of the subassemblies over the height between reactions. Values of moment of inertia, I , at selected levels are:

	Wall Thickness, in.	Diameter, in.	I , in. ⁴
Hexagonal tube	0.080		5.3
Hexagonal section of adapter	0.25		13.1
Adapter between pivot and driver plates	0.25	4.0	5.2

The flexure of the adapter between plates was not considered in the example. This section of the fuel assembly can be designed to compensate in part for the stiffness of the adapter near the shroud tube joint if desired.

The welding area required for attaching the shroud tube to the adapter appears to be reasonable, since the fiber stresses for a complete circumferential weld are low, as inferred from maximum fiber stress esti-

mates using F_4 cantilever reaction and length corresponding to $3H$:

	Maximum Fiber Stress, psi
Hexagonal tube assumed to extend to pivot plate	7,500
Hexagonal section of adapter	2,500
Round section of adapter	4,000

6.2.5. Experimental Bowing of Hexagonal Tube Flats

Two single sets of radial deflection measurements were made on hexagonal tubes, one on a tube 3.0 in. inside across flats and one on a tube 2.2 in. across. The ends were sealed and the tubes pressurized with water. A micrometer was used to measure the distance between parallel flats at midspan as a function of pressure. A Boice dial contact gage was rested on the tube so as to measure total decrease in distance between the pair of corners joined by a line parallel to the pair of flats. The results are given in Table 6.2.18 and plotted in Figs. 6.2.12 and 6.2.13. The results across corners for the smaller tube are not so meaningful because of difficulty in mounting the dial gage.

The bowing of the flats is not very different from the deflection at midspan calculated for a uniformly loaded beam with fixed ends. The span used in the values of $2f_{max}$ given on the figures was taken as one side of an equilateral triangle with two corners at half thickness of the tube wall. For a 5.0-in. hexagonal tube

TABLE 6.2.18. EXPERIMENTAL DEFLECTIONS, MILS

Pressure, psig	Hex. Tube, 2.962 in. × 0.060 in.		Hex. Tube, 2.210 in. × 0.040 in.	
	Across flats	Across corners	Across flats	Across corners*
0	0	0	0	0
20	1.5	0	0.5	0
40	2.5	0	3.0	0
60	4.5	-0.15	4.5	-0.08
80	6.2	-0.33	6.5	-0.16
100	8.1	-0.58	7.8	-0.33
120	9.5	-0.58	10.3	-0.55
140	11.0	-0.58	11.7	-0.69
160	12.5	-0.68	13.4	-0.76
180	14.0	-0.88		
150		-0.88		-0.76
140	11.0	-0.78		
120		-0.68	10.5	
100	7.5	-0.54		
80		-0.44	7.2	
60		-0.26		
40	2.5	-0.09	3.9	
20				
0	-0.3	-0.01	-0.2	-0.76

* Difficulty in attaching gage.

Fig. 6.2.12. Hexagonal Tube Deflections (3 in. ID Tube).

of 0.080-in. wall at 800°F, the expected bowing would be 17 mils per plate for 100 psi differential; the modulus of elasticity is about 90% of the room-temperature value.

The very small amount of shrinkage across corners has two implications for design. The external areas near the corners can be used for bosses to define the contacts between subassemblies without their positions being sensitive to pressure differential across the tube wall. Secondly, the bowing of the flats is not affected by any restraint acting to prevent corner shrinkage.

The test equipment and methods for gaging the hexagonal tube flexures were provided by Mr. H. W. Ostrander of Central Shops Special Projects Group.

6.2.6 A Control-rod Program and Average Boron-poison Concentration in Core

The upper limit for reactivity changes in a core module and thus the maximum assignment to the

control-rod system is for the condition of zero coupling between modules. This is the basis on which scouting calculations have been made for the reactivity worth of homogeneously distributed boron. Related estimates have been made for: 1. reactivity worth at central axis compared to worth for homogeneously distributed boron; 2. reactivity effect of core expansion; 3. reactivity effect of change in temperature level of fuel (Doppler); and 4. reactivity effect of fuel burnup during interval between refuelings. See Section 3.2. The use of these estimates will be considered first.

The reactivity worth of natural boron distributed in the core has been estimated for two conditions.

Condition 1:

B₄C substituted for sodium
fissium-alloy fuel

one fuel zone

beryllium zone at core-boundary thick blanket

The $-5.4\% \Delta k/k$ for 2 v/o B₄C gives: 2.70
($\% \Delta k$)/(v/o).

Fig. 6.2.13. Hexagonal Tube Deflections (2.2 in. ID Tube).

Condition 2:

B₄C distributed in core
 titanium-alloy fuel
 one fuel zone
 no beryllium zone

The $-0.62\% \Delta k/k$ for 0.222 v/o B₄C gives $-2.80 (\% \Delta k)/(v/o)$. A value of $-2.75\% (\Delta k)/(v/o)$ is assumed for two enrichment zones in the core and beryllium at the radial boundary.

For condition 2 above, the worth of a central B₄C (natural) column substituted for an equal volume of core material was calculated to be $1.30\% \Delta k/k$ resulting in a radial average to maximum of 0.48 (see Section 3.2.9). This ratio is consistent with a $\bar{\phi}^2$ radial variation in reactivity worth of boron for an effective core radius of 52 cm and equivalent bare-core radius of 70 cm. Using this variation, the relative values of two rings of control rods at 22 cm and 40 cm are estimated to be 1.39. The $\bar{\phi}^2$ variation axially results in a ratio of 1.51 as the worth of full-length rods to half-length rods symmetrical about the core midplane. Thus, if the inner ring is composed of half-length rods, individual rod worth can be about 0.9 of the rod worth for full-length rods in the outer ring; boron enrichment and geometrical cross section are held constant. For the arrangement of the same number of rods in each ring, the total reactivity assignment to the control system would be almost equally divided among the rods.

The reactivity assignment is composed of the following reactivity losses and margins:

Shutdown margin with core in its most reactive normal condition	3 $\% \Delta k$
Reactivity decrease from refueling temperature to full-power temperature pattern	0.5
Reactivity decrease over interval between refuelings	3.0
Reactivity compensated by partially inserted rods at end of loading interval	0.2
	<hr/>
	6.7% Δk

The shutdown margin at the beginning of the search for criticality is $-3.15\% \Delta k$ since the reactor is heated about 80°C to lock the fuel subassemblies together radially.

From the rod arrangement and reactivity requirement given above, the individual rod assignment is $0.53\% \Delta k$ for the inner ring of six half-length control rods and $0.59\% \Delta k$ for the outer ring of six. The core axis position is reserved for a regulating rod.

A sequence of control-rod movements is rather arbitrarily chosen to provide a basis for estimating the average boron content of the core over the interval between refuelings. The program is as follows:

Rod movement to criticality

Quick-action safety rod withdrawal

Two inner rods are moved, each in two reactivity steps, to positions leaving 0.1% Δk in each rod

Reactivity added to core 0.86% Δk

Outer rod withdrawals

The six outer rods are moved to the upper half of the core, each adding 0.3% Δk

Reactivity added to core 1.80

Inner rod withdrawals

The four inner rods are moved to the upper half of the core, each adding 0.12% Δk

Reactivity added to core 0.48

3.14% Δk

Rod movement to obtain full power level at temperature

Outer rods each moved 0.06% Δk to compensate for 0.35 to 0.36% Δk

Rod movement to maintain operation overloading interval by complete withdrawal of each rod in turn

Outer rods each moved 0.23% Δk

Reactivity compensation 1.38% Δk

Four inner rods each moved 0.41%

Reactivity compensation 1.64

3.02% Δk

A linear change in reactivity with burnup is assumed to find the time average length of control rod in the core.

Outer rod, average length	24 in.
Inner rod, average length	
Four rods used for burnup	20 in.
Two rods used for fast safety	16 in.
	<hr/>
	60 in.

The natural boron cross-sectional area of 14 cm² per control rod is found for the pattern of 12 rods discussed previously using the $-2.75\% \Delta k$ per v/o of core for the average position. An average fraction of the core for boron content over loading interval is about 0.003 using the 60 in. of control-rod length. This corresponds to a natural boron density of 2.25×10^{20} atoms per cm³ of core.

The control-rod diameter for natural boron is 4.22 cm. To assure a satisfactory arrangement for cooling and gas containment, the boron is considered to be in seven tubes nested in the above diameter. Corresponding enrichment of the boron is 50%.

The particular reactor arrangement under study has a balance of reactivity effects so that raising the inlet temperature about 200°F is estimated to cause a change from full to zero power without control-rod movement. Added radial and axial expansion removes reactivity as cooling of fuel adds reactivity due to the Doppler effect.

6.3. FUELS

6.3.1. Uranium-Plutonium Alloys (L. J. Kelman, W. N. Beck, C. M. Walter, W. F. Murphy, and C. C. Crothers)

The work on uranium-plutonium alloys at Argonne evolved largely from the developmental work for EBR-II. The EBR-II alloy, uranium-fissium, (U-Fs) had resulted from early processing considerations. Irradiation tests indicated that this alloy is a suitable fast reactor fuel operating at conditions suitable for power reactors. Eventually the development reached the point where there is now a reasonable confidence that such fuels can be designed, using stainless-steel cladding for operating at power reactor conditions for burnup levels of at least 10%.

The addition of plutonium to the uranium-fissium alloys lowers the melting point and increases the fuel swelling rate under irradiation at any given temperature over that of the uranium-fissium alloy. Also, the plutonium forms low-melting eutectics with iron and nickel, so that at least the U-Pu-Fs alloy is not compatible with stainless-steel cladding. The concept of a strong compatible cladding to restrain irradiation swelling was developed in order to obtain high burnup levels. The details on the fissium alloys are given in Ref. 6.3.1.

The initial pyrochemical processing included only a slagging operation that resulted in the fissium alloy, but it is now possible to lay out process flow sheets for relatively complete fission-product removal. Laboratory bench tests and, for many steps, semi-work tests have demonstrated the ability not only to remove all types of fission products, but to accommodate to any desired fuel alloy. Effluent separations can also be made of uranium and plutonium. This allows consideration of any uranium-plutonium alloy having desirable physical and nuclear properties.

