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Proceedings of the
International Conference on
**SODIUM TECHNOLOGY and
LARGE FAST REACTOR DESIGN**

November 7-9, 1968

Part II. Sessions on Large Fast Reactor Design

Conference Chairman
D. Okrent

Program
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L. W. Fromm, Large Fast Reactor Design

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Preface

This report is the proceedings of the fourth of a series of topical conferences sponsored by Argonne National Laboratory and the United States Atomic Energy Commission on specific topics related to the development of fast power breeder reactors. It is bound in two volumes: Part I contains papers presented at the Sessions on Sodium Technology, and Part II contains papers presented at the Sessions on Large Fast Reactor Design. The Table of Contents in each volume lists all of the papers presented at the conference.

The Proceedings of the first of these conferences, held in October 1963 and entitled "Breeding Economics and Safety in Large Fast Power Reactors," were issued as ANL-6792. The proceedings of the second, held in October 1965 on "Safety, Fuels, and Core Design in Large Fast Power Reactors," were issued as ANL-7120. The proceedings of the third conference, held in October 1966 on "Fast Critical Experiments and Their Analysis," were issued as ANL-7320. A related conference on "The Physics of Breeding" was held at Argonne in October 1959, with the proceedings issued as ANL-6122.

We at Argonne appreciate very much the assistance of all who participated in this conference. In particular, the open discussion and the willingness to examine controversial questions contributed to the value of these meetings. The cooperation and last-minute efforts of the speakers during the first informal session on Sodium Technology and in the final general discussion periods warrant special thanks. We wish also to express our appreciation to the U. S. Atomic Energy Commission and the participating organizations from abroad for making possible such meetings in which recent development on specific topics are reviewed and the basis for a cooperative effort on the remaining problems established.

These proceedings have been edited by J. E. Draley, L. W. Fromm, A. Glassner, Joyce Kopta, and D. Okrent, with the direct assistance of the scientific secretaries of the various sessions. In the interest of hastening publication, the speakers have not been given an opportunity to edit the discussions. The editors hope that a reasonable portrayal of the actual discussion has been accomplished.

I wish to acknowledge the able guidance and assistance provided by J. E. Draley and L. W. Fromm in formulating the Program. Finally, on behalf of all the participants, I express our appreciation of the efficient manner in which Dorothy Carlson, Gail Ahlvin, Rosalie Deskin, and Elizabeth Wallis handled the myriad of administrative details and thereby contributed to an effective meeting.

D. Okrent
Conference General Chairman

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SESSIONS ON LARGE FAST REACTOR DESIGN

SESSION LFRD-I

Thursday Morning, November 7, 1968

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Argonne National Laboratory

Secretary: D. E. Lutz
Argonne National Laboratory

Sodium Technology Considerations in Fast Reactor Design

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1. Introduction

At the 1965 Fast Reactor Conference at Argonne, status of the fast reactor technology was covered broadly and a number of design studies were presented. These studies included gas-cooled and steam-cooled fast reactors as well as sodium reactors. Since 1965, judgments reflecting probability of economic and technical success has led to worldwide acceptance of sodium as the preferred coolant for fast reactors. Thus, the present conference covers the current status of sodium technology and the design of large sodium-cooled fast reactors.

The 1000-MWe design studies this time represent judgments on extrapolation of the state of the art to the period 1975 to 1980 as stipulated by the AEC for these design studies. These studies, as compared to those of 1965, are characterized by more attention to detailed plant design and more detailed economic evaluation, leading to choice of plant conditions. In the world at large, the corner has been turned in starting the transition from paper studies and idealized tests to the reality of large-scale equipment. The reality is typified by commitment and design and construction progress on power demonstration plants BN-350 and PFR, by the extensive fast-flux-irradiation experience in DFR, EBR-II, and now in Rap-sodie, and by the completion of SEFOR construction.

There is also a factor of quickening pace of development and foreshadowing of commitments in the next year or two. Russia has already committed a second-generation power plant, BN-600; the UK plans to commit a second-generation unit within two years. The design of Phenix is well along, and commitment is scheduled for next year. Three serious design studies with major utility groups are underway in the US with potential for one or more large plant commitments in the next two years. Large-scale design and development work for the FFTF is underway. Serious plant studies in Germany, Japan, and Italy foreshadow additional projects. The development required by these plants will provide definite focus, and set the pace and main lines of development for the next decade.

Although a considerable variety of design approaches are being pursued, there appears to be a trend

towards agreement regarding the useful and feasible areas of technology. The differences in design are differences in quantitative judgments on cost trade-offs, and different judgments on the relative weighting of operating and maintenance costs and reliability versus lower first costs. The designs tend to be optimized for power costs for a specific economic environment rather than for arbitrary targets in specific power, breeding gain, or top temperature. Indeed, more realistic design and cost analyses have made it clear that increased power costs result from arbitrarily selecting high values of any single index of performance.

Since a major session will be devoted to presentation of the 1000-MW design studies, the remainder of this paper is restricted to discussion of progress made in selected areas of technology that strongly affect plant design or potential plant performance, and in which better perception of the potentials and limitations of the technology has developed in the past three years. Plainly, a comprehensive survey of sodium fast reactor development work is not possible in this introductory paper. We have selected a few topics that bear most directly on current efforts in the design and technology pertinent to design choices of near-term projects. The first part of the paper is concerned with broad aspects of reactor plant design, and the latter part touches upon specific technological aspects, namely, steam-generator design, materials considerations, and material behavior in sodium and under irradiation. Since fuel plays a central role in setting reactor design conditions and in determining economics, and since it is not discussed elsewhere in this conference, it will be treated in somewhat more depth than the other topics.

2. General Aspects of Sodium Fast Reactor Plant Design

2.1. Plant Configuration

At the 1965 Argonne Conference, two general approaches to sodium fast reactor plant arrangement were evident. The first and more conventional design places the reactor vessel, the heat exchangers, pumps and other components of the primary system, and the interconnecting piping in a large building

containing an inert atmosphere to preclude a sodium fire in the event of a sodium leak. The second "tank" approach takes advantage of some favorable properties of sodium, namely, its low operating pressure and low cost, and submerges the entire primary system including vessel, pumps, and intermediate heat exchangers in a large tank filled with sodium. The tank is made part of the primary-coolant loops, with the heat exchangers discharging effluent sodium directly into it and the pumps taking suction directly from it.

In the first category are proposed designs by Combustion Engineering,⁽¹⁾ Westinghouse,⁽²⁾ and Atomics International⁽³⁾ in this country, and designs by Russia⁽⁴⁾ and Germany⁽⁵⁾ abroad. With the notable exception of EBR-II, all operating sodium plants to date have utilized this approach. Aside from the factor of experience, its major apparent advantage is that of separation and decoupling of components. A disadvantage is the large volume of the shielded pipeways and equipment cells of the spread-out system, all of which are made inert, and in general are part of a large and complex containment structure. Also, the design of the piping system connecting the components is complex, requiring large expansion loops to accommodate thermal expansion. Development of even small leaks may require extended periods of downtime for repair, and large breaks in piping or components may reflect back on the reliability of core cooling.

The advantages that accrue from the tank design are relative insensitivity to leaks in the primary system and a more compact plant arrangement. One pays for these advantages with a closely coupled system in which the mechanical interaction of various components on one another are accentuated, and where one must accommodate temperature differences and thermal expansions in a relatively confined area. On the positive side is the ability to use movable seals with which small amounts of leakage are acceptable. The tank approach also leads to complex structure in the closure region of the tank, that is, the roof of the reactor tank must act as a shield, a gas enclosure, and the support of the various components that must penetrate through it into the tank. To summarize (in an oversimplified manner), the tank concept appears to have potential cost and safety advantages, whereas the piped-loop concept appears to have the advantage of a more straightforward mechanical design. Examples of the tank-type approach are the designs by General Electric⁽⁶⁾ and Babcock & Wilcox^(6a) in this country, and the French⁽⁷⁾ and British⁽⁸⁾ designs abroad.

In the period since the 1965 Argonne Conference,

a considerable amount of more detailed design work has been performed by all of the organizations listed above, building on sharply increased worldwide efforts in sodium technology and some additional plant-operating experience. It is noteworthy that as a result of this work no changes have been made by any of the organizations in their concept of a basic plant arrangement. Experience of the British with detailed analysis, model tests, and actual construction of the PFR vessel has evidently confirmed their preference for the tank concept. It is of interest to note that some organizations which have had actual plant experience with the vessel and piping approach on sodium reactors, namely, the British, the French, and the General Electric Company in this country, have chosen the tank approach for their large power reactors. On the other hand, other organizations such as Atomic International and the Russians have also had experience with the vessel and piping approach and have retained this concept for their large power reactors. At this point in time, therefore, it appears that no serious technological problems have been uncovered which would preclude the success of either approach. Perhaps future experience will depend as much on the execution of the specific designs as on the basic concept. In this area of general plant arrangement, the properties of sodium appear to provide a flexibility in basic design approach that is not obtained with other coolants.

2.2. Fuel Handling

2.2.1. Refueling

The transparent nature of water makes refueling in PWR's and BWR's relatively straightforward in concept. The opacity of sodium and its high radioactivity level in a sodium plant introduce some added complexity to the refueling process. Several different approaches to refueling a sodium reactor have been suggested and a number successfully demonstrated. These can be characterized according to whether the refueling equipment is permanently located within the reactor vessel, whether the refueling equipment is brought over the reactor head and grappling equipment inserted through a port in the head, or whether refueling is accomplished by removal of the head, which is done in an inert-atmosphere refueling cell above the reactor. At the time of the 1965 Argonne Conference, very limited experience with the first two approaches and variations and combinations thereof had been obtained. Since then, experience at EBR-II, DFR, Fermi, and Rapsodie⁽⁹⁾ has provided additional information.