A brief screening study of several alloying elements (Sc, Ti, Y, Zr, Mo, Pd, and Th) showed titanium and zirconium to have the greatest promise for raising the solidus temperatures of U-Pu alloys. These elements are also most acceptable from a nuclear standpoint. A comparison of equivalent U-Pu, U-Pu-Fz, and U-Pu-Ti alloys (on an atomic percent basis) shows that titanium is very effective in raising the solidus temperature. The approximate solidus temperatures, in °C, of U-Pu, U-Pu-Fz, and U-Pu-Ti alloys are:

	U-10 a/o	U-15 a/o
	1025	970
20 a/o Fz (9.3 w/o)	890	845
20 a/o Ti (4.8 w/o)	1150	1100

From a metallurgical point of view, alloys of U-Pu-Ti based on the hexagonal U_2Ti compound are quite interesting. The compound has no transformations from room temperature to 898°C, where it transforms to the body-centered cubic gamma U-Ti phase. Plutonium can replace some of the uranium in the binary compound. The high-temperature gamma phase cannot be retained on quenching in the U-Pu-Ti system. It has been observed that those alloys have less of the detrimental U-Pu zeta phase than do other ternary alloys of comparable composition. As a consequence, the U-Pu-Ti alloys have relatively good resistance to corrosion in air.

PHYSICAL PROPERTIES

Phase Relationships. The U-Ti alloys differ from the U-Zr alloys in that they are characterized by the presence of a compound, U_2Ti , in the composition range of interest. The compound exists over only a narrow homogeneity range very near to the stoichiometric composition. It is formed from γ -solid solution at 898°C.^{6.3.2} It has been reported^{6.3.3} that plutonium will replace about half of the uranium in the U_2Ti compound. The introduction of plutonium will cause a slight expansion of the U_2Ti lattice. The lattice constants of U-16.67 a/o Pu-33 a/o Ti, homogenized for one week at 1050°C and water quenched, were found to be $a_0 = 4.843 \pm 0.001 \text{ \AA}$, $c_0 = 2.850 \pm 0.001 \text{ \AA}$ compared with $a_0 = 4.828 \text{ \AA}$, $c_0 = 2.847 \text{ \AA}$ for binary U_2Ti , from Ref. 6.3.2.

Water quenching of the γ -uranium solid solution did not prevent the compound formation. When the same alloy was subsequently heat treated for three days at 775°C and water quenched, the a_0 parameter was reduced by 0.23% to $4.832 \pm 0.0002 \text{ \AA}$, while the c_0 parameter was expanded by 0.42% to $2.8586 \pm 0.0002 \text{ \AA}$, contrary to the observations in Ref. 6.3.3.

These lattice changes are reflected in the dilatometric curves where, for instance, an alloy with nominal 53.7 a/o U-10.7 a/o Pu-35.6 a/o Ti expands by no less than 1% during the transformation of the $(U, Pu)_2Ti$ compound to a γ -uranium solid solution. The large expansion during this transformation is observed on all alloys of sufficiently high titanium content in which the compound $(U, Pu)_2Ti$ predominates. This applies particularly to alloys with $33\frac{1}{3}$ a/o Ti and more in which the compound is in equilibrium with α -titanium, or β -titanium, depending upon temperature. In alloys with less than $33\frac{1}{3}$ a/o Ti the phases of the uranium-plutonium system determine the expansion characteristics of the alloys and the more so, the lower the titanium content.

The temperatures above which the ternary alloys

consist only of the (γ -U, β -Ti) solid solution are listed in Table 6.3.1.

Thermal Expansion. The expansion behavior of several alloys has been determined as a function of temperature to about 950°C in a vertical, quartz push-rod dilatometer. The data are summarized in Table 6.3.2.

Figure 6.3.1 shows the thermal expansion curves for four alloys.

Thermal Conductivity. The thermal conductivity of a 75 U-15 Pu-8 Ti (w/o) alloy has been measured in the comparative apparatus from 200 to 800°C. The values obtained are about 10% less than those for Inconel.

Compressive and Tensile Strength. Compressive tests of U₂Ti, U-15 w/o Pu-10 w/o Pu-10 w/o Ti, and U-22.1 w/o Pu-11.8 w/o Ti were made in the as-cast and homogenized conditions, as shown in Table 6.3.3. The strength decreased and the ductility increased with temperature.

Homogenization strengthened the U₂Ti alloy and did not significantly affect the U-15 w/o Pu-10 w/o

TABLE 6.3.1. (U, Pu)₂Ti $\rightarrow$ (γ -U, β -Ti) SOLID SOLUTION, TRANSFORMATION TEMPERATURES

Alloy Nominal Composition, a/o			Transition Temperature, °C
U	Pu	Ti	
79	0	21	878
75	10	15	809
72	18	10	755*
69	25	6	673
65	35	0	700
47	0	53	890
53.7	10.7	35.6	848
56	14	30	800
63	25	12	732
68	32	0	710
45	15	40	816

* Interpolated value.

FIG. 6.3.1. Thermal Expansion of U-Ti, U-Pu-Ti, and U-Pu-Zr Alloys.

Ti alloy at a given test temperature. The binary alloy is stronger than the ternary alloy.

Compressive properties of U-15 w/o Pu-10 w/o Ti have been supplemented with tensile tests at elevated temperatures. The ratio of compressive to tensile strength varies from 3:1 to 1:1 depending on whether the material is brittle or ductile in tension. Tensile

TABLE 6.3.2. THERMAL EXPANSION OF URANIUM ALLOYS

Alloy composition a/o w/o	U-33 Ti U-9 Ti	U-13 Pu-17 Ti-16 Zr U-17 Pu-5 Ti-8 Zr	U-10 Pu-15 Ti U-11 Pu-3 Ti	U-11 Pu-36 Ti U-15 Pu-10 Ti	U-15 Pu-40 Ti U-22 Pu-12 Ti	U-14 Pu-30 Zr U-17 Pu-14 Zr
Exp coeff $\times 10^6/^{\circ}\text{C}$ (25°C to 1st trans.)	20.1	17.4	19.2	20.3	16.9	18.2
Transformation range, °C	860-897	562-769	566-809	750-848	653-812	596-665
Exp, $\Delta l/l \times 10^3$	10.6	9.0	12.0	1.09%	1.14%	0.58%
Exp coeff $\times 10^6/^{\circ}\text{C}$ (from trans. temp to 960°C)	18.2	18.4	22.3 to 855°C only	19.7	21.3	22.3

ductility was appreciable only above 750°C. (There was appreciable ductility in compression at temperatures as low as 600°C.)

Creep Properties. The U-15 w/o Pu-10 w/o Ti and U-18.5 w/o Pu-14.1 w/o Zr alloys (Table 6.3.4) have similar creep properties at 700°C. At 600°C the latter has the greater creep strength. The results are reported as the time required to reach 1 % creep strain, including "first" and "second" stage creep strains. Since the same specimens were used for several temperatures and loads, the prior creep affected the results of the later tests, but the trends are significant nevertheless. In both alloys the creep behavior was found to obey the following law reasonably well up to 1 % strain:

$$\epsilon = at^n,$$

where

ϵ = strain

a = a constant for a particular test

t = time

n = a constant dependent on stress and structure

Solidus Temperatures. Enough experimental evidence has been obtained to make reasonable estimates of the solidus temperatures of the U-Pu-Ti alloys (Fig. 6.3.2). The plot shows that a titanium content of at least 20 a/o and no more than 10 to 15 a/o plutonium results in alloys with reasonably high solidus temperatures. The combination, however, of a low titanium content and a high plutonium content should be avoided.

The solidus temperatures of the U-Pu-Ti alloys and those of U-Pu-Zr alloys are compared in Figs. 6.3.3 and 6.3.4.

COMPATIBILITY OF FUEL AND CLADDING

U-Pu-Fz Fuel. The extent of interaction between the fuel and cladding in a nuclear reactor is of primary importance in choosing the proper cladding material. With this in mind the compatibility of a number of potential cladding materials with U-Pu-Fz alloys was investigated.

The compatibility of U-10 w/o Pu-10 w/o Fz, U-15 w/o Pu-10 w/o Fz, and U-20 w/o Pu-10 w/o Fz alloys with chromium, Hastelloy-X, type 304 stainless steel, molybdenum, niobium, Nb-1 w/o Zr alloy, vanadium, V-10 w/o Ti, and V-20 w/o Ti alloy was investigated by annealing diffusion couples at 550° to 750°C for 7 to 150 days. Penetrations were obtained by metallographic inspection of the couples and were checked by microprobe analyses.

Figure 6.3.5 compares the cladding penetrations of a number of materials with U-10 w/o Pu-10 w/o Fz alloy. Arranging these cladding materials in order of decreasing compatibility with U-Pu-Fz alloy at 600°C gives:

1. V-20 w/o Ti
2. V-10 w/o Ti
3. Molybdenum
4. type 304 stainless steel
5. Niobium and Nb-1 w/o Zr
6. Hastelloy-X

Table 6.3.5 compares the penetration coefficients (K) calculated (except where noted) from the expression

$$x^2 = Kt,$$

where x is the cladding penetration in centimeters, t is the time in seconds, and K is the penetration coefficient in cm^2/sec . This table summarizes (in order of decreasing compatibility) all data obtained in our study.

TABLE 6.3.3. ULTIMATE COMPRESSIVE STRENGTH OF U-Ti-BASED ALLOYS

Alloy Composition, w/o	Heat Treatment	Temp, °C	Ultimate Compressive Strength, kg/mm ²	Mode of Failure
U ₂ Ti	As cast	615	66	Ductile
U ₂ Ti	As cast	709	41	Ductile
U ₂ Ti	1050°C, 1 wk	554	103	Ductile-brittle
U ₂ Ti	1050°C, 1 wk	710	50	Ductile
U ₂ Ti	1050°C, 1 wk	758	36	Ductile
U-15 Pu-10 Ti	As cast	20	120	Brittle
U-15 Pu-10 Ti	As cast	409	44	Brittle
U-15 Pu-10 Ti	As cast	560	30	Ductile-brittle
U-15 Pu-10 Ti	1050°C, 1 wk	560	35	Ductile-brittle
U-15 Pu-10 Ti	1050°C, 1 wk	643	15	Ductile
U-15 Pu-10 Ti	1050°C, 1 wk	766	6.7	Ductile
U-22.1 Pu-11.8 Ti	As cast	25	141	Brittle
U-22.1 Pu-11.8 Ti	As cast	405	71	Brittle
U-22.1 Pu-11.8 Ti	As cast	516	48	Ductile-brittle
U-22.1 Pu-11.8 Ti	As cast	618	34	Ductile
U-22.1 Pu-11.8 Ti	As cast	717	11	Ductile

TABLE 6.3.4. TIME (MINUTES) TO ATTAIN 1% CREEP STRAIN IN U-Pu-X ALLOYS

Load, kg-mm ⁻²	U-15 w/o Pu-10 w/o Ti			U-18.5 w/o Pu-14.1 w/o Zr		
	600°C	650°C	700°C	600°C	675°C	700°C
0.28	—	210	64	—	94	70
0.42	170	35	30	2.97×10^5	27	32
0.56	120	30	17	2.95×10^5	13	12

FIG. 6.3.2. Estimate of Solidus Temperatures of U-Pu-Ti Alloys.

FIG. 6.3.3. Solidus Temperatures of U-Pu-Ti.

FIG. 6.3.4. Solidus Temperatures of U-Pu-Zr.

Table 6.3.6 presents extrapolation of cladding penetration to a common time of one year for various materials against U-10 w/o Pu-10 w/o Fz and U-15 w/o Pu-10 w/o Fz alloys. Because the cladding material must at some point come in contact with structural components, usually made of austenitic stainless steel, it was also desirable to study the diffusion interaction between the cladding material and type 304 stainless steel. Vanadium and V-20 w/o Ti alloy were checked against type 304 stainless steel because of their superior compatibility with the fuel. Bonding, but not serious penetration, occurred between these materials; thus it is necessary to be prepared to encounter this in disassembly of reactor components.

Some general conclusions that may be drawn from this study are:

1. Of the cladding materials investigated, V-20 w/o Ti alloy is the most compatible with U-Pu-Fz alloy.
2. Molybdenum is also good, but, because of difficulty encountered in determining the direction of penetration, V-Ti alloys were judged to be somewhat better.
3. Hastelloy-X and type 304 stainless steel show evidence of melting and accelerated penetration between 650° and 675°C. Their temperature limits of operation would, therefore, be considerably below 350°C.
4. As expected, an increase in plutonium content of the fuel results in poorer compatibility with all the cladding materials investigated.

The results of this investigation indicate that the compatibility of a given metallic fuel with various cladding materials may be predictable, on a relative basis, using the binary phase diagrams of the major elements in the fuel and in the cladding. In general, systems showing limited solid solubility, especially on the cladding side of the phase diagram, and no intermetallic compounds will have good compatibility. In contrast, systems having extensive solid solubility or intermetallic compounds will have poorer compatibility. In systems that form compounds, the closer in composition these compounds are to the cladding side of the diagram, the greater is the cladding penetration.

U-Pu-Ti or Zr Fuels. Recent studies show that type 304 stainless steel is compatible with U-18.5 w/o Pu-14.1 w/o Zr alloy up to 800°C. This is contrary to all previous experience with U-Pu-Fz and U-Pu-Ti alloys, which showed melting of reaction products and drastic penetration into the cladding and into the fuel in the range 650 to 675°C.

The possibility that commercially available iron- or nickel-based alloys can be used as a cladding adds

FIG. 6.3.5. Diffusion Penetration of U-10 w/o Pu-10 w/o Fz into Prospective Claddings at a Constant Time—42 days.

TABLE 6.3.5. PENETRATION COEFFICIENTS (K)
(in cm^2/sec except where noted)

Composition, w/o	Temperature, °C				
	550	600	650	700	750
V/U-10Pu-10Fz	$7.5 \times 10^{-14}\dagger$	$1.5 \times 10^{-13}\dagger$	$4.0 \times 10^{-13}\dagger$		
V-20Ti/U-10Pu-10Fz	$2.3 \times 10^{-15*}$	1.3×10^{-14}	6.4×10^{-14}	3.1×10^{-13}	$1.7 \times 10^{-12*}$
V-20Ti/U-15Pu-10Fz		1.3×10^{-14}	8.1×10^{-14}	$8.0 \times 10^{-13*}$	$3.0 \times 10^{-12*}$
V-10Ti/U-10Pu-10Fz	4.7×10^{-15}	2.3×10^{-14}	1.1×10^{-13}		
Cr/U-10Pu-10Fz				$3.5 \times 10^{-13*}$	
Mo/U-10Pu-10Fz		4.5×10^{-14}	1.2×10^{-13}	1.9×10^{-13}	
Mo/U-15Pu-10Fz		$7.5 \times 10^{-14*}$	$2.0 \times 10^{-13*}$	$4.0 \times 10^{-13*}$	
304SS/U-10Pu-10Fz	6.9×10^{-14}	2.2×10^{-13}	1.1×10^{-12}		
304SS/U-15Pu-10Fz			$3.4 \times 10^{-12*}$		
Hast-X/U-10Pu-10Fz	1.8×10^{-13}	8.0×10^{-13}	3.6×10^{-12}		
Hast-X/U-15Pu-10Fz			$1.1 \times 10^{-11*}$		
Nb/U-10Pu-10Fz	1.3×10^{-13}	6.3×10^{-13}	$2.2 \times 10^{-9} \text{ cm/sec}\dagger$		
Nb/U-20Pu-10Fz	2.3×10^{-13}	1.8×10^{-12}	$4.2 \times 10^{-9} \text{ cm/sec}\dagger$		
Nb-1Zr/U-10Pu-10Fz	1.6×10^{-13}	4.0×10^{-13}	$1.9 \times 10^{-9} \text{ cm/sec}\dagger$		
304 SS/V			$3.4 \times 10^{-13}\S$		
304 SS/V-20Ti			$2.3 \times 10^{-13}\S$		

* Calculated from only one data point, assuming an $X^2 = Kt$ law.

† Obeyed the $X = Kt$ rather than $X^2 = Kt$ law.

‡ No cladding penetration. K 's represent band on the fuel side, believed to be (U, Pu) O_2 . Vanadium contained 1300 ppm oxygen.

§ Total band width. Also calculated from only one data point, assuming an $X^2 = Kt$ law.

TABLE 6.3.6. CALCULATED PENETRATIONS FOR ONE
YEAR ($\text{cm} \times 10^4$)

Composition, w/o	Temperature, °C				
	550	600	650	700	750
V-20Ti/U-10Pu-10Fz	3	6	15	30	75
V-20Ti/U-15Pu-10Fz		6	15	50	100
V-10Ti/U-10Pu-10Fz	4	10	20		
Cr/U-10Pu-10Fz				30	
Mo/U-10Pu-10Fz		10	15	25	
Mo/U-15Pu-10Fz		15	25	35	
304SS/U-10Pu-10Fz	15	25	60		
304SS/U-15Pu-10Fz			100		
Hast-X/U-10Pu-10Fz	25	50	110		
Hast-X/U-15Pu-10Fz			190		
Nb/U-10Pu-10Fz	20	45	700		
Nb/U-20Pu-10Fz	30	75	1300		
Nb-1Zr/U-10Pu-10Fz	25	35	600		

increased interest to the U-Pu based metallic fuels. A thorough investigation of the compatibility of U-Pu-Zr alloys with several steels and superalloys is under way to determine (1) the nature of the reaction products at the interface; (2) the effect of time and temperature up to the melting temperature; (3) the lower limit of zirconium in the fuel that maintains adequate compatibility; and (4) the range of stronger iron- and nickel-based alloys that are compatible with the fuel. Other factors of interest are: other additives that will make U-Pu alloys compatible with stainless

steel; the influence of a sodium thermal bond; the influence of an irradiation environment; and the effect of fission products that grow in at high burnups.

Figure 6.3.6 shows the reaction zones after 7 days at 650°C of type 304 stainless steel with U-15 w/o Pu-10 w/o Fz, U-18.5 w/o Pu-14 w/o Zr, and U-22.1 w/o Pu-11.8 w/o Ti alloys. The reaction layer for the zirconium alloy is so thin (approximately 1μ) that it is hardly discernible, whereas melting and thick reaction layers formed on both the cladding and the fuel sides of the other couples. Figure 6.3.6 also shows the influence of increasing temperature on the U-18.5 w/o Pu-14.1 w/o Zr stainless-steel couple. The reaction layer remains thin and melting does not occur up to 800°C. At 850°C gross melting does occur. The transformation to the gamma phase is complete just above 650°C, which accounts for the change in the fuel microstructure.

Microprobe analysis has shown that the very thin reaction layer in the U-18.5 w/o Pu-14.1 w/o Zr stainless-steel couple contains about 50 w/o Zr, about 15 w/o Fe, and smaller amounts of uranium, plutonium, and chromium.

IRRADIATION OF METALLIC FUELS (W. N. Beck)

Fuel pins have been and are being irradiated in the CP-5 and EBR-II reactors. The U-Pu-Fs alloy system was initially of interest, but it was superseded by the high-melting alloys of U-Pu-Zr and U-Pu-Ti.

FIG. 6.3.6. Compatibility of Type 304 Stainless Steel with U-Pu Based Fuels (w/o): 7 Days at Temperature.

Fuel burnups of 8.7 a/o have been demonstrated with U-Pu-Fs clad with Nb-1 w/o Zr at maximum cladding temperatures of 550°C. Irradiations in progress with this alloy clad with V-20 w/o Ti tubing are designed to study the specimen design parameters as related to achievable fuel burnup. Present results show that fuel burnups in excess of 10 a/o are possible. Six specimens still under irradiation have attained burnups of 6.5 a/o at maximum cladding temperatures of 600°C. These specimens were designed to fail at predetermined burnup levels, and the periodic nondestructive examinations support the calculations. Two of these specimens will be irradiated to 10 a/o burnup before they are destructively examined.