The concept of a refueling cell is currently being

FIG. 1. Full-scale Mockup of SEFOR Refueling Cell.

implemented at the SEFOR reactor.⁽¹⁰⁾ Operation of a full-scale mockup of the SEFOR refueling facility (see Fig. 1) has shown the general feasibility of the refueling-cell approach.⁽¹¹⁾ Experimental data obtained in the full-scale mockup confirmed small-scale tests and analytical work which indicated that sodium

deposition in the cell is negligible if the refueling temperature of the sodium is below 500°F. Extensive changes to equipment were made by switching to an air atmosphere. Inspection and small repairs were done conveniently in the inert atmosphere by man entry in an impermeable suit. Also demonstrated were the

excellent visibility available for refueling and the advantages of utilizing a combination of television and high-magnification periscopes, as well as the cell windows to guide operations. Additional experience with the refueling-cell concept is being obtained as the operation of SEFOR commences.

There also has been extensive design and analytical effort on the refueling-cell concept for power reactors. Work at General Electric has indicated that, on a time and motion basis, the refueling period for a fast sodium-cooled reactor with the refueling-cell approach should be about the same as that for a BWR.

2.2.2. Storage of Spent Fuel

Two days after shutdown from full power, a fast reactor fuel element will be generating decay heat of 15 to 50 kW, depending upon the size of the fuel element and its specific power. Although it is desirable to remove the fuel element from the confines of the reactor vessel for purposes of flexibility to permit fuel examination, preparation for shipment (and decoupling of the refueling interval from the shipping schedule so as to reduce inventory costs) poses a problem because of the need to provide a means of decay-heat removal with the necessary backups to assure reliability.

According to one manufacturer, whose design of a prototype plant is being detailed, a refueling machine with the capability of removing fuel directly from the reactor after operation "is considered to be beyond the state-of-the-art today" because of the decay-heat problem.⁽³⁾

For this reason, some proposed fast reactor designs, which do not utilize a refueling cell, provide for storage of the fuel in outer positions of the reactor vessel for vessel designs or, as with EBR-II and PFR, in areas of the reactor tank away from the core. These elements are cooled by primary reactor sodium until the decay heat is reduced to a level that does not produce an excessive requirement on the refueling machine.

Some progress in attacking the problem has been made since the last Argonne Conference in connection with the refueling-cell concept. Designs by General Electric and Westinghouse envision removal of the fuel from the vessel during the refueling period and transfer to the sodium-filled decay-storage tank. This tank is independent of the main primary system, and accessibility to the tank is possible and proposed during reactor operation. The design of this transfer equipment has been studied at General Electric. For the small-size fuel elements contemplated for a demonstration reactor, the maximum heat generation

in a fuel bundle two days after shutdown from full power is approximately 16 kW. Transfer of this fuel element between the reactor and the fuel-storage tank is made by placing the element in a finned transfer pot containing sodium, designed to provide natural-circulation heat transfer between the interior of the fuel channel and the inner surface of the finned pot. The decay heat is removed from the finned pot directly to the argon cell atmosphere by means of a flow of argon directed over the fins by a small blower. Calculations indicated that this arrangement will keep the maximum sodium temperature below 400°F. In the event of failure of the blower and transfer mechanism, the sodium in the pot would asymptotically reach a temperature below 1000°F, so that no fuel damage would occur. This temperature may be adequate for routine operation so that the blower could be eliminated. In the case of the 1000-MWe design by G.E., a similar scheme is utilized; but in this case additional surface for transfer of heat to the cell is provided by means of a closed natural-circulation NaK loop which is inserted in the transfer pot.

It appears to the authors that keying the fuel-element decay period to the reactor refueling cycle will entail appreciable cost, since it appears advantageous in commercial application of fast reactors to consider refueling periods of a year or more. For those refueling schemes which do not utilize a refueling cell, there are two approaches to this problem:

- (1) make the reactor vessel or the reactor tank accessible during reactor operation for removal of the spent fuel after the decay heat has reached the level where preparation for fuel shipment can begin;
- (2) provide a reliable heat-removal system in the fuel-transfer machine such that the high level of decay heat can be accommodated from elements which have operated just prior to the refueling interval.

Both approaches appear feasible but difficult. The former involves questions of safety as well as problems of contamination of the transfer machine with radioactive gases and deposition of sodium; the latter will undoubtedly add to the complexity and cost of the fuel-transfer machine.

Thus, as with the case of general plant arrangement, there are alternative means to design for fuel handling in a sodium reactor. The experience to date is encouraging (with some exceptions; see next section). The refueling-cell scheme appears to have advantages for fuel handling in terms of flexibility and simplicity of its mechanical equipment, but because of its large

size and its potential constraints on plant layout may entail a higher initial capital cost.

2.3. Reactor Repair

At the time of the 1965 Argonne Conference, the difficulties at Hallam were well-known. Aside from the basic problem of graphite swelling, it was disturbing to find that merely gaining access to the sodium-filled vessel and removing the defective components took approximately six months.⁽¹²⁾ Since that time, the British DFR has successfully replaced its blanket region which had undergone severe distortion.⁽¹³⁾ This took four months. A small pipe leak in DFR near the reactor vessel caused a shutdown of 11 months, although the actual repair was effected fairly easily once the correct diagnosis was made. Currently, we are following the repairs in the Fermi reactor involving a shutdown in excess of two years. If, on the basis of hindsight, one were to look for improvements over past designs of sodium reactors, it could certainly be for added facilities for diagnosis and repair in and near the reactor vessel.

Similar considerations apply to the refueling equipment. EBR-II has experienced difficulties with its under-sodium fuel equipment, although the repairs to date have taken many weeks rather than years.⁽¹⁴⁾ The initial damage to the Fermi refueling equipment would have been extremely difficult to repair had it occurred after the reactor had reached a high level of activity in the vessel region. There is one case of a fuel-handling machine that malfunctioned while transporting a fuel element and which required a period of years for fission product decay before the element could be removed.

The message is clear that future designs should incorporate much better means for inspecting components. They should provide efficient access for diagnosis and for removal and replacement of failed core components (or fuel-handling equipment) which are known or suspected to be defective. In our opinion, it appears that the refueling-cell approach has advantages in this regard because of the access to the vessel and refueling equipment that it provides and the ease of viewing. Further, the ability to remove elements with high levels of decay heat from the vessel (see above) may save many months in the event major inspection or repair is required. On the other hand, one must be encouraged by the ability and ingenuity of the Hallam, DFR, and Fermi operators to develop visual aids and special-purpose tools that, in time, permitted repairs through ports in the vessel not originally intended for this purpose. On this basis, we might expect improved capability and performance

if flexibility and special devices for repairs are provided in the basic design.

2.4. Reactor Safety

2.4.1. Sodium Voiding and Core Configuration

Sodium acts as a moderator, tending to soften the neutron spectrum and reduce reactivity. It also acts as a neutron scatterer, tending to reduce leakage and increase reactivity. In large fast reactors, the former effect dominates, so that loss of sodium increases reactivity, especially for partial loss of sodium near the core center. At the 1965 Argonne Meeting, the designs proposed by Atomics International, General Electric, and Westinghouse proposed core geometries intended to minimize the sodium void coefficient. In the case of A.I. and Westinghouse, this was accomplished by means of a "modular" core arrangement in which fissile regions were surrounded by blanket regions. In the G.E. concept, a small sodium coefficient was obtained by keeping the height of the core small ("pancake"). In each case, the low sodium-void coefficient results from high leakage into the blanket region.

In the intervening period, one notes a significant change in philosophy by G.E. and A.I.⁽³⁾ Studies at G.E.⁽¹⁵⁾ indicate that under credible accident conditions the presence of a positive void coefficient does not significantly affect the safety of the reactor. It is only when one considers accidents of a hypothetical nature in which one assumes failure to scram concurrently with an arbitrary reactivity input or almost instantaneous loss of coolant flow that the void coefficient becomes significant. Depending upon the creativity of the person hypothesizing the accident, the results can be dramatic. However, studies indicate that dramatic results can be obtained whether there is a large positive void effect or even a large negative void effect. For example, a large negative void effect can increase the severity of the accident under a loss-of-coolant situation by delaying prompt criticality until the point at which acceleration of molten fuel tending toward core compaction is large, leading to high rates of reactivity insertion.

The conclusion of both the G.E. and A.I. work is that reactor safety can be most effectively enhanced by attention to instrumentation and coolant-system design to preclude core voiding. This approach has been already taken by all of the European designers. Westinghouse, on the other hand, still retains its modular core design which, aside from considerations of sodium void, has, in their view,⁽²⁾ a significant advantage in permitting one to go from a small-size reactor

to a large reactor by adding additional modules to the core and additional modularized coolant loops.

2.4.2. Hypothetical Accidents

As indicated above and in Ref. 15, it appears that sodium fast reactors can meet the high standards of reactor safety of the present generation of power reactors. Sodium reactors have an advantage in that the sodium coolant operates well below its boiling point, so that there is a wide margin (hundreds of degrees and factors of the order of 3 in power) before such phenomena as the onset of boiling or "burnout" could occur. In addition, the low operating pressure in a sodium reactor eliminates the concern about rapid blowdown of the primary system in the event of a failure, and avoids large reaction forces on the primary system, the containment, and nearby equipment. The low operating pressure makes it possible to place a safety vessel outside of the reactor vessel so that even in the event of failure of the vessel, the reactor core remains covered with sodium. Thus, in a fast reactor there can be wide margins between the normal operating point and accident conditions that could result in radiation exposure to the general public.

It is in the realm of hypothetical accidents that the situation with regard to fast sodium reactors is less clear. By tradition, a hypothetical accident may be defined as a series of extremely low-probability events (some of which may be nonmechanistic, that is, with no obvious physical mechanism) that result in serious reactor damage and that determine the containment required. There are three technical areas which can be called upon to construct serious hypothetical accidents in a sodium-cooled fast reactor; (a) meltdown and core compaction, (b) occurrence of sodium superheat and consequent rapid voiding of the core, and (c) explosive interaction of molten fuel and sodium. Each area was identified at the 1965 Argonne Conference and has since been discussed in some detail at the Fast Reactor Safety Conference at Aix.⁽¹⁶⁾ Briefly, the situation is that upper, but physically unrealistic, limits can be put on the potential consequences of accidents involving such phenomena. Although there has been some progress in basic understanding since 1965, there has been little progress towards a realistic assessment of accident potential under actual conditions of reactor operation. Further work is plainly required in this area.