U-Pu-Ti alloys are being irradiated in CP-5 and

EBR-II. The pins are clad with V-20 w/o Ti tubing and, at present, one specimen in the CP-5 reactor has attained 6.0 a/o burnup at a maximum cladding temperature of 590°C.

Twenty-five specimens of U-Pu-Zr are being irradiated in refractory as well as in iron- and nickel-base alloy tubings. This fuel has been found to be compatible with type 304 stainless-steel cladding at the desired irradiation temperatures. Consequently, only three of these specimens were clad with refractory-alloy tubing. One specimen, which is being irradiated in V-20 w/o Ti tubing, has reached a calculated burnup of 7.0 a/o at cladding temperatures of 620°C. The other specimens are being irradiated at maximum cladding

TABLE 6.3.7. IRRADIATIONS OF U-Pu ALLOY

Sam- ple No.	Fuel Alloy, Wt. %	Cladding Material and Thickness, mils	Burnup, Heavy Atom %	Max-Time Ave. Clad- ding Sur- face Temp., °C	Status as of April 1966
32-1	U-20Pu-10Fz	Nb-1Zr-15	4.2	590-460	(1)
-2	"	" -15	4.2	590-460	(1)
-3	"	" -25	4.2	590-460	(1)
-4	"	" -25	4.2	615-480	(1)
-5	"	" -25	4.2	615-480	(1)
-6	"	" -15	4.2	615-480	(1)
29-1	"	Nb-4V-9	3.0	490-450	(1)
-5	"	" -9	3.0	500-460	(1)
-4	"	" -9	3.0	500-460	(1)
33-1	U-10Pu-10Fz	Nb-1Zr-15	7.1	550	(1)
-2	"	" -20	7.1	550	(1)
-3	U-15Pu-10Fz	" -15	8.7	550	(1)
-4	"	" -25	8.7	550	(1)
-5	"	" -15	8.7	550	(1)
-6	U-10Pu-10Fz	" -25	7.1	550	(1)
40-1	U-15Pu-10Fz	V-20Ti-16	5.7	600	(1)
-2	"	"	5.7	600	(1)
-3	"	"	5.7	600	(1)
-4	"	"	5.7	600	(1)
-5	"	"	5.7	600	(1)
41-1	"	"	5.6	600	(2)
-2	"	"	6.5	600	(2)
-3	"	"	6.5	600	(2)
-4	U-10Pu-10Fz	"	6.5	600	(2)
-5	"	"	6.5	600	(2)
-6	"	"	6.5	600	(2)
43-1	U-15Pu-10Fz	V-20Ti-15	5.5	550	(2)
44-1	U-15Pu-10Ti	V-20Ti-15	6.0	590	(2)
45-1	U-19Pu-14Zr	V-20Ti-15	7.0	620	(2)

(1) Specimens out of reactor.

(2) Specimens still in reactor.

temperatures of 640°C and have reached an average burnup of 1.6 a/o.

Particulars and status of current high-burnup specimens are listed in Table 6.3.7.

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Although austenitic stainless steels have been widely

used for structural components in sodium-cooled fast reactors, it has been recognized for some time that a more satisfactory material is needed for the fuel cladding in such reactors. The strength properties of most stainless steels fall off rapidly above 500°C. Yet, most current designs for fast reactors call for fuel-cladding temperatures in the range of 600° to 700°C. Furthermore, nickel or iron-base alloys form eutectics in this temperature range with several uranium-plutonium fuel alloys of interest for fast breeder reactors.

Early work at Argonne National Laboratory led to the exploration of vanadium-base alloys as a possible substitute for stainless steel.^{6.3.4} In this study it appeared that V-Ti alloys offered the greatest promise. Further development work on vanadium alloys was reactivated in 1963 at Argonne, largely owing to the combination of advantages shown by physics analyses^{6.3.5} and acceptable properties such as corrosion resistance and compatibility with plutonium-containing alloys.

Much of the recent experimental effort has been concentrated on the binary alloy V-20 w/o Ti. There have been significant recent advances concerning the fabrication of this alloy into tubing, its physical and mechanical properties, its behavior in sodium, and its compatibility with U-Pu alloys. Irradiation studies are in progress. Vanadium-titanium alloys are also under active development in Europe, at Fontenay-aux-Roses and especially at Karlsruhe.

FABRICATION OF REACTOR-GRADE TUBING

Consolidation. Starting materials for alloy consolidation consist of -3 +30-mesh vanadium chips and -3 +14-mesh titanium sponge, with total interstitial element contents (C, O, N and H) of 1000 ppm or less. The chips and sponge are weighed, blended, and cold-pressed into briquets, which are tack-welded together to form a "drip-rod". A two-step melting practice is used; it consists of electron-beam (EB) melting of the drip rod followed by consumable arc remelting of the 2.3-in. diameter ingots, all under high vacuum. The resulting arc-melted ingot is 3.8 in. in diameter and weighs 26 to 31 lb. after cropping.

The EB melting operation provides initial alloying and purification, while the final arc melting provides improved homogeneity, a smaller and more uniform grain size, improved ingot soundness and surface, and minimal metal loss. Alloy homogeneity is very good with the above techniques, and there is little or no difficulty with this phase of operations.

Primary Fabrication. V-20 w/o Ti alloy is amenable to hot working by extrusion at 1000 to 1200°C, with 1100°C yielding the best surface consistent with reasonable reduction ratios. A double-extrusion technique

is necessary to obtain an acceptable extruded-surface quality.

The initial (ingot breakdown) extrusion usually produces a heavily striated surface. After machining and re-extrusion, the surface is greatly improved and generally acceptable for further processing.

In all cases, the alloy billets are encased in a type 304 stainless-steel jacket to eliminate contamination during conventional air heating. The jackets are removed mechanically or by dissolution in hot HCl. Extruded tube-blank sizes are approximately 0.75 in. OD $\times$ 0.51 in. ID or 0.87 in. OD $\times$ 0.65 in. ID.

Secondary Fabrication. The dejacketed extrusion product requires little conditioning prior to further secondary fabrication operations. If necessary, minor surface roughness can be removed by light belt-sanding. The extruded tube blanks are sized for drawing by swaging over a non-deformable mandrel. Vacuum annealing (900°C) is required after each 10–15% reduction. Heavier reductions can cause catastrophic wall cracking, resulting in a complete loss of material.

The conditioned tube blank is rather easily cold-worked to reductions of 50–60% by rolling or drawing. The alloy may be drawn through conventional carbide dies but there is a marked propensity towards galling unless a suitable lubricant is used. Early development in tubing fabrication utilized various aluminum-bronze type dies to overcome the galling tendency. Ductile cores of mild steel and hard-drawn copper are also employed. The present procedure is to reduce the blank by ductile core, mandrel, and/or plug drawing to the final desired size. Intermediate vacuum anneals are used as required. Finished tubing lengths are dictated by the length capabilities of existing vacuum-annealing furnaces. Attempts are being made to overcome this restriction by an induction-heating, zone-annealing technique presently under development.

FIG. 6.3.7. Typical Sizes of Reactor-quality V-20 w/o Ti Alloy Tubing Produced at Argonne National Laboratory.

Considerable attention is given to quality control. No reactor-grade tubing is released to ultimate users unless it has passed the eddy-current (under 10% defect level) inspection along with the usual visual and dimensional inspection procedures. It has been clearly demonstrated that high-quality tubing of V-20 w/o Ti alloy can be fabricated in a range of sizes such as are shown in Fig. 6.3.7 if care is exercised in the selection and control of the tubing-fabrication techniques.

Industrial Participation. Industrial participation in the various fabrication operations for producing V-20 w/o Ti alloy tubing has been encouraged. Ingots measuring 4 and 6 in. in diameter have been produced by industry using a double melting technique similar to that used at Argonne. The larger ingot size has been successfully extruded to about 2 in.-diameter bar stock by a commercial extrusion facility. A commercial capability for the extrusion of tube blanks has not as yet been developed.

Commercial tube reducing of Argonne-extruded tube blanks appears feasible according to a cursory evaluation by one industrial organization. Drawing of extruded tube blanks furnished by Argonne has been demonstrated by three industrial organizations; the resulting 0.208 in. OD by 0.015 in. wall tubing from one vendor is of excellent quality. Efforts to encourage commercial tube drawing of this alloy are continuing.

PHYSICAL AND MECHANICAL PROPERTIES

Young's Modulus. Young's modulus has been measured both by dynamic^{6,3,6} and static methods from room temperature to around 650°C. The results are given in Fig. 6.3.8.

Short-time Tensile Properties. The short-time tensile properties of V-20 w/o Ti alloy have been measured from room temperature to 800°C. The tensile strength does not fall off appreciably with increasing

FIG. 6.3.8. Change in Young's Modulus with Temperature for V-20 w/o Ti Alloys.

TABLE 6.3.8. SHORT-TIME TENSILE PROPERTIES
OF V-20 w/o Ti ALLOY

Temperature, °C	Ultimate Tensile Stress, kg-mm ⁻²	0.002 Off-set Yield Stress, kg-mm ⁻²	Total Elongation, %	Uniform Elongation, %	Reduction of Area, %
<i>Rod Material</i> —1.27 cm diam, extruded 1110°C. Average strain rate 2.7×10^{-4} sec ⁻¹ , 0.635 cm reduced section diam.					
RT	70.3	62.6	34	16.7	56
RT	70.6	64.2	36	20.2	56
650	66.2	45.8	37	15.6	44
750	46.3	38.3	36	7.0	56
800	41.5	35.9	38	5.0	50
<i>Sheet Material</i> —0.152 cm thick, recrystallized 900°C, 1 hr. Average strain rate 2.2×10^{-4} sec ⁻¹ .					
550	—	36.7	—	—	—
650	—	32.6	—	—	—
650	—	34.2	—	—	—

FIG. 6.3.9. Change in Tensile Strength with Temperature for Several Cladding Alloys.

temperature below 650°C. Above 650°C the tensile strength decreases more rapidly. The uniform elongation is quite high (around 15%) and decreases above 650°C. The short-time tensile properties of V-20 w/o Ti alloy tested in a vacuum of 2×10^{-5} mm Hg or

better are given in Table 6.3.8. A comparison with other cladding alloys is given in Fig. 6.3.9.