It may be possible to design for unrealistic upper-limit consequences of hypothetical accidents as has been done with the small fast reactors built in this country. However, in the case of the large fast power reactors, the economic consequences of this approach

are not yet clear. Further, the added complexity that is required to accommodate the hypothetical accidents of extremely low probability may affect adversely the behavior under more probable conditions. Since 1965 some progress has been made in an alternative approach which recognizes that hazards exist in any human activity and which attempts to provide quantitative evaluation of hazard probability to guide the reactor design and to determine its suitability. This approach has been most intensively pursued in Great Britain⁽¹⁶⁾ and in the US space programs. Work at G.E.⁽¹⁵⁾ has indicated that it should be possible to incorporate instrumentation and controls on large fast reactors that would make the probability of an event with consequences to the general public sufficiently low to be acceptable by any rational standards.

Hypothetical accidents are, by definition, events of such low probability that they are beyond normal human experience and the application of intuition is not valid. In the case of the newly developing fast reactor, one cannot call upon precedent and experience to justify design choices; the use of the more quantitative tools of probability and reliability analysis appears mandatory. Significant effort is warranted to place these quantitative tools on a firm basis and gain acceptance of their use.

2.5. Cost Experience

The designs to be discussed at THIS CONFERENCE are optimized with respect to economic performance foreseen in the 1980's. However, benchmark cost experience for large fast power reactors will not be in hand until the first generation of demonstration reactors planned for operation in the next decade. Thus, there is interest in any current cost experience on sodium reactors. The SEFOR reactor provides some information on this subject.

The precommitment cost estimate for SEFOR was approximately \$12.5 million. Its actual cost of construction is under \$15.5 million. (Neither figure includes contingency for plant changes that might be necessary as a result of operating experience.) After a year of detailed design work and completion of the licensing activities leading to the construction permit, a revised estimate indicated a cost of approximately \$15 million.

The actual cost as related to first cost estimate compares favorably with experience with the more mature water reactors and indeed with fossil plants constructed in this period of rapid inflation of construction costs. Further, it is encouraging that, after the detailed designs of equipment had been established and necessary changes for licensing were incorporated into the design, costs were estimated rather closely.

Although one cannot draw gross conclusions on the basis of this specific experience with a small plant, nevertheless, it provides some basis to believe that the economic analyses of the large plants are becoming increasingly realistic and provides basis for confidence that once the details of the design have been established, fairly good cost estimates can be made.

3. Sodium Technology Effects in Specific Components

3.1. Material Considerations

3.1.1. Corrosion and Mechanical Properties in Sodium

Austenitic stainless steel cladding and oxide fuel continued to be the near-term preference for nearly all reactor designs. Economic analysis continues to show that fuel lifetime is the dominating factor in fuel-cycle economics, in the range of average burnup from 50,000 to 100,000 MWD/T. Although a philosophical case might be made for fixed targets for specific power, breeding gain, or top temperature, it is expected that for the next decade these parameters will be set primarily in those combinations that are consistent with the longest fuel lifetime.

Sodium technology enters in the basic selection of materials for the reactor core, and particularly fuel-cladding materials, under the requirement that the corrosion of the material stay within acceptable limits. Recently it has become evident that exposure to sodium at high temperatures may have significant effects on the long-term strength and creep properties of the material. Properties measurements of materials after long exposure in sodium were relatively few as of 1965. To the extent that only simple properties, such as tensile strength of relatively thick specimens were commonly measured, one could say that the usable

properties of stainless steel materials were essentially the same whether tested in sodium, inert gas, or vacuum. Since then, there has come the recognition that in addition to normal effects of thermal aging, nearly all alloys have a "metabolism" in the sodium environment that will significantly affect the usable mechanical properties of the material, especially in critical applications such as fuel cladding and steam-generator tubing. Such effects increase with increasing temperature, and probably become limiting for some applications at temperatures near 1300°F for exposure times of 2000 to 3000 hr and below 1300°F for longer exposures. The changes of mechanical properties are associated with the formation of a compositionally modified zone for a distance of the order of 2 to 6 mils in from the surface of the material (see Fig. 2). This effect is in addition to the commonly observed formation of a thin ferritic layer which has been observed on sodium-exposed materials and which has, of itself, negligible effects on mechanical properties.⁽¹⁷⁾ Although there is still insufficient long-term data available to allow conclusions of the precise effects of these properties changes on fuel-design lifetimes, there is a clear signal to the designer that substantial degradation of properties must be accommodated as temperatures approach 1300°F. These effects are barely discernible at 1200°F and times of 20,000 hr. The selection of hot-spot surface temperatures not much in excess of 1200°F accordingly appears prudent for first-generation demonstration reactors.

3.1.2. Radiation Effects of Integral Tests

Acceptable long-term corrosion and mechanical properties in sodium are necessary but obviously not sufficient conditions for selection of preferred fuel-cladding material. The factor with even greater weighting in the selection is the resistance of the ma-

FIG. 2. Surface Effects from Exposure of Stainless Steel to Flowing Sodium at 1300°F. Left side shows ~0.3-mil ferritic layer and ~3-mil zone with modified structure and composition after 1750 hr. Right side shows ~0.3-mil ferritic layer and ~6-mil modified zone after 3000 hr. (Type 316; <10 ppm O₂; sodium velocity of 17 fps, 875°F cold leg.)

FIG. 3. Swelling Rates of Mixed-oxide Fuel and Type 316 Stainless Steel Cladding after Irradiation in EBR-II at $\sim 1050^\circ\text{F}$.

material to loss of useful properties as a result of irradiation.

The last two years have been signal successes in integral tests in fast flux of oxide fuel clad with stainless steel at burnups up to about 75,000 MWD/T in EBR-II⁽¹⁸⁾ and to similar burnups in DFR. When these tests were mounted in 1965, the main element of concern was the known trend to loss of ductility of the cladding. Since then, several new factors have become evident, namely,

- (1) decreases in density of the cladding alloy, in some cases as high as $\sim 1\%$ per 10^{22} nvt;
- (2) the effect of alloy structure in decreasing the loss of postirradiation ductility (for example, titanium-doped Type 304 or 321 Stainless Steel, or carbide-agglomerated Type 304);
- (3) the effect of strain rate and stress pattern on the elongation to fracture.

Thus, along with the initial success of integral testing in fast fluxes has come quantitative recognition of several variables that affect the dimensional expansion of fuel pins with burnup, in addition to the expected strain of cladding by the swelling of fuel.

The relatively low swelling rate for the oxide-fuel body itself, which had been observed in thermal irradiations to 125,000 MWD/T and in special capsules to burnups of over 300,000 MWD/T,⁽¹⁹⁾ has been confirmed by the fast-flux irradiations in EBR-II. The apparent higher swelling rate in fuel pins in the fast flux is now attributable to the swelling of the cladding

itself, superimposed on the fuel-induced strain of the cladding. Figure 3 summarizes oxide-fuel- and clad-swelling data obtained to date in EBR-II.

3.1.3. Density Decrease of Cladding in Fast Fluxes

The decrease in density of the cladding was originally believed to be due to nucleation and expansion of helium-filled bubbles, the helium being produced by n, α reactions with alloy constituents such as nickel-58 and nitrogen-14. The amount of helium produced continues to be puzzling in several respects. Recent quantitative measurements⁽²⁰⁾ confirm the observations elsewhere that the amount of helium produced is greater by an order of magnitude than that which would be calculated from known and estimated n, α cross sections of the alloy constituents present. Part of the discrepancy is removed on recognition that continued irradiation of nitrogen-14 and its daughter products, boron and lithium, leads to the production of 4 atoms of helium per initial nitrogen atom. However, the situation is clouded by several unexplained observations:

1. No simple correlation is yet evident between the amount of helium found and the initial chemical composition of the alloy.
2. The volume of voids observed, or density decrease, is much greater than that expected from the amount of helium measured.
3. There is no direct correlation observed between the loss of ductility of a given alloy and the volume change it experiences.

At present one can only seek hypotheses in which the amount of void production is only indirectly related to the amount of helium present. One such view at present is that void production is due to vacancy agglomeration rather than to the nucleation and growth of helium bubbles. In this model the role of helium is to stabilize the vacancy agglomerates once they are formed, so that the normal processes of formation and collapse of vacancies is shifted so as to inhibit slightly collapse of vacancies and thus favoring increased average life and opportunity for growth of vacancy agglomerates. Figure 4 illustrates typical voids observed in irradiated steels.

3.1.4. Effects of Neutron Spectrum on Swelling of Cladding Alloys

The amount of void formation observed at a given total irradiation in the fast flux varies with the composition and structure of the alloy. If the factors which already show trends to relatively small swelling can be confirmed and regularly reproduced, the problem of cladding swelling becomes reduced to manageable proportions from a design standpoint. The swell-

FIG. 4. Voids and Swelling of Irradiated Stainless Steel (1.4% Volume Increase, Irradiated at 1220°F in EBR-II to 3.4×10^{22} nvt [$6.7 \times 10^{21} > 1$ MeV], Type 304, Average Void Size 282 Å, Magnification 230,000×).

ing observed to date is a volume change of 3 to 6% at 75,000 MWD/T, or 1 to 2% change in length and diameter of the fuel pins.⁽¹⁸⁾

The success of the integral tests to date are encouraging, but do not yet provide full assurance that the initial cores of large fast reactors will reach burnups of 75,000 MWD/T. The neutron spectra of EBR-II (and DFR) are somewhat harder than those of large, dilute, ceramic-fueled cores which power reactors will have. The spectrum differences lead to a calculated factor of about 1.7, which should be applied to the flux in EBR-II in estimating the equivalent cladding damage produced in a large ceramic-fueled reactor.⁽²¹⁾ (The theory is sufficiently uncertain so that the factor may well be as high as 2.2 or as small as 1.2.) This implies that we may be "over-testing" the materials-irradiation specimens if we compare only the total neutron flux exposure.