Short-Time Creep and Stress Rupture. Creep rates have been measured under different stress levels for stress-relieved and recrystallized sheet, 0.061-in. thick, at 650°C. These tests were conducted in vacuums better than 2×10^{-5} mm Hg. Results are given in Table 6.3.9.

For stress levels around 56,000 psi the stress-relieved specimen fractured in seven hours, while the recrystallized specimen fractured in eleven hours. Data by Armour Research Foundation^{6.3.7} show rupture life for V-20 w/o Ti alloy to be around 50 hr at this stress level. Their tests were conducted on rod material in the as-swaged condition. Although the chemical analyses of their specimens were not reported, it is believed that their material is of lower purity. Material purity as well as specimen condition could be responsible for the difference in rupture life.

The uniform elongation in the creep tests is around 15%.

The creep rates for V-20 w/o Ti alloy are compared to those for type 304 stainless steel^{6.3.8-6.3.11} in Fig. 6.3.10. V-20 w/o Ti alloy is more creep-resistant at stress levels above 28,000 psi. Data below this level are not yet generated for this alloy.

Thermal Expansion. The thermal expansion of V-20 w/o Ti alloy was measured over the temperature

TABLE 6.3.9. CREEP RESULTS FOR V-20 w/o Ti ALLOY
TESTED AT 650°C IN VACUUM

Specimen Number	Stress, kg-mm ⁻²	Minimum Strain, Rate, min ⁻¹	Rupture Time, hrs	Condition
<i>Extrusion 264—Solution Heat Treated at 1300°C, 15 min.</i>				
TV-20-1S	37.8	4.2×10^{-4}	7	Stress relieved, 700°C, 1 hr.
TV-20-2S	19.3	1.6×10^{-6}	*	Recrystallized, 900°C, 1 hr.
	25.3	7.2×10^{-6}		
	37.7	1.05×10^{-4}		
	25.3	8.8×10^{-6}		
<i>Extrusion 281</i>				
TV-20-3S	38.5	1.6×10^{-4}	11	Recrystallized, 900°C, 1 hr.
TV-20-4S	38.9	1.2×10^{-4}	*	Recrystallized, 900°C, 1 hr.
	42.3	3.2×10^{-4}		
TV-20-6S	31.6	1.9×10^{-5}	68	Recrystallized, 900°C, 1 hr.
TV-20-7S	29.3	9.0×10^{-5}	*	Stress relieved, 700°C, 1 hr.
	20.0	2.9×10^{-5}		

* Load changed during test.

range 25–967°C. The experimental expansion curve is given in Fig. 6.3.11 along with published curves for unalloyed vanadium^{6.3.12} and titanium.^{6.3.13}

Enthalpy. The enthalpy of the V-20 w/o Ti alloy was measured to an accuracy of $\pm 1\%$ in an isothermal drop calorimeter^{6.3.14} from room temperature to 1150°C. The results are given in Table 6.3.10. The

mean values of the specific heat, given in Table 6.3.11, were calculated between the reference temperature (25°C) and 1000°C in increments of 200°C. The mean specific heat values are comparable to those of vanadium.^{6.3.15} Between 25°C and 200°C the alloy value is 0.119, compared with 0.121 cal/(g)(°C) for vanadium. Between 25°C and 1000°C the alloy value is 0.129 compared to 0.139 cal/(g)(°C) for vanadium.

The variation of enthalpy with temperature is not linear. The mean specific heats vary about 9% with temperature in the range studied.

Thermal Conductivity. The thermal conductivity of the V-20 w/o Ti alloy is being determined on the comparative type apparatus.^{6.3.16} Values to 600°C are listed in Table 6.3.12. The alloy was run against Inconel and Armco standards, with good agreement between the two runs. The standard deviation of the data is 0.011.

Density. The density of 0.152-cm-thick sheet was measured at 22°C. A density value of 5.7 g/cm³ was obtained for sheet stress-relieved at 700°C for 1 hr and for sheet recrystallized at 900°C for 1 hr.

Hardness. The hardness is given below:

Condition	Hardness	
	DPH	Rockwell
Arc-cast ingot	208	94R _B
Rod, extruded at 1110°C	233	98R _B
As-rolled sheet	266	25R _C
Sheet, stress relieved at 700°C, 1 hr	219	96R _B
Sheet, recrystallized at 900°C, 1 hr	197	91R _B

FIG. 6.3.10. Creep of V-20 w/o Ti Sheet and Type 304 Stainless Steel at 650°C.

FIG. 6.3.11. Thermal Expansion of Titanium, Vanadium, and V-20 w/o Ti Alloy.

TABLE 6.3.10. HEAT CONTENT OF V-20 w/o Ti ALLOY ABOVE 25°C

Measured		Smoothed	
Temp, °C	ΔH_{25} , cal/g	Temp, °C	ΔH_{25} , cal/g
-80	-9.4	100	9.0
297	32.3	200	20.9
471	54.3	300	33.0
647	77.9	400	45.5
831	102	500	58.6
1001	127	600	71.8
1153	149	700	85.0
		800	98.2
		900	112
		1000	126
		1100	140
		1200	154*

* Extrapolated.

TABLE 6.3.11. MEAN SPECIFIC HEAT OF V-20 w/o Ti ALLOY ABOVE 25°C

Temperature Range, 25°C to T°C	Mean Specific Heat, $\Delta H_{25}/T$, cal/(g) (°C)
200	0.119
400	0.121
600	0.125
800	0.127
1000	0.129

TABLE 6.3.12. THERMAL CONDUCTIVITY OF V-20 w/o Ti ALLOY

Temperature, °C	Thermal Conductivity, Watts/(cm) (°C)
0	0.188*
100	0.205
200	0.220
300	0.234
400	0.249
500	0.262
600	0.276

* Extrapolated.

COMPATIBILITY OF PLUTONIUM FUELS WITH V-20 w/o Ti ALLOY

The compatibility of U-Pu-Fz alloys with V-Ti alloys was investigated by annealing diffusion couples at 550–750°C for 7 to 150 days. Penetrations were obtained by metallographic inspection of the couples and were checked by microprobe analyses.

V-20 w/o Ti alloy was found to be more compatible with U-Pu-Fz alloy than any other cladding material investigated, including chromium, molybdenum, niobium, vanadium, Hastelloy-X, type 304 stainless

steel, Nb-1 w/o Zr, and V-10 w/o Ti at 650°C and lower. Slight bonding with the fuel was observed at 650°C for V-20 w/o Ti alloy and type 304 stainless steel. The penetration of U-10 w/o Pu-10 w/o Fz into V-20 w/o Ti after 42 days at temperature ranged from 20 microns at 750°C to 1 micron at 550°C.

BEHAVIOR OF VANADIUM-BASE ALLOYS IN SODIUM AT 650°C

Initial exploratory tests, with sodium at 650°C, indicated that vanadium and vanadium-titanium alloys are superior to common niobium alloys. Unfortunately, the oxygen concentration, during two tests of seven and nine days duration, is not known with the desired degree of accuracy. Values between 50 and 200 ppm were obtained as the result of chemical analysis but it is now suspected that the concentration was below 20 ppm. Niobium-base alloys suffered intergranular attack and weight changes that in some instances were an order of magnitude higher than vanadium-titanium alloys. The weight change and depth of the hardened layer (interpreted as caused by oxygen absorption) on vanadium alloys decreased markedly as the titanium content was increased to 20 w/o. Based on these results, our subsequent work has been devoted to the study of vanadium-base alloys. A more recent test, in a cleaner system, at a lower oxygen concentration—probably 5 ppm or less—confirmed the superiority of V-20 w/o Ti as compared with unalloyed niobium.

The behavior of V-20 w/o Ti alloys under static conditions in sodium at 650°C containing nominally about 8 ppm and 50 ppm respectively of oxygen is shown in Fig. 6.3.12. The curves include data for samples exposed continually for the indicated time as well as those removed periodically for inspection and replaced in the test. The sharp decrease in rate after about 100 hr of exposure (in the nominally 8 ppm experiment) is now thought to be due to oxygen depletion. The cyclic phenomenon in the nominally 50 ppm experiment may have been caused by a change in environment, i.e., a slight oxygen contamination at about 250 hours.

In a subsequent experiment, V-20 w/o Ti alloy was exposed to sodium of very low oxygen concentration (hot-gettered with zirconium for 48 hr prior to exposure, with the cold trap maintained at 137°C during test as added precaution). The results are shown in Fig. 6.3.13. In contrast with the experiments summarized in Fig. 6.3.12 a constant reaction rate "constant" is, in fact, observed. These results demonstrate the sensitivity of corrosion rate to oxygen concentration at low oxygen concentrations.

Other inferences concerning the behavior of V-20

FIG. 6.3.12. Behavior of V-20 w/o Ti in Static Sodium at 650°C.

FIG. 6.3.13. Behavior of V-20 w o Ti in Purified Sodium at 650°C.

TABLE 6.3.13. EFFECT OF ALLOY COMPOSITION ON
CORROSION IN 650°C SODIUM CONTAINING
ABOUT 45 ppm OXYGEN

(All specimens were exposed for 7.0 days, and had been 50% cold-reduced prior to testing.)

Alloy, w/o	External Loss Per Side, μ	$\Delta W/cm^2$, mg/cm ²	Depth of Hardened Layer, μ^*	Total Metal Penetration, μ
V-20Ti	42	-16.3	30-35	72-77
V-5Ti-15Cr	9	-9.2	60-70	69-79
V-20Ti-2Mo	17	-8.5	40-45†	57-62
V-20Ti-2Ta	34	-11.2	35-40†	69-74
V-20Ti-2Nb	34	-14.7	40-50†	74-84
V-40Ti	12	-4.9	15-20	27-32
V-15Ti-5Cr	24	-14.7	40-45	64-69
V-15Ti-7.5Cr	21	-15.6	34-38	55-59
V-20Ti-5Cr	23	-19.5	32-35	55-58
V-30Ti-5Cr	14	-13.1	20-22	34-36
V-30Ti-10Cr	18	-14.1	15-18	33-36

* Including adherent oxide.

† Longitudinal surface cracks in hardened layer, 10-20 μ deep.

w/o Ti alloy in sodium may be drawn, at least in a qualitative sense:

1. Corrosion behavior is independent of metallurgical condition over a considerable range, i.e., cold-rolled, stress-relieved, or recrystallized material show the same behavior.

2. Stress, to a level great enough to produce permanent deformation (samples under load in sodium), does not affect corrosion behavior.

3. Reduction of the temperature to 550°C reduces corrosion rate by a factor of between 5 and 10.

Concurrent with the study of the effect of pertinent variables on V-20 w/o Ti alloy, other vanadium-base alloys are being investigated in an effort to develop a superior material. These tests are being conducted at a relatively high oxygen concentration, 40-50 ppm, on the assumption that this condition can serve as an accelerated test useful for defining the more promising composition and for indicating the effect of various alloying elements.

The results are summarized in Table 6.3.13. A V-20 w/o Ti alloy sample was included to serve as a basis of comparison. Under these test conditions all samples lost weight and had an adherent "oxide" under a loose corrosion product. The indicated measurements were made with only the loose coating removed.

Chromium is effective in reducing the amount of metal lost to the system. However, if the depth of reacted metal (sum of metal loss plus thickness of hardened layer on exposed sample) is taken as a criterion, titanium and chromium appear about equally effective as alloying elements.

As is well known to workers in this field, reliable oxygen analyses are of vital importance and are obtained with great difficulty in the range of interest. We have developed a sampling technique (incorporating extrusion in vacuum) combined with the amalgamation method that is giving reliable results. For example, a sodium bath maintained just above the melting point yielded a sample that indicated 0 ppm. Duplicate samples taken from a bath maintained at 145°C yielded results of 6.1 and 5.5 ppm oxygen. The equilibrium solubility of oxygen in sodium at 140°C has been reported as 7.5-8.0 ppm.^{6,3,17} Typically, duplicate samples containing oxygen in the 50 ppm range differ by only a few ppm.

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6.3.3 Estimation of Achievable Burnup in EBR-II-Type Fuels (Frank G. Foote)

Fuel rods of the EBR-II type consist of a fuel pin of uranium-base alloy in a loose-fitting jacket tube; the annulus between the fuel and jacket is filled with sodium for heat-transfer purposes. The top end of the fuel pin is covered with a layer of sodium and above

this is a gas void volume. The jacket is welded shut at both ends and acts as a small cylindrical pressure vessel with massive flat end plates. The initial geometry is shown in Fig. 6.3.14.

When the rod is irradiated, heat is generated in the fuel and removed through the jacket. Everything heats up and expands; in particular the sodium melts and increases appreciably in volume. The gas in the void volume is heated and also compressed by the expanding sodium, and hence the pressure increases. As burnup proceeds, the fuel increases in volume by the accumulation of solid fission products and fission-product gases. This displaces sodium in the annulus and further increases the pressure of gas in the void volume. Eventually, the gas pressure in the void or in gas bubbles trapped in the fuel exceeds the bursting pressure of the tube and the jacket fails.

An attempt has been made to identify and to analyze the effect of various factors that limit the achievable burnup. In general, the factors that tend to limit the burnup have been included in the analysis; factors that tend to extend the lifetime have not been included. Hopefully, the calculated burnups are the minimum to be expected from a given geometry and operating temperature.

RELATIONSHIP OF HOOP STRESS TO INTERNAL PRESSURE

The maximum hoop stress (inner wall) is related to the internal pressure in a thick-walled cylinder by:

$$\frac{S}{P} = \frac{\left(\frac{OD}{ID}\right)^2 + 1}{\left(\frac{OD}{ID}\right)^2 - 1}, \quad (1)$$

where

S = maximum hoop stress, kg/cm²

P = internal pressure, kg/cm²

OD = outside diameter of jacket in cm

ID = inside diameter of jacket in cm.

Although the jackets used for fast reactor fuel rods are thin-walled tubes of small diameter, the ratios of diameter to wall thickness are usually in the range that requires the use of the thick-wall-cylinder formula.

RELATIONSHIP OF FUEL SWELLING TO BURNUP

As the fuel material is fissioned, the fuel increases in volume. Part of this increase in volume is due to the accumulation of solid fission products; the rest is due to gaseous fission products (Xe + Kr) in the form of gas bubbles within the fuel. The situation can be expressed by:

$$\frac{Y}{Bu} = 2.50 + 100 \times n \times R \left(\frac{T_f}{P} \right), \quad (2)$$

where

Y = % swelling of the fuel

Bu = a/o burnup

2.50 = % volume increase of fuel per 1 a/o burnup due to solid fission products

n = moles of fission-product gas (Xe + Kr) per cm³ of fuel per 1 a/o Bu

R = gas constant = 84.781, when pressure is in kg/cm² and volume is in cm³

T_f = volume averaged temperature of fuel = effective temperature of gas bubbles in °K

P = pressure of the fission gas in the bubbles, kg/cm².

The assumption is made that no fission gas escapes from the fuel and none is trapped in the lattice as isolated atoms. Hence all fission gas appears as free gas in the form of bubbles and obeys the ideal-gas law.

Equation (2) shows that for indefinitely large amounts of swelling, the burnup can also be indefinitely large. If the amount of swelling is restricted to some practical value, then the smaller the (Y/Bu) ratio, the higher will be the burnup. Therefore, the lower the fuel temperature and the stronger and heavier the jacket, the higher the burnup.

MECHANICAL RESTRICTIONS ON FUEL SWELLING

The amount of fuel swelling can be limited by mechanical restraints built into the design. Clearly the

FIG. 6.3.14. Initial Geometry: Cold.

radial swelling cannot exceed the limit set by the inside diameter of the jacket. Axial swelling can be limited by restrainers built into the fuel rod. In general we can specify four reasonably well-defined cases.

1. Swells to the Jacket Plus $Z\%$ Axially

$$\frac{Y}{100} = \left[1 + \frac{Z}{100} \right] \left[\frac{B}{A} \right]^2 \left[\frac{D_j}{D_f} \right]^2 - 1 \quad (3A)$$

where

Y = % swelling of the fuel

Z = % axial swelling over and above the increase in length due to thermal expansion alone

$A = 1 + \alpha_f (T_f - 300)$

$B = 1 + \alpha_j (T_j - 300)$

where α_f and α_j are the linear thermal expansion coefficients of the fuel and jacket materials in $^{\circ}\text{C}^{-1}$;

T_f and T_j are the effective average fuel and jacket temperatures in $^{\circ}\text{K}$

D_j = cold, inside diameter of jacket [i.e., D_j = ID of Equation (1)]

D_f = cold diameter of fuel pin.

The B/A ratio is near unity for almost any combination of metallic fuel and jacket, and in any case is nearly independent of temperature if the fuel and jacket temperature differ by only a hundred degrees or so.

2. Tight Axial Restraint: $Z = 0$

The fuel expands thermally upon heating to operating temperature and contacts an axial restrainer. Swelling is entirely radial and is limited by the inside diameter of the jacket.

$$\frac{Y}{100} = \left(\frac{B}{A} \right)^2 \left(\frac{D_j}{D_f} \right)^2 - 1, \quad (3B)$$

where symbols have the same meaning as in Eq. (3A)

3. Isotropic Swelling—Relaxed Axial Restraint:

$$Z = 100 \left[\frac{D_j}{D_f} - 1 \right]$$

The fuel swells isotropically such that $L/L_0 = D_j/D_f$. A restrainer is built into the fuel rod in such a position that the length of the fuel pin cannot exceed $L = L_0(D_j/D_f)$.

Then

$$\frac{Y}{100} = \left[\frac{B}{A} \right]^3 \left[\frac{D_j}{D_f} \right]^3 - 1 \quad (3C)$$

4. Isotropic Swelling—Axial Restraint:

$$Z = 100 \left[\frac{D_j}{D_f} - 1 \right]$$

The fuel swells isotropically but an axial restrainer is built into the fuel rod such that $L/L_0 < D_j/D_f$. Under these conditions, the fuel swells to the restrainer before it reaches the jacket.

We assume that further radial swelling is prevented by pneumatic restraint (see below). The jacket-to-fuel diameter ratio does not enter the situation, and under these conditions:

$$\frac{Y}{100} = \left(1 + \frac{Z}{100} \right)^3 - 1. \quad (3D)$$

PNEUMATIC RESTRAINT OF FUEL SWELLING

A void volume is built into the top of the fuel rod. During welding closure of the rod jacket, gas at more or less ambient temperature and pressure is sealed into this void. As the fuel rod is heated to operating temperature, this trapped gas is heated and is also compressed by the expansion of bonding sodium. The gas pressure therefore increases, the level of pressure being strongly dependent upon the size of the void volume. Indeed, if this volume is too small, the internal pressure may exceed the bursting pressure of the jacket merely upon heating to operating temperature.

As fissioning of the fuel occurs, fission-gas bubbles form in the fuel. When the pressure in the bubbles exceeds the pressure in the gas void by a certain amount, the fuel begins to swell. Swelling displaces annulus sodium, which further increases the pressure in the gas void. We will assume that at operating temperature ($T_f = 1000^{\circ}\text{K}$) the fuel material has zero creep strength and that the fission-gas bubbles are of such size that surface-tension effects are small. Under these conditions the pressure of fission gas in the bubbles is equal to the pressure of gas in the void volume. We will further assume that, except for the negligible amount of gas from surface recoil, all the fission gas remains within the fuel material and none leaks to the gas void above the fuel.

Under these conditions, it can be shown that:

$$Y = \left[\left(\frac{B}{A} \right)^3 \left(\frac{P_o}{T_o} \right) \left(\frac{1}{A^3} \right) \left(\frac{T_v}{P} \right) \right] X - 100 \quad (4)$$

$$\cdot \left[\frac{C - B^3}{A^3} \right] \left[\left(\frac{D_j}{D_f} \right)^2 \left(1 + \frac{Ln}{L_f} \right) - \left(\frac{C - A^3}{C - B^3} \right) \right],$$

where

$A = 1 + \alpha_f (T_f - 300)$

$B = 1 + \alpha_j (T_j - 300)$

$C = 1 + \alpha_n (T_n - 300)$

α_f, α_j = linear thermal expansion coefficients of fuel and jacket materials

α_n = average volume thermal expansion coefficient of sodium from room temperature (300°K) to operating temperature (T_n)

T_f, T_j, T_n = volume-averaged temperatures ($^{\circ}\text{K}$) of fuel, jacket, and sodium, respectively

P_o, T_o = initial pressure and temperature of gas trapped in gas-void volume

T_v = temperature of gas in void at operating temperature

P = pressure of gas in void volume or pressure of fission gas in bubbles in fuel

X = gas void volume expressed at $X\%$ of initial fuel volume

D_j, D_f = cold inside diameter of jacket and diameter of fuel pin, respectively

L_n = thickness of sodium layer above the fuel pin.