On the other hand, for actual fuel-pin tests, most of the test fuels in EBR-II use fully enriched U-235 oxide along with the PuO₂ to increase the kW/ft attained in the available flux. Because of the extra fissile enrichment, the total fast-flux exposure of the cladding per unit of fuel burnup is accordingly lower by about a factor of 3.6 than it will see in a large ceramic-fueled core at the same burnup. The overall combined effect of the harder spectrum but lower flux is that we are subjecting the cladding to the proto-typical strain from fuel expansion but "under-exposing" cladding by a factor of about two ($= 3.6/1.7$) in terms of neutron damage. The net effect of these considerations is that fuel tests to date support an initial burnup target of about 50,000 MWD/T for demonstration power reactors, with good prospects of higher burnups for subsequent reloads.

This is brought into perspective when one recalls

that when the first demonstration units of the present water-cooled reactors with slightly enriched fuel were committed, the tested burnup of uranium oxide fuel—with only a handful of samples—was only about 3,000 MWD/T. However, the initial economic targets of 7,000 to 10,000 MWD/T were attained, and present burnups are over 20,000 MWD/T.

It has been observed that effects generally similar to those produced by neutron irradiation—including voids—can be produced in various alloys by brief bombardments by helium ions and protons in accelerators.⁽²⁸⁾ This offers the interesting prospect of speeding up the basic learning cycle on the alloy factors which determine swelling and ductility effects.

3.1.5. *Effects of Alloy Composition and Applied Stress Patterns*

There are several promising directions of development to reduce the swelling and loss of ductility of austenitic claddings. In limited tests so far, titanium-containing alloys such as Type 321⁽²²⁾ and titanium-modified Type 304⁽²³⁾ show lower swelling rates than Type 304 or 316 stainless steel. Specially heat-treated Type 304 ("carbide agglomerated") shows smaller loss of ductility after irradiation than the unmodified materials.⁽²⁴⁾ Cold-worked Type 316 cladding also appears to show smaller volume expansion and less loss of ductility than does annealed Type 316.⁽²⁵⁾

The integral tests of fuel pins have given much better results than might be expected from the sharply reduced ductilities observed in the irradiation tests of tensile specimens. This suggests the importance of simulating the actual types of loading applied to the cladding in a fuel pin if non-fuel materials-irradiations tests are to have significance for fuel design. Two effects which may be important (and which tend to support the better performance of integral tests) are the strain rate and loading-pattern effects recently uncovered. The elongation to fracture of irradiated alloys shows a strain-rate dependence.⁽²⁶⁾ A broad minimum in ductility (testing at 1200 and 1300°F) is observed with several materials for strain rates of 0.001 to 0.01 per minute. Ductilities of twice the minimum values are observed at high strain rates (0.1 to 1 per minute) and at low rates (10^{-4} to 10^{-7} per minute). The strain rates of practical interest are of the order of 10^{-7} per minute for fuel swelling, and of the order 0.1 per minute for potential strains due to rapid power changes. Thus the tensile elongations as usually measured may provide a somewhat pessimistic view of effective ductilities after irradiation.

Relatively uniform loading of the cladding is believed to be applied by steady-state expansion of fuel. Under these conditions, the effective ductility

can also be substantially greater than that measured by usual tensile or burst tests. For example, a novel test method capable of measuring the residual ductility of small rings cut from irradiated fuel-pin cladding has been developed, using a deformable mandrel.⁽²⁷⁾ The bulging of the mandrel was chosen to simulate the strain applied to cladding by uniform expansion of fuel. Unexpectedly high ductilities are measured by this technique for both irradiated and unirradiated cladding. This is apparently due to the uniform support of the specimen, which establishes a nearly uniform strain pattern due to the shear forces between the sample and the expanding mandrel. The local percent strain at rupture is made to be nearly the same as the average over the whole specimen. The unsupported gage length of conventional tensile or tube-burst tests permits local necking and thus higher local strain than the average over the specimen.

The loading of cladding by fuel expansion may approach the uniform case. Under long-term steady-state operation, cracks in the fuel are believed to be largely healed. The fission-gas pressure component of clad strain, however, is analogous to the unsupported gage-length case, so there may be a further incentive for limiting fission gas pressure to a low value.

3.2. *Design Consequences of Clad Swelling*

Initially, the interest in the phenomenon of clad swelling stemmed from the assumption that clad swelling was directly related to the loss of ductility of the alloys. However, it is not as yet established that alloy modifications which show lower swelling will also have better ductility. Consequently, it is prudent at present to proceed on the assumption that the alloy modifications which will show the best ductility after irradiation may still experience appreciable swelling. The accommodation of swelling of fuel pins and nearby structure of the fuel bundle thus remains a requirement for prudent design.

At present, it appears practical to design for maximum diametral expansion of fuel pins of the order of 3% (linear) for a target burnup of 100,000 MWD/T. The fuel bundle must accommodate about a 3% change in linear dimensions of the fuel pin (the sum of fuel- and clad-swelling effects in the region of maximum expansion), together with about 1.5% linear expansion of nearby structural elements such as spacers, spacer wires, and flow channels.

If we assume that the outside dimensions of the pin assembly originally was 40 mils smaller than the inside dimensions of a 3-in. channel, the differential expansion just described would just reduce the clearance from 40 mils to zero. In addition to allowing for

expansion by selection of the original dimensional tolerances, part of the differential expansion between a fuel-pin array and the fuel channel can be accommodated by small bending strains in the fuel pins. The bending moment, in turn, will be accommodated by creep, at least over part of the temperature range at which the fuel pins operate. This is an area requiring very thoughtful design, even though it appears that swelling can be accommodated by a combination of the tolerances selected and the allowance for small bending deflections. There is now some evidence in tests without irradiation that localized line or point loads on tubular cladding can reduce the rupture lifetime of the cladding under a constant hoop stress.⁽²⁹⁾ The avoidance of significant line and point loads by careful design and control of tolerances accordingly may be required and may pay substantial dividends in improving fuel lifetimes.

The economic incentive for long fuel lifetime is at its highest in the early stages of fast reactor development when fuel fabrication costs are high because of small-volume manufacture and low experience factor. However, even with lower fabrication costs in a maturing fast reactor fuel industry, there will continue to be some economic incentives for obtaining average burnups as high as 150,000 MWD/T. The avenues of approach which we believe may eventually make such burnups obtainable will include some or all of the following:

- (1) establish the relationship between alloy composition (structure and operating temperature) and the extent of void formation at total fluxes of 2×10^{23} nvt;
- (2) establish the effects of alloy composition and structure on ductility as a function of temperature at total fluxes of 2×10^{23} nvt;
- (3) establish permissible limits of elastic and creep strains of fuel pins which do not result in significant penalties in fuel lifetime;
- (4) develop core structure and clamping designs which accommodate elastic and creep deformations of the channel, as well as of the fuel-pin array with minimal local loadings in the cladding;
- (5) develop a higher level of sophistication in the measurement and correlation of local flow distributions within a channel so as to minimize local flow and temperature nonuniformities.

Extension to lifetimes substantially higher than 100,000 MWD/T will probably require sophisticated control of alloy compositions and metallurgical structures, and probably somewhat tighter tolerances in design and in fabrication. There is also the possibility of finding materials showing substantially lower swell-

ing than stainless steel. At present, with only limited irradiation experience, vanadium looks promising in respect to swelling but appears to have a loss of ductility similar to that for stainless steel. Vanadium is also somewhat more sensitive to oxygen levels in the sodium than is stainless steel, requiring some advances in the state of the art of sodium purification and control, and in maintenance procedures for sodium systems. Because of the substantial added cost and difficulty of fabrication and operation with vanadium, there remains a strong incentive to develop specially tailored stainless steel alloys which permit extended fuel lifetimes. Vanadium alloys would have to show substantial advantages in attainable fuel lifetimes, of the order of 10 to 20%, over fuels with stainless steel cladding in order to break even economically. Nevertheless, vanadium appears to warrant continuing modest effort as a possible backup route to high burnups.

3.3. Vented Fuel and Fission Gas Emissions

Extensive analysis and limited experimentations continue to point to the potential value of vented fuel in extending the potential burnup lifetime of fast reactor fuel. A series of thermal irradiations up to 47,000 MWD/T showed normal behavior and no measurable diametral expansion of fuel to this relatively low burnup.⁽³⁰⁾ A plausible analysis of vented-fuel behavior, taking credit for a reduction in hoop stress due to the reduction in fission-gas pressure, indicates the potential for extensions in fuel lifetime of 10 to 25%.

With vent designs that provide holdup times of the order of 1,000 hr, only krypton-85 is emitted in significant quantities. Thus there is negligible contamination of the primary system by rare gas daughter products and no interference with fuel-failure-detection systems based on either xenon detection or on delayed-neutron emitters.

With gas-holdup times of less than 100 hr (probably typical of defected fuel after the initial burst), xenon-133 and krypton-85 are the main activities, but appreciable amounts of other radioisotopes are also released to the reactor coolant and cover gas. Accommodation of even one defect per 10,000 pins requires provision of low-leakage barriers between the reactor cover gas and both refueling-cell atmosphere (less than 0.5 cfm) and the primary component cell atmosphere (less than 0.01 cfm). Continuous cleanup of cover gas may also be required to accommodate defected fuel.

Since there is a considerable economic incentive to obtain the longest possible fuel lifetime, this requires operation until an appreciable fraction of the fuel has failed. This results in the surprising conclusion that

systems with vented fuel can operate to a given fuel lifetime with *lower* ambient levels of fission-gas radioactive emissions, and less contamination of the primary system, than "non-vented" fuel at comparable lifetimes.