$(II/4)D_j^2L_n$ is a measure of the volume of sodium over and above that included in the annulus between fuel pin and jacket.

It should be noted that B/A is close to unity, the second term of the coefficient of X is only about 0.01 for pressures of about 300 kg/cm^2 , and hence the coefficient of X is approximately unity. The second term of the equation contains the geometric factors other than the void volume and also the thermal-expansion coefficient of sodium, which is quite appreciable.

It is obviously possible to choose X in such a manner that Y becomes zero or negative; i.e., the gas-void pressure equals or exceeds the allowable or bursting pressure without any swelling of the fuel and therefore without any burnup. By choosing a suitable value of X , the swelling can be limited to any desired value. In particular, if X is small, Y can be made less than the value given by Eq. (3C). Under this condition, the fuel swells isotropically but the limiting pressure in the gas void is reached before the fuel has swelled to the jacket and the fuel pin is still free-standing in the jacket. If X is calculated for the value of Y given by Eq. (3C), the limiting pressure is reached just as the fuel swells to contact the jacket.

If X exceeds this value, the fuel will swell to the jacket and, in the absence of axial restraint, axially by an amount exceeding the isotropic value; i.e., the final length of the swelled fuel pin will exceed $L = (D_j/D_f)L_o$. However, wall friction between jacket and fuel will limit the additional swelling to some undeterminable amount.

In general, for every value of Y calculated by Eq. (3A-3D), a value of X can be calculated by Eq. (4).

his is the minimum void volume which will just permit the mechanical restraint to operate without exceeding the allowable pressure in the void volume. If X is smaller than this value, the limiting pressure is

reached in the gas void volume before the mechanical restraint operates completely; if X is greater than this value, the mechanical restraint operates before the limiting pressure is reached in the gas void. Under this condition, swelling stops by mechanical restraint, the gas pressure in the gas void remains below the limiting pressure, and further burnup builds up fission-gas pressure in the bubbles in the fuel until the limiting pressure is reached. Under this condition it is also possible for fission gas to vent from the fuel to the gas void, since there now is a pressure differential that aids fission-gas bubbles to break through the fuel-pin surface.

U-10% Pu-10% Zr FUEL PIN IN V-20% Ti JACKET

Not all of the pertinent properties of the jacket material are known, but reasonable values can be assumed and a set of operating temperatures can be postulated, the values put into the equations, and the whole set reduced to forms which involve only geometry factors.

Assume:

Temperatures

T_f	= 1000°K (727°C)
T_j	= 923°K (650°C)
T_n	= 923°K (650°C)
T_v	= 923°K (650°C)
T_o	= 300°K (Room Temperature)

Expansion Coefficients

α_f	= $16 \times 10^{-6} \text{ }^{\circ}\text{C}^{-1}$
α_j	= 10×10^{-6}
α_n	= 340×10^{-6}
A	= $1 + \alpha_f(T_f - 300)$ = $1 + 16 \times 10^{-6} (1000 - 300) = 1.01120$
B	= $1 + \alpha_j(T_j - 300)$ = $1 + 10 \times 10^{-6} (923 - 300) = 1.00623$
C	= $1 + \alpha_n(T_n - 300)$ = $1 + 340 \times 10^{-6} (923 - 300) = 1.21182$

Also

S	= 2500 kg/cm^2 (hoop stress)
P_o	= 1 kg/cm^2 (about 1 atm helium)
L_n/L_f	= $0.1 [1 + (L_n/L_f)] = 1.1$
n	= $2 \cdot 10^{-4}$ moles per cm^3 per 1% burnup [fission-yield data from Table 13 of Ref. 6.3.18 for 365 days irradiation, 0 days cooling]
R	= $84.78 (P \text{ in kg/cm}^2, V \text{ in cm}^3)$

Then the general equations become:

$$\frac{1}{P} = [0.4 \times 10^{-3}] \left[\frac{\left(\frac{OD}{ID}\right)^2 + 1}{\left(\frac{OD}{ID}\right)^2 - 1} \right] \quad (I)$$

$$\frac{Y}{Bu} = 2.50 + 1.696 \times 10^3 \left(\frac{1}{P} \right) \quad (\text{II})$$

Mechanical Restraint:

1. Swells to Jacket + Z % Axially

$$\frac{Y}{100} = 0.990 \left(1 + \frac{Z}{100} \right) \left(\frac{Dj}{Df} \right)^2 - 1 \quad (\text{IIIA})$$

2. Tight Axial Restraint ($Z = 0$)

$$\frac{Y}{100} = 0.990 \left(\frac{Dj}{Df} \right)^2 - 1 \quad (\text{IIIB})$$

3. Relaxed Axial Restraining (Isotropic)

$$\frac{Y}{100} = 0.985 \left(\frac{Dj}{Df} \right)^3 - 1 \quad (\text{IIIC})$$

4. Isotropic Swelling with Axial Restraint

$$\frac{Y}{100} = \left(1 + \frac{Z}{100} \right)^3 - 1 \quad (\text{IIID})$$

Pneumatic Restraint:

$$Y = \left[0.985 - 2.976 \left(\frac{1}{P} \right) \right] \cdot X - 20.534 \left[\left(\frac{Dj}{Df} \right)^2 - 0.8376 \right] \quad (\text{IV})$$

Using a series of values for the Dj/Df ratio, we can calculate the amount of swelling permitted by the various mechanical restraints. Four specific cases are tabulated in Table 6.3.14. The achievable burnup for each case—i.e., for various values of (OD/ID) and the various mechanical restraints—can be obtained by dividing the Y values of Table 6.3.14 by the appropriate values of Y/Bu .

For three assumed jacket OD/ID ratios, we have, using Equations I and II, the following results:

TABLE 6.3.14. CHANGE IN Y (% SWELLING) WITH (Dj/Df) FOR VARIOUS TYPES OF RESTRAINT

Dj/Df	Y (% Swelling)			
	$Z = 0$	$Z = 5$	$Z = 5$ Iso	Isotropic
1.05	9.17	14.63	15.76	14.06
1.10	19.81	25.80	15.76	31.15
1.15	30.95	37.50	15.76	49.86
1.20	42.59	49.72	15.76	70.26
1.25	54.72	62.45	15.76	92.45

For $Z = 0$ by Equation IIIB
 $Z = 5$ by Equation IIIA
 $Z = 5$ Iso by Equation IIID
 Isotropic by Equation IIIC

TABLE 6.3.15. ACHIEVABLE BURNUP VS. (Dj/Df) FOR VARIOUS TYPES OF RESTRAINT AND FOR THREE VALUES OF (OD/ID)

(Dj/Df)	Burnup (Bu), a/o					(OD/ID)
	$Z = 0$	$Z = 5$	$Z = 5$ Iso	Iso-tropic	(Z)	
1.05	1.24	1.98	—	1.90	(4.5)	1.15
1.10	2.68	3.49	2.13	4.22	(9.5)	↑
1.15	4.19	5.08	2.13	6.75	(14.4)	↓
1.20	5.77	6.73	2.13	9.52	(19.4)	1.15
1.25	7.41	8.46	2.13	12.52	(24.4)	
1.05	1.46	2.34	—	2.25	(4.5)	1.20
1.10	3.16	4.12	2.52	4.97	(9.5)	↑
1.15	4.94	5.99	2.52	7.96	(14.4)	↓
1.20	6.80	7.94	2.52	11.22	(19.4)	1.20
1.25	8.74	9.97	2.52	14.76	(24.4)	
1.05	1.64	2.62	—	2.52	(4.5)	1.25
1.10	3.54	4.62	2.82	5.57	(9.5)	↑
1.15	5.54	6.71	2.82	8.92	(14.4)	↓
1.20	7.62	8.89	2.82	12.57	(19.4)	1.25
1.25	9.79	11.17	2.82	16.54	(24.4)	

OD/ID	Y/Bu
1.15	7.38
1.20	6.26
1.25	5.59

The calculated burnups are tabulated in Table 6.3.15 and plotted in various ways in Figs. 6.3.15 and 6.3.16. For each value of Y in Table 6.3.14, Equation IV can be used to calculate a minimum gas-void volume which will not be more restrictive than the mechanical restraints. These values are tabulated in Table 6.3.16.

FACTORS EXCLUDED FROM THE ANALYSIS

Several factors, most of which tend to increase the achievable burnup, have not been included in the analysis. Some are discussed below.

1. Thermal stresses: Temperature drop through the jacket produces a compressive stress in the inside jacket wall and a tensile stress in the outside fibers. Thus, the thermal stresses decrease the maximum hoop stress that occurs at the inside wall. The sum of tensile hoop and thermal stress in the outside fibers of the jacket will still be less than the difference between the hoop and thermal stresses in the inside fibers, except where the temperature drop through the jacket is quite large.

2. Fuel strength: We have assumed that the fuel has no strength and the surface tension effects are negligible. Actually the fuel material always has some strength and surface tension is always acting. Both factors tend to restrict the size of fission-gas bubbles and thus to diminish fuel swelling.

FIG. 6.3.15. Change in Allowable Burnup With Ratio of Jacket Diameter to Fuel Diameter: Effect of Swelling.

3. Elastic and plastic deformation of jacket: Any elastic or plastic deformation of the jacket will increase the D_j/D_f ratio and thus increase the achievable burnup.

4. Compressibility of sodium: Liquid sodium has an appreciable compressibility coefficient. The volume of sodium at temperature and under pressure will therefore be smaller than assumed in the analysis.

FIG. 6.3.16. Change in Allowable Burnup with Ratio of Jacket Diameter to Fuel Diameter: Effect of Ratio of Jacket OD to Jacket ID.

5. Deviation for ideal gas law: We have assumed ideal gas-law behavior for Xe and Kr. This is not strictly true.

6. Fission-gas escape from fuel: Any escape of fission gas from the fuel to the gas void would diminish the gas bubble pressure in the fuel. If the gas-void volume is relatively large, the total pressure within the jacket would be markedly decreased. Experiments to evaluate the magnitude of fission-gas escape are under way.

EFFECT OF TEMPERATURE

Decreasing the fuel and jacket temperatures by 100°C will increase the achievable burnup by only 6 to 7%. The effect is primarily through Equation (2) (Y/Bu) with perhaps a small effect on the allowable internal pressure through an increase in hoop stress (S). The swelling equations (4, 4A, 4B) are essentially independent of temperature (i.e., the B/A ratio is nearly independent of temperature and in any case is always within a few percent of unity).

Hence, a new burnup table can be constructed by dividing the Y values of Table 6.3.15 by a new set of (Y/Bu) values. Assuming that the allowable hoop stress and internal pressure remain constant, we have the values given in Table 6.3.17.

HIGH-BURNUP FUEL RODS

Table 6.3.14 shows that the way to get very high burnups (10% or better) is to allow isotropic swelling. Except in the most restrictive cases, however, this involves axial swelling greater than 5% (i.e., $Z > 5$), which has otherwise been assumed to be the tolerable maximum. If, however, we assume that the 5% limitation applies to the height of the core rather than the

FIG. 6.3.17. Swelling of Segments of Fuel Pin.

length of the fuel pin, then we can divide the fuel pin into separately supported segments such that only the swelling of the top segment contributes to the increased height of the core. Thus, if we divide the fuel pin into two segments, each of which swells $Z\%$, we have Fig. 6.3.17

$$H_o = L_1 + \frac{Z}{100} L_1 + L_2$$

$$H = H_o + \frac{Z}{100} \times L_2$$

$$\frac{\% \Delta H}{100} = \frac{H - H_o}{H_o}$$

and finally:

$$\frac{L_2}{L_1 + L_2} = \left(\frac{\% \Delta H}{Z} \right) \left(\frac{100 + Z}{100 + \% H} \right)$$

From Fig. 6.3.16

$$\text{for } \frac{D_j}{D_f} = 1.25, \left(\frac{OD}{ID} \right) = 1.25$$

$$Bu = 16.54\% \text{ and } Z = 24.4$$

If we limit $\% \Delta H$ to 5, then

TABLE 6.3.16. MINIMUM GAS-VOID VOLUME

D_j/D_f	X (Gas Void Volume) = X% of Fuel Volume			
	Z = 0	Z = 5	Z = 5 Iso	Isotropic
1.05	14.9	20.5	—	20.0
1.10	28.1	34.2	23.9	39.6
1.15	41.8	48.5	26.3	61.1
1.20	56.2	63.5	28.8	84.5
1.25	71.1	79.0	31.3	109.7

TABLE 6.3.17. EFFECT OF TEMPERATURE ON BURNUP

(OD/ID)	P, kg/cm ²	1/P	Y/Bu (1000°K)	Y/Bu (900°K)
1.15	347	2.881×10^{-3}	7.384	6.896
1.20	451	2.218×10^{-3}	6.261	5.885
1.25	549	1.822×10^{-3}	5.590	5.281

$$\frac{L_2}{L_1 + L_2} = \frac{5}{24.4} \times \frac{124.4}{105} = 0.243$$

and the top, separately supported, fuel-pin segment should be 24.3 % of the total fuel-pin length.

Hence, if 24.3 % of the total length of fuel pin is separately supported, the increase in height of core can be limited to 5 % even though each piece of fuel swells 24.4 % axially. Further, the longer piece may be subdivided into any number of pieces; these need not be identical, nor does the mode of subdivision need to be the same for adjacent fuel rods. Each piece can be separately supported, the only limitation being that an axial void equal to 24.4 % of the length of each sub-pin be associated with each piece of fuel. Under these conditions, a burnup of 16.5 % can be achieved. The gas-void volume would need to be 110 % of the fuel volume (Table 6.3.16).

REFERENCES

- 6.3.18. Leslie Burris, Jr., and I. G. Dillon, *Estimation of Fission Product Spectra in Discharged Fuel from Fast Reactors*, ANL-5742 (July, 1957).

6.4. COSTS

6.4.1. Alternative Estimate of Fuel-cycle Costs

As an alternative to the cost calculations given in Sections 4.5.2 and 5.0, estimates have been made based on Case II of the processes described by Ayer *et al.*^{6.4.1} The fabrication processes are assumed to be remotely controlled and carried out in a shielded facility. The process flow is that shown in Fig. 4.3.

In lieu of reliable costs for production quantities of V-20 w/o Ti tubing or rod stock, some reasonable estimate of jacket costs for an existing productive capacity had to be made. Tubing costs were generated by assuming that, in an advanced technology, productive capacity for refractory-alloy tubing exists. It was further assumed that V-20 w/o Ti tubing would be no more difficult to form than seamless type 304 stainless-steel tubing on which present-day, reliable cost figures exist. The cost of fabrication of the refractory-alloy jacket tubing, then, is assumed to be the cost of a similar size of seamless stainless-steel tubing purchased in random mill lengths in lots greater than 20,000 ft.

FIG. 6.4.1. Cost of Increments of Fabrication vs Fuel Pin Diameter at Process Use Factor of $78 \pm 4\%$: Integrated Case.

The cost of the refractory alloy is taken as the weight of alloy required multiplied by a cost per pound of alloy. Finally, the total jacket cost is generated from the total of costs for fabrication, alloy, and cutting and inspection.

The fabrication cost for metallic fuels is greatly influenced by the price paid for the jacket alloy and by the diameter of the fuel pin. Fig. 6.4.1 shows changes in fabrication cost with fuel-pin diameter for the reference fuel case. It does take into account in-

TABLE 6.4.1. FUEL AND BLANKET
FABRICATION: ANNUAL COSTS

	Reactor Output, MW(e)		
	1000	2500	6000
<i>Fuel and all blanket in refractory-alloy jacket</i>			
Total building and capital-equipment charge*	\$ 970,000	\$1,320,000	\$ 2,770,000
Total labor charges	640,000	1,630,000	3,900,000
Product material costs	2,560,000	6,330,000	14,190,000
Total annual charges	4,170,000	9,280,000	20,860,000
Cost per kg fuel	117	110	107
Cost per kg axial and inner radial blanket	117	110	107
Cost per kg outer radial blanket	73	55	45

*Fuel, axial and inner radial blanket in refractory alloy jacket—
outer radial blanket in stainless-steel jacket*

Total building and capital-equipment charge*	\$ 970,000	\$1,320,000	\$ 2,770,000
Total labor charges	640,000	1,630,000	3,900,000
Product material costs	2,210,000	5,470,000	13,130,000
Total annual charges	3,820,000	8,420,000	18,800,000
Cost per kg fuel	117	110	107
Cost per kg axial and inner radial blanket	117	110	107
Cost per kg outer radial blanket	51	33	24

Fuel and all blanket in stainless-steel jacket

Total building and capital-equipment charge*	\$ 970,000	\$1,320,000	\$ 2,770,000
Total labor charge	640,000	1,630,000	3,900,000
Product-material charges	1,400,000	3,460,000	7,750,000
Total annual charges	3,010,000	6,410,000	14,420,000
Cost per kg fuel	75	69	67
Cost per kg axial and inner radial blanket	75	69	67
Cost per kg outer radial blanket	51	33	24

* Total building and capital charges are derived from total building and capital equipment costs, which are: \$8,120,000, \$13,450,000 and \$20,770,000 for reactor output of 1000, 2500, and 6000 MW(e), respectively.

creases in jacket thickness, which should accompany increases in fuel diameter. The figure plainly shows the great influence of cost of product materials upon the cost of fabrication. The single horizontal line through the product materials bar indicates the cost of product materials when the cost of jacket material is that of present-day stainless steels.

Fabrication costs for blanket, 11.3 mm OD, and fuel, 3.73 mm OD, are shown in Table 6.4.1. The costs shown are "shop-cost type," in that no general operating expenses, overhead and administrative charges, or interest charges have been made against the process.

Table 6.4.1 is calculated upon plant throughput sufficient to satisfy the fuel and blanket needs of a 1000, 2500, or 6000 MW(e) reactor or reactor series at a fuel burnup of approximately 54,000 MWd/metric tons. The plant is assumed to operate two shifts per day, five days per week, with a total throughput of 42,000 kg/year of fuel and blanket. In all cases the assemblies fabricated are those described in the reference case.

"Total building and capital-equipment charge" includes:

1. The cost of in-cell space to provide for fabrication equipment.
2. All out-of-cell space required by the fabrication.
3. All handling and service equipment required by fabrication.
4. The installed cost of all fabrication equipment—both in-cell and out-of-cell.

The total annual charges include one-thirteenth of the building costs and one-fifth of the cost of installed capital equipment.

The highest single cost in Table 6.4.1 is the product-material cost. Product materials for the fabrication of metal fuels are crucibles, molds, end fittings, jacket tubing, etc. Given below is a breakdown of product-material costs for the fabrication at a throughput of 94.2 kg per day of fuel and axial blanket:

	% of PM Cost
Crucibles	1.60
Molds	17.45
Pallets	0.96
(V-20 w/o Ti) jacket end fittings	4.40
(V-20 w/o Ti) jacket tubing	63.14
Sodium	0.01
Bundle hardware	12.44
Total	100.00

REFERENCES

- 6.4.1. J. E. Ayer, D. A. Jones, D. D. Ebert, T. A. Buczwinski, and J. V. Sana, *A Cost Estimate for Remote Refabrication of Metallic Fuels*, ANL-6950 (to be published).

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1000-MW(e) METAL-FUELED FAST BREEDER

Document Title REACTOR

Date of Document June 1966

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W. B. Loewenstein, W. J. Mecham, D. Meneghetti Contract No. W-31-109-eng-38

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