A few *de facto* vented fuel elements have been tested (in addition to the planned tests) as a consequence of occasional pinhole leaks in the fabricated fuel elements (typically associated with end plugs or welds). In the instances in which the pinhole is fine enough that fission gas is emitted but entry of sodium into the fuel element is prevented, the effect of a vented fuel element is obtained. In one instance, such an adventitiously vented element operated to 34,000 MWD/T in EBR-II at 16 kW/ft. Normal behavior ($\sim 0.3\% \Delta D/D$) was observed in respect to diametral expansion with no entry of sodium. The Rapsodie reactor has indicated several instances of brief fission-gas emission peaks followed by resumption of low ambient levels of fission gases in the cover. Presumably, the fission-gas spikes are due to the sudden emission of the fission-gas inventory accumulated and carrying relatively high specific activity due to the sweeping out of relatively short-lived fission gases by the bulk of the gas inventory in the fuel pin and plenum. After the initial emission, and for leaks which remain small as with an intentionally vented element, an effective delay time of the order of 100 hr or more is reached. This serves largely to eliminate the emission of shortlived fission gases, corresponding to the low steady-state rates observed after the spikes of emission.

Near the end of life, practical designs of nonvented fuel pins will accumulate fission gases at pressures of the order of 800 to 1500 psi. The sudden rupture of such pins may release gas in such a way as temporarily to gas-blanket an adjacent flow subchannel. Prior calculations with simple models showed that clad melting would not result (with sodium in up-flow at >15 fps). More detailed, recent two-dimensional calculations show that in tight lattices, circumferential temperature differences of 400°F or 300°F may occur in a fuel pin during the gas emission, depending on whether zero or $\frac{1}{3}$ flow obtains in the blockage area.⁽³¹⁾ The resulting stresses due to thermal bowing could be accommodated by cladding after moderate irradiations, but will no doubt result in increased probability of rupture with highly irradiated cladding. The possibility of such failure propagation from pin to pin remains a significant penalty to reactor safety and fuel economics. This provides a continued incentive for determining the phenomena associated with fuel failures and for development of suitably vented fuel to

reduce the stored energy of the fission-gas inventory at high pressures.

The effect of vented fuel in reducing the stored energy available to drive gas into the coolant stream in a power transient or flow coastdown appears to be significant, but has not yet been subject to systematic testing. Gross failures of encapsulated fuel pins (non-vented) have occurred which are correlated with local gas blanketing of the pin capsule annulus and resulting in local clad melting. Systematic testing of pin-failure effects is scheduled, starting in 1969, using small integral loops. As burnups of fuel testing increase to produce appreciable fission gas pressures and inventories, data from both planned and inadvertent testing of such effects will become available.

3.4. Steam Generators

The basic feasibility of steam generators with both single-wall and double-wall tube designs has been well established through the operation of model tests and small actual units. In recent years, significant operating times have been obtained with a double-walled unit in connection with the operation of EBR-II, and relatively brief operation with single-walled units in connection with the operation of the Fermi reactor. Although the Fermi units have been subject to a high level of repair and retubing operations, even for prototype equipment, none of the phenomena leading to problems which have occurred appear novel nor, in retrospect at least, very difficult to solve in design.

Although all of the problems individually appear readily manageable, it is clear that a somewhat higher order of perception and integration between design, analysis, fabrication, startup, operation, and maintenance is required than is usually applied to conventional boiler equipment. This is especially the case if several different sets of people are involved in the many steps from design to operation.

Some of the early steam-generator designs bore considerable similarity to fossil-fired units in respect to tube and tubesheet layouts. It is significant to note that of the three large fast reactor power plants which are under construction (PFR, BN-350, and BN-600), the evaporator units use bayonet tubes, with tubesheets separated from the sodium surface by a gas space. Such designs are responsive to design criteria which give much greater weight to reliability, inspectability, and maintainability features than are needed with fossil-fired units. For example, a bayonet-tube design can be built with only one weld submerged in sodium and that weld subject to low stresses typical of an end cap. The operation of the tubesheet in cover gas rather than in direct contact with sodium

limits the thermal stress and thermal shock to which it may be subjected. These designs also appear to provide exceptionally good access for inspection, both in fabrication and in maintenance.

The economic evaluation of cycles with high top temperatures has been made more quantitative by cost analyses done in connection with the 1000-MWe design studies. These show that in the neighborhood of 1000°F there is a relatively small net cost incentive for a 100°F increase in top temperature. Such incentives can easily be reversed (that is, the optimum temperatures reduced) if prudent factors for decrease in component lifetime with increase in temperature are used in the analysis. With less near-term emphasis on high temperatures, designs now tend toward the use of the low alloy steel for the evaporator (or the low-temperature section of a once-through system) and austenitic steel only for the superheater regions, if at all.

The problem of carbon losses and consequent decrease in creep strength of ferritic materials in sodium is clearly design-limiting at present. The use of carbon-stabilized materials, for example, as planned for PFR, is still subject to some uncertainties in design lifetime. In the United States questions arise because of the limited amount of relevant data on the long-term mechanical properties for ferritic materials in flowing sodium. This problem is discussed more extensively in another paper at THIS CONFERENCE.⁽³²⁾

The problem of sodium-water or steam-sodium reactions in the steam generator has several dimensions. One is the occurrence of very small leaks, since even a small leak proceeding over a considerable period of time can produce consequential damage, possibly in regions remote from the original leak. As the size of a leak increases, a point is reached which may be viewed as a "flame" of steam or water burning by reaction with surrounding sodium. As determined by several test programs, if the flame impinges on an adjacent tube, rapid wastage of tube material can occur, with the potential for fairly rapid, and thus catastrophic, failure of a tube. The stored chemical energy of sodium reacting with water then leads to a potential for energy release which can severely damage the entire heat exchanger. A necessary requirement, accordingly, is the reliable detection of leaks to permit shutdown of the unit before the stage of massive damage is reached. Blowdown systems, which can accommodate potential massive reactions corresponding to the rupture of one or several tubes, are also required. Current designs are obviously proceeding on the basis of the assumption that, with good quality control, leaks can be made very infrequent and will usually start out in

life as very fine pinhole leaks. Provided that the occurrence and location of the leak can be determined before the flame-like stage is reached, isolation and repair of the unit can prevent the occurrence of major damage to the component.

Accordingly, there will be considerable value to simple, but reliable, detection systems for steam-generator leaks, especially at the small-leak stage. The ability to detect very small quantities of hydrogen by various means is well-known. However, the problem of design of sampling systems, and decisions on whether or not to locate leaks by multipoint-sampling during operation, can be resolved only by analysis of specific designs and modes of operation. For modular designs with large numbers of shells, it may be sufficient to detect that a given shell has a leaky tube producing hydrogen. For larger units, which are typical to attain the economics of, say, a three-loop design, it may be advantageous to localize the region of the leak somewhat during the operation of the unit, since location of leaks in the shutdown condition is subject to considerably lower sensitivity of detection.

Test experience with model units has, in recent years, been almost exclusively overseas, with significant test units operating or near operation in Russia, France, Germany, and the UK. However, significant design and testing programs associated with forthcoming US demonstration plants are underway, including that being conducted jointly by Foster-Wheeler and General Electric on behalf of the ESADA utility group.

3.5. Carbon Transfer in Sodium Systems

The role of oxygen in determining the rate of corrosion in materials in sodium has been reasonably well understood and substantiated in several independent programs. Further developments in this direction will include the improvement in precision and accuracy of oxygen measurement and control to reduce the scatter in corrosion data, especially as one approaches limiting acceptable corrosion rates between 1200 and 1300°F for many materials. The behavior of oxygen in sodium systems can be largely described in terms of sodium oxide and sodium hydroxide (which is metastable under many conditions in sodium loops).

The analogous descriptions of the carbon species that participate in carbon transport and in carburization reactions are not yet established. The effects due to carbon are still only qualitatively known. Defining quantitative effects due to carbon transport constitutes the major avenue for further improvement in the control and measurement of operating conditions of the sodium-coolant systems.

The observation in recent years that carbon transfers from the hot to the cold leg of a mono-metallic austenitic stainless steel system indicates the importance of carbon transport in modifying the expected properties of materials of the reactor core and of the intermediate heat exchanger. In the secondary system, the transfer of carbon from ferritic steels at low temperature to austenitic steels at higher temperatures is important in the behavior of the steam generator and also of the intermediate heat exchanger.

The rates of carbon transfer are controllable by (1) limiting temperatures, (2) by the use of "stabilized" alloys that contain elements which form very stable carbides, or (3) by using thicker sections and properties values for design which take into account the expected changes due to changing levels of carbon content. Thus the effect of carbon transport on design at present is to require somewhat greater material margins for changes in properties, and thus more cumbersome and possibly less reliable designs.

The presence of a wide variety of carbon compounds, as well as elemental carbon, has been demonstrated in various sodium systems. At least five classes of carbon-containing compounds have been identified or strongly inferred for sodium systems, including sodium cyanide and sodium carbonate, metal carbides, including sodium carbide and/or acetylide, and elemental carbon.⁽³³⁾

The detection and identification of the species involved in carbon metabolism in sodium loops has been especially difficult because of the lack of direct means to observe solution equilibria analogous to those available for water-solution chemistry. Consequently, the main chemical processes involved must be largely inferred from trace amounts of solid compound residues from distillation or extraction processes. The examination of cold traps in sodium systems has been particularly fruitful, since the cold traps serve to concentrate carbon-containing species that otherwise are present only at part-per-million levels and less. Striking differences in the amount and pattern of compounds found in cold traps of an all-austenitic stainless steel loop, as compared with ferritic steel loop, has been interpreted as being due to the effect of chromium transport on carbon transport.⁽³⁴⁾ This hypothesis is given further support by the pattern of depletion and deposition of chromium in surface layers in austenitic sodium loops. The deposition of chromium appears to occur to a substantial extent as carbide and, accordingly, relatively less carbide carbon reaches the cold trap in an austenitic loop than in a ferritic loop that has lower chromium content. Chemical analyses, surface analyses, and measurement of changes in hardness of foils

continue to be the main tools for observing carbon in sodium systems.

The development of in-stream carbon sensors, which depend on the diffusion of carbon in iron (to produce an electrochemical potential or a measurable gas concentration by conversion to CO or methane), affords the possibility of observing some of the carbon activity directly. Although the reproducibility and interpretability of such measurements are as yet rather uncertain, this nevertheless provides one of the important and potentially fruitful avenues for further development aimed at control or inhibition of carbon transport.⁽³⁵⁾

4. Conclusions

1. Relative success of the two main plant-design alternatives presently being pursued appears to depend as much on execution of specific designs as on basic concepts, since no questions of feasibility have been identified.

2. It is necessary, and appears practical, for all designs to incorporate provisions for rapid access for diagnosis and repair of malfunctions.

3. Limited recent cost experience relative to sodium plants provides encouragement that design tradeoffs based on economic analysis are becoming meaningful.

4. Substantial progress is noted in the statistical reliability of corrosion data in sodium with continued preference for Type 316 stainless steel for fuel cladding. Properties effects in sodium environments which may limit top temperatures to less than 1300°F are largely attributable to carbon transport.

5. The reduction in density of cladding alloys on exposures to fast fluxes affects fuel design and suggests that target irradiation levels of 50,000 MWD/T appear prudent for first cores of demonstration plants. Several promising alloy treatments are available that should permit higher irradiations for later cores.

6. Vented fuel continues to show potential economic and safety advantages and good performance in limited tests to date. Problems of radioactivity transport appear manageable and may be easier than those for nonvented fuel at equal lifetimes.

7. Carbon transport in sodium can cause major effects on materials properties in both primary and secondary systems. Quantitative measurement and control of such effects, including the establishment of stabilized alloy for practical use, represents a major area for improvement of technology of sodium systems.

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Discussion

Mr. Zitek (ComEd): The G.E. speakers indicated they had good results in estimating the price for the SEFOR construction project. How about the cost estimate for fuel fabrication?

Mr. Wolfe: We had good experience on the cost of the fuel that we paid for as compared to the initial cost estimates. We went to two vendors: NUMEC and Nuclear Fuel Services. We also had independent estimates made by our Vallecitos Laboratory. I think it was remarkable that all estimates came within a few percent of each other. Basically the big

differences were in estimates of loss and inventory, primarily loss. We let the contract at a fixed price, and so our cost experience was good. Nuclear Fuel Services, who made the fuel for us, did encounter some problems that I think they didn't expect, and I think that their cost experience was not as good.

Mr. Kuesters (Karlsruhe): You said that the value of the reactivity change due to sodium loss is no longer to be regarded as a deciding number because the probability of various severe accidents are hopefully small when sodium voiding and the associated ramp rates are concerned. It is necessary to prevent accidents due to blockage and consequent sodium ejection, and eventually propagation. Now the designers are to rely to a large extent on instrumentation. This means that you should have reliable detection for nearly each sub-assembly. My questions are: are you confident that you will have reliable instrumentation for detecting incidents in subassemblies; have you developed criteria for what action should be taken if certain indications showed deviations from normal behavior?

Mr. Wolfe: I think Dr. Kuesters' general explanation of the situation puts it in perspective. There are several mechanisms present for the hypothetical accident problem in the fast reactor, namely, the meltdown and the explosive interaction of molten fuel in sodium, and superheat. Either separately or in conjunction these provide one with mechanisms to construct rather elaborate or dramatic hypothetical accidents. Furthermore, I think that since 1965 there has been very little progress in these areas in terms of putting them in the context of a realistic fast reactor environment. One has already had in 1965 many ways of estimating upper limits of accidents in some sense, but they're such high upper limits that they put a severe penalty on any reactor design. It's not plain whether in a real big reactor one can even live with them as we have in the small reactors. I think the hypothetical accident already is of such a low probability, especially in a sodium reactor in which you have advantages of low-pressure operation: many hundreds of degrees from boiling or burnout, factors of three in power before one gets to the boiling region, and the ability to, say, put a safety vessel around the reactor vessel, one can design to be insensitive to breaks. Also, the sodium reactor doesn't have blow-down problems, loss of heat transfer, blower stop, and so on. The reactor can be made very safe under normal operation, or at least very far removed from potential catastrophic accidents. These then become accidents of very low probability, of such low probability that I think intuition and judgment is no longer a valid way to look at it. It doesn't make any sense to sit in a room and have twelve people think of things

that might happen with probability weights of 10^{-6} to 10^{-7} . It seems to me that the only way to approach this problem is to try and get some quantitative feel of the situation by coupling our knowledge of what will happen to some probability of its likelihood of happening, in somewhat the way that the British have pioneered in their studies. Now, in order to get these probabilities low, I think that Dr. Kuesters is quite correct. One has to provide good instrumentation; the effort, at least at General Electric, as you will see from Mr. Gibson's paper later in THIS CONFERENCE, is to provide instrumentation for each channel. The type of instrumentation as a minimum would be redundant temperature thermocouples on each outlet, chain set so that if you got an indication of two out of three, you would automatically scram; you wouldn't just ignore these things. We hope that we can develop incipient-boiling detectors; this is a little bit harder to do, but it does not seem to be completely impossible, and it may even be possible to detect local blockages by means of thermocouple noise-type arrangements. These represent a challenge to the industry, but basically the approach that we're suggesting is that one does instrument heavily and then tries to assess quantitatively the probability of these more dramatic accidents, not just to assume them without any basis.

Voice: What advantage does one lose when the temperature is lowered from 1400°F maximum temperature in the core to something below 1300?

Mr. Zebroski: I regret that I don't really understand the question. I simply suggested that there is noticeable deterioration in materials, and nobody's measured properties of the materials at 1300°F in sodium for 10,000 to 20,000 hr. The data we see after 5,000 hr already suggest, from the structural changes that are going on, that there will be loss of creep strength and reduction of ductility, even without radiation. That's simply an indication to the designer that he has to allow, if he wants to go to 1300°F, bigger margins for the numbers that he uses for design.

Voice: I mean if you're going from 1400 or 1300°F to some lower temperature, what advantages do you expect to realize?

Mr. Zebroski: You just get a trend in the properties of the design curves that you can use—the usable creep strength. In some cases, you can make up for higher temperature by a little thicker material; in other cases, you can't.

Voice: Why the higher temperature if there are no substantial advantages?

Mr. Zebroski: I agree. The cost incentive for a 100° rise in temperature is measured rather quantitatively by each designer, and is in fact rather small if you make any serious account of the degradation of

properties and possibly component lifetime as you go up in temperature. I agree with the implication of your question that 1200°F is not an unreasonable place to go for now.

Mr. Ash (AI): I think that Dr. Zebroski mentioned that there was an economic advantage in terms of increased burnup with vented fuel. I was wondering under what conditions that occurs and is there experimental evidence to indicate that?

Mr. Zebroski: The calculation unfortunately applies for the conditions between 75,000 and 100,000 MWD/T at which the cladding is reaching the end of its available strain lifetime with the degraded properties under radiation. Only by calculation of models of how the fuel stresses the cladding and how the gas pressure stresses the cladding can you make any progress. There are no integral tests on this point as yet. There are some tests, however, and you can deduce a little bit of these trends by the fact that there have been fuel elements with rather different sizes of plenums built and which have operated under slightly different conditions, so that the amount of gas release is somewhat different, and the gas-pressure component of the total stress picture is different. With a little imagination (unfortunately we still have burnups of only 60,000 to 70,000 MWD/T) you can begin to deduce that there is an effect of the gas pressure, but I will admit that this is primarily calculation. It's a question of how the stresses add, and as you know, stress analysts can argue these things endlessly. We do need the experiment.

Mr. Ash: Is it a function of the particular plenum length that you have in the nonvented design? If you make it long enough, you can probably get the same burnup.

Mr. Zebroski: I think this is considered in some of the 1000-MWe studies. To get the gas pressure down to a point at which it would be a trivial part of the total stress picture, you lose overall economically, because the pumping power of the core has gone up substantially. I don't believe you'd go to such large plenums.

Mr. Nozawa (JAERI): With regard to radiation damage of the cladding material, you mentioned alpha damage as the main cause, and also vacancies. How about the proton contribution to the damage? How do you explain the rapid increase in diameter at a certain burnup?

Mr. Zebroski: If you were deducing from the curve that I showed for fuel swelling, this is not, as far as we can tell, the swelling due to the helium and vacancy agglomeration: that phenomenon does not have a sharp change in rate with burnup, but it's easily measurable only when you get to radiations above 10^{22} . The effect that that slide showed was that the

combined effect of fuel swelling and clad swelling had a knee in the curve at about 50,000 or 60,000. We think at that point the available void in the fuel for a particular design has been consumed and you're beginning to have the clad expansion due to fuel-swelling strain.

Mr. Golan (AI): Dr. Zebroski, you mentioned that based on your observations today, you would recommend 3% maximum $\Delta D/D$ swelling of fuel clad and 1½% of structure. Is this linear or volumetric?

Mr. Zebroski: Linear.

Status of Sodium Technology in France

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(PRESENTED BY J. LEDUC, CEN-Saclay)

Work on sodium technology began in France at the CEA (Commissariat à l'Energie Atomique) in 1953. At that time, the first "pioneers" in the liquid-metals field knew nearly nothing about sodium. At the best, we could speak by memory of its chemical and physical properties, and we remembered some experiment at school: a piece of sodium sliding on the water with small flames and white smoke. The main actual use of sodium was in the manufacture of lead tetraethyl, but its potential use for reactor cooling seemed very attractive. By the way, it must be recalled that the work on this field in USA and the construction of EBR-I had paved the way.

Nevertheless, there was a strong feeling that sodium is a very exotic and very dangerous material, and the first experiment we made seemed to confirm such a feeling. This experiment was only intended to see with our own eyes some melting and filtering of sodium; following a laboratory device described in some old paper, we put a nut-sized sodium piece in a small glass tube with some stainless steel mesh inside. The tube was then sealed and heated; the sodium melted and filtered. We got effectively a very shiny ball of sodium at the bottom of the tube. Unfortunately, a few seconds later and for unclear reasons, the tube blew up and injured the poor people who made this first trial.

Fifteen years went by. Now our experimental fast reactor Rapsodie has worked for more than one year, and we are beginning the construction of our fast prototype reactor Phenix. This situation is probably sufficiently clear to show that since 1953 we have come a long way in the sodium reactor technology.

The use of sodium for fast reactors shows well-known advantages. Briefly, the most important ones are:

- excellent heat-transfer coefficients of sodium (about 10 times more than gas or steam) are very suitable for high specific power, and hence low plutonium investment;
- reasonable fissile inventory (doubling time can be achieved in perhaps less than 10 years);
- engineering safety of the reactor in normal or emergency conditions is best provided by sodium, due

to high cooling efficiency, large thermal inertia, and easy natural convection;

wide technological background is now available in France and in many other countries to support sodium fast breeder development.

It seems agreed that the two main problems for fast reactors development involve fuel and steam generators. However, it should be noted that we have to face more uncertainties than difficulties; not enough results or experience are now available, and more time is needed to get a realistic feeling and knowledge in these fields.

Fuel technology is not in the scope of this paper. Therefore, it will just be said that the $\text{UO}_2\text{-PuO}_2$ mixed-oxide fuel elements of Rapsodie, designed for 30,000 MWd/t, have now reached more than 40,000 MWd/t, and that this fuel does not appear to be at the end of its life.

Steam generators will be considered later on in more detail.

Coming back to sodium technology, a brief history of our work may be recalled. From 1953 to 1958, about 20 to 40 people at the best were engaged in liquid-metal studies. But this was an important period in our development work: without any reactor construction in mind, and hence without any tight schedule, a small staff at Fontenay-aux-Roses could quietly get acquainted with sodium handling, piping, pumping, etc. Components for small experimental loops were designed and tested: electromagnetic and mechanical pumps, flowmeters, level probes, sodium-sodium heat exchangers, etc. This phase of research culminated in a facility of 600 kW at Fontenay, first operated in May 1958. The heat source was an oil-fired heater on a primary NaK loop. Heat was transferred through an intermediate heat exchanger (19 tubes in a U-shell) to a secondary NaK loop with a steam generator. The boiler was double walled, with static mercury as intermediate fluid; the superheater was single walled. Superheated steam was produced at 400°C and 30 bars.

In June 1957, first ideas on Rapsodie began to take shape, and we faced for the first time an important realization. Many fields of sodium technology had previously been covered, the results were promising, and our experience valuable. So we could envisage confidently the Rapsodie design and construction.

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However, beside the 600-kW facility mentioned above, our experience had been gained from operation of very small test rigs. In order to avoid large extrapolation from our knowledge, two cooling systems were considered, intended to work in parallel on the reactor: one of 1-MW capacity, the other of 10-MW capacity. In case of difficulties in the studies during the following years, the two loops could be of 1-MW capacity; in case of pretty good success, the two loops would be of 10-MW capacity (this was finally the case).

From the beginning of 1958, all sodium-technology studies were focussed on the Rapsodie target. Due to the unique problems to be solved for the first time in our country, it was decided to assess the design studies very cautiously; insofar as possible, all components of the reactor and cooling systems were tested on full-scale models. The larger and more expensive mockups were probably the reactor vessel with internal parts, and the 1-MW and 10-MW cooling systems. In spite of so large time- and money-consuming testing experiments, the Rapsodie reactor itself and all components and related loops were carefully commissioned as a sodium installation before any neutrons were released.

Past history cannot be rewritten, and nobody knows what would have happened with Rapsodie if a different strategy had been chosen. The only thing we can speak about is the actual result:

Nominal power was reached on March 17, 1967, less than two months after first criticality (January 28).

From the beginning of normal power operation (at the end of August 1967) the reactor operated very smoothly. From August 1967 to the end of September 1968 (annual schedule maintenance shutdown), the reactor was in operating condition 91.3% of total time, and load factor (at nominal power of 20 MW, and then 24 MW) was 69%.

The future cannot be predicted, and some unexpected trouble cannot be ruled out. In any case, the very successful past operation of Rapsodie is strong evidence of the possible reliability of sodium-cooled reactors.

Furthermore, Rapsodie is not a pushbutton reactor. After more than one year of power operation, a number of incidents occurred. Some were of no great importance; others were more troublesome. It must be said that these incidents were very fruitful; we learned on the spot and in detail what works and what does not. However, the important actual result is that all incidents were overcome in a few hours or a few days (only in one case were two weeks spent).

More details about Rapsodie operation are given

in another paper at this meeting.⁽¹⁾ As a sodium fast reactor, the main incidents can be summarized as follows:

rotating plugs. Movement of the rotating shield plugs was disturbed by sodium and sodium oxide deposits, and became at one time quite impossible. Adequate operation sequence was then elaborated to provide relative heating of plug gaps after the reactor was shut down. No more trouble happened.

argon system. It was also a matter of sodium deposits, which turned out to some plugging of argon pipes, especially in definite parts (valves, T junctions, etc.). Fortunately, Rapsodie design had taken that trouble into account, and the argon system can be washed out by raising the sodium level. This technique worked successfully. Nevertheless, some improvements were made to get an easier washing, and a more efficient argon-purification system was provided, to reduce air contamination in argon cycle.

control-rod mechanisms. Fingers of one gripper (gripping device at the bottom of mechanism) were distorted and needed a spare replacement.

refueling machine. An incident in refueling operation came from distortion in the holding tube (the tube resting on the subassemblies around the unloaded one), and induced distortion of the top head in some subassemblies. This was promptly solved after manual unloading.

sodium-level measurement. No proper results were obtained with resistance or inductance detectors after long-time operation. On the other hand, level probes giving just definite level position worked without difficulty and were found to give sufficient indications for reliable reactor operation.

No trouble occurred with the sodium pumps, after some increase of clearance during commissioning of the reactor. Some improvements were made on the rotating mechanical gas seal, which then worked successfully.

It must be mentioned that during October 1968 one primary pump was removed for scheduled inspection. The pump was found in the same condition as at the previous examination before reactor power operation (in the meanwhile, the pump worked 11,096 hr).

The intermediate heat exchanger operated quite satisfactorily.

Cold traps operated without difficulty. Oxide deposits tended to accumulate in the colder part of the trap. In normal operation the sodium flow was very clean. Consequently, cold traps in secondary loops

will be out of operation when not necessary, that is, when plugging temperature is sufficiently low. Cold traps in primary loops will be kept in operation for continuous trapping of the unknown impurity related to the second plugging temperature (this second plugging appears only above 180°C).

It is to be noticed that, after reactor commissioning and before power operation, the two cold traps for the primary sodium and the cold trap in one secondary loop had been removed. After more than one year of reactor operation, the new cold traps always worked efficiently.

Automatic plugging meters gave very good results. Due to high purity of sodium (cold trap at 102 or 103°C), plugging-meter indication is seldom of interest. Plugging meters are now only used as alarm detectors, giving a signal if plugging temperature comes above 130°C.

It must be recalled that there are no *valves* in primary and secondary main cooling systems. A number of small-diameter valves are only used in auxiliary sodium loops. On the primary side, valves are bellows sealed; on the secondary one, there are both frozen and bellows-sealed valves. Both types worked satisfactorily, but bellows-sealed valves need careful operation, especially in case of freezing of sodium.

As for *activity*, the only remark is that, in October 1968, after more than one year of operation, some activity was measured on pipes and components (from 10 to 200 mrem/hr). This activity was recognized as due to a mass transfer from stainless steel elements (Mn-54; Co-58). After careful brushing of the surface, activity was strongly reduced. In any case there is no health hazard and no difficulty for any maintenance work on reactor components.

Regarding the studies for the prototype fast breeder reactor Phenix, our strategy is, of course, rather different than for Rapsodie. Firstly, it is obvious that no full-scale models could be considered for many parts of the reactor system, due to size and characteristics. Secondly, our knowledge and experience is much larger now than in 1958, and we could reasonably enter the construction with less experimental tests. However, experimental checking is carried out when possible in case of specific or unique component design.

The broad lines of the main experiments are listed below:

Sodium flow and heat transfer in the fuel subassemblies: By hydraulic tests, the pressure-drop equation was experimentally established and the influence of fuel-subassembly characteristics was determined. Inlet flow at the bottom of subassembly was care-

fully tested. Reliability studies are under way at nominal sodium flow, in the temperature range from 550 to 600°C. Actual thermal checks will be carried out.

The hydraulics of the two reactor vessels was investigated in detail by means of water models.

Fuel-handling systems in Phenix and in Rapsodie are very different.⁽²⁾ Therefore the transfer arm inside the reactor and refueling system (from inner to outer storage, and vice versa) will be full-scale tested under operating conditions. Regarding more directly the sodium environment, preliminary tests have been conducted on specific parts of these systems, such as sliding or rolling guides, lifting steel tape (used for loading and unloading), gripper device (to catch subassemblies upper end), and large-diameter flat valves (to isolate reactor cover gas from the top loading-unloading chamber). In all cases, very satisfactory systems have been developed for the actual environment.

Control-rod mechanisms: Before building and testing two prototypes of different design, some elements were experimentally developed from mid-1967: rack and pinion driver in argon with sodium vapor, long bellows in sodium, electromagnetic gripper in sodium, etc.

Pumps: Very satisfactory operation of the Rapsodie sodium pumps (from 12,398 to 16,045 hr at the end of September 1968) gives reasonable confidence in those for Phenix (4,000 m³/hr). No sodium testing is planned before reactor commissioning. Only water runs will be made to check the characteristics of the pumps and to confirm lack of vibrations. Although a development program is pursued on hydrostatic bearings, satisfactory solutions are now at hand.

Intermediate heat exchangers: Since the intermediate heat exchanger is very similar in Phenix and Rapsodie, pressure-drop and heat-transfer law are already known. Only hydraulic tests will be conducted to investigate flow pattern and provide an adequate tube-clamping system against induced vibrations.

Valves: A development program is under way to obtain valves with diameters up to 350 mm. Valves of larger size are contemplated. A frozen-seal type was highly developed for large diameters and is much in favor. A special gate valve designed to close the inlet openings for primary sodium in the intermediate heat exchangers of Phenix will be mechanically tested.

Instrumentation: New development on an inductance-type level-measurement device is under way. In addition, a specific flowmeter arrangement was designed to measure large flow at the discharge of each primary pump inside the sodium of the reactor main tank; an electromagnetic flowmeter is put on a small bypass pipe connected as a Pitot tube on the

main discharge pipe. Furthermore, ultrasonic scanners are now under study for different possible uses, such as sodium-level measurement and sweep of the tops of subassemblies. Also to be noted is a hydrogen-measurement system now being tested on our steam-generator facility.

Miscellaneous: Among various problems that have to be solved for reliable operation of sodium-cooled reactors, a unique one comes from sodium vapor and sodium aerosols in the argon cover gas. These can yield large deposits on the upper colder walls of the reactor system. Troubles of such kind were reported for Rapsodie. An experimental program is under consideration to get more knowledge on aerosols evolution, deposition, and separation.

Steam generators: This topic needs larger consideration than the preceding ones for two reasons. On the one hand, it is commonly agreed that this is an important problem for which experience is still limited. On the other hand, no steam generator being installed on Rapsodie, this is specifically the new field in our next prototype. As a matter of fact, Phenix will gather technological knowledge gained on Rapsodie and steam-generator experience obtained by another way. Indeed, our Grand Quevilly 5-MW facility, specially intended for steam-generator experiments, was first placed in operation in June 1964. The first steam-generator prototype accumulated about 8000 hr of operation without the least trouble. By the way, it was recognized that Type 321 stainless steel could be a very good material for the boiler as well as for the superheater, if water is carefully treated.

A new steam generator was put in operation in December 1967. It is a prototype of the modular system designed for Phenix (ferritic steel, 2.25 Cr-1 Mo-1 Nb in the boiler; Type 321 stainless steel in the superheater and reheater). Steam has been generated at 540°C and 167 bars for 3500 hr without any trouble. Experimental results are in good agreement with design, and stability is excellent from 20 to 100% of nominal power.

A larger facility of 50-MW capacity is now under construction at Les Renardières (EDF). Three moduli of the Phenix steam-generator system are expected to be tested at that facility in 1970.

Furthermore, a large experimental program is under way at Cadarache concerning the effect of a sodium-water reaction in a steam generator. Many experiments were conducted with various geometrical configurations, and a method was developed to predict temperature and pressure patterns during sodium-

water reaction. In July 1968, one tube was intentionally broken in a full-scale model of a Phenix modular boiler. The experiment was quite successful, and bursting discs worked normally. Maximum pressure was only half of that predicted. Very small effects were noticed on the shell and on the other tubes of the bundle.

Obviously, sodium technology is no longer only a laboratory technology, nor a small rigs technology, but really a reactor technology.

Before building a reactor, a number of experiments and tests are necessary, and such tests are or will be conducted for Phenix. Others will be defined for larger future reactors, and special attention will be paid to the development of sodium steam generators.

The success of Rapsodie as a sodium reactor points out that the state of the art in sodium technology has already reached a stage that permits safe and reliable operation of sodium-cooled reactors.

Moreover, with sufficient basic knowledge of sodium technology, designing, building, commissioning, and operating a reactor is the only way to get better understanding of problems to be solved, and to find out for them the most proper and most appropriate solutions.

Much progress remains to be done and will surely be done, as happens normally for a new technology. We can anticipate that, in a few years, sodium will be considered as a usual fluid, and its technology as different but not more difficult than gas or water technology.

REFERENCES

1. Gajac, G., et al., *Rapsodie's First Year of Operation*, THIS CONFERENCE.
2. Carle, R., et al., *Phenix: Status of the Design before Construction*, THIS CONFERENCE.

Discussion

Mr. Charak (ANL): I'd like to ask if caustic corrosion of the intermediate sodium loop is a problem in the event of a sodium-water reaction in the Phenix steam generator, a small leak perhaps that goes undetected for some length of time?

Mr. Leduc: In case of a sodium-water reaction in the steam generator, we closed two valves to isolate the intermediate heat exchanger from the reaction products. But now we are not sure that we have time to close this type of valve sufficiently rapidly, and we are making calculations by using the results of experiments to see if we get some reaction products in the intermediate heat exchangers.

Operating Experience with EBR-II

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SUMMARY

Operation of the Experimental Breeder Reactor II during the past year has successfully demonstrated the use of the reactor as an invaluable facility for fast-flux irradiations. Approximately 130,000 MWh of reactor operations were applied to the irradiation of experimental fast reactor fuels and materials. Considerable information on diagnostic techniques was developed as the result of fission product releases. Mechanical performance of the heat-removal systems has been good. Extended and repeated usage of the fuel-handling mechanism has demonstrated that the basic concepts are acceptable and can be adapted to sodium-cooled systems.

The reactor has been used to develop a better understanding of reactor kinetics measurements using rod-drop techniques, and fission-rate distributions using variations in individual worths of control rods. The extensive experimental irradiations program progressed satisfactorily throughout the year with the completion of the irradiation of 22 subassemblies containing 335 experimental specimens.

As in any endeavor, most progress is obtained by experiencing difficulties, and EBR-II is no exception. This paper therefore includes the more pertinent difficulties encountered during operation of the reactor and its associated systems.

1. Introduction

Argonne National Laboratory, operated by the University of Chicago under contract with the Argonne Universities Association and the U. S. Atomic Energy Commission, has been the leading research and development center for the development of the liquid-metal-cooled fast breeder reactor concept. Experimental Breeder Reactor I, now a national historical landmark, was the initial step in demonstrating the feasibility of the LMFBR concept. Primarily because of its successful operation, the development of a larger reactor was started.

This larger reactor, Experimental Breeder Reactor II, was designed and is now operated by the Argonne National Laboratory for the U. S. Atomic Energy Commission. The purpose of the design and early

operation of the reactor was to demonstrate the feasibility of a larger sodium-cooled fast breeder reactor operating as an integrated reactor and power plant. To this purpose the reactor has operated successfully, and principal events since its construction include (a) dry critical experiments,⁽¹⁾ September 1961; (b) sodium-system tests, January 1963; (c) wet critical experiments,⁽²⁾ November 1963; (d) approach to power,⁽³⁾ July 1964; and (e) the insertion of the first reprocessed and recycled fuel from the Fuel Cycle Facility.

During the past two years, the role of EBR-II has been reoriented from that of a demonstration plant to that of an irradiation facility for fast-flux irradiation of candidate fuels and structural materials as part of the AEC's Liquid Metal Fast Breeder Reactor Program. The first experimental irradiation subassembly was inserted in May 1965,⁽³⁾ and the first singly encapsulated experimental fuel pins in August 1968.

Continued operation of the plant also provides operational and engineering information on fast breeder reactor components and systems in sodium environments at elevated temperatures under fast neutron irradiation. The experimental information obtained will be useful in the design of other fast breeder reactor plants.

To date the reactor has operated to the midpoint of Run 31. The design, construction, and early operating experience with EBR-II have been the subject of numbers of papers and reports.⁽⁴⁻⁷⁾ Winkleblack⁽⁸⁾ has described operations up through and including reactor Run 26A. Since that time, reactor operations have been completed through Run 31D, for a cumulative total of 490,500 MWh. This paper will update the reporting of EBR-II operations, commencing with Run 26B and concluding with Run 31D, which will have occurred through calendar year 1968.

2. Operating Experience in 1968

During the end of 1967 and the first quarter of 1968, EBR-II experienced several operational problems which had an adverse effect on plant factor. These problems included a continuing low power co-

efficient (associated with the use of a stainless steel radial reflector), small fission-product releases, a damaged control-rod thimble, and two incidents involving reaction of sodium.

Reactor Run 26C continued during December 1967. The power coefficient between 15 and 25 MW continued to be near zero. At the end of Run 26C with the control rods at the 14-in. banked position, the power coefficient at 25 MW reached the operating limit of 0.5 lh/MW.

During fuel handling for Run 27, interference at the 60-in. elevation was detected during the removal of control subassembly L-446 from control-rod position number 12 (grid position 5-C-3). This control subassembly was removed from the core with some difficulty. Therefore, to permit the insertion of gaging tools and the inspection of the control-rod thimbles, the bulk sodium temperature was reduced from 700 to 450°F. In addition, an access plug was removed from the small rotating plug.

Verification of damage to the control-rod thimble in position 5-C-3 was made by inserting the gaging tool through the auxiliary gripper plug location. An obstruction was apparent at the top of the thimble flat adjacent to core position 5-C-4. This gaging operation also indicated that a new thimble-removal tool which had been designed and fabricated could be locked in the damaged thimble. Checkout of the operation of the tool was performed without incident. As a precautionary measure, six control rods which had not been changed during the fuel handling for Run 27A were manually raised approximately 12 in. in order to obtain data on potential binding.

The primary-tank bulk sodium was heated to 600°F in preparation for the fuel-handling operations associated with removal of the damaged thimble. The six surrounding subassemblies were removed and replaced with special scalloped dummy subassemblies designed to permit rotation and removal of the thimble. The damaged thimble was removed by using the main core gripper and normal fuel-handling procedures. A new control-rod thimble was installed through the auxiliary gripper hole by using a special insertion tool. The new thimble was locked in the grid, and fuel handling to remove the six dummy subassemblies was begun.

Reinsertion of the auxiliary gripper plug was in progress when a chemical reaction occurred in the auxiliary gripper plug hole. A small amount of sodium was expelled around the plug shaft and up through the hole. Investigation indicated the probable cause of the sodium expulsion to be residual alcohol-water cleaning solution in the clearance be-

tween the labyrinth seal and the shaft of the auxiliary gripper plug. Damage to equipment due to the burning sodium was insignificant and contamination was minor.

The auxiliary gripper plug was removed from the primary tank and inspected for damage by pulling it into a container with an inert gas atmosphere and a viewing window. Visual inspection of the labyrinth and retaining cap revealed no damage, and the plug was returned to the primary tank. Following these operations, the bulk sodium was returned to 700°F for the start of Run 27A. Fuel handling was resumed after cleanup of the sodium had been accomplished. Six control rods which were not changed during the Run 27 fuel handling were individually lifted to the full-up position of the core gripper and lowered again as an additional test for binding.

Among the driver-fuel surveillance subassemblies installed for Run 27 was one subassembly including nineteen 70%-enriched fuel pins. This subassembly was installed in Row 4 as an irradiation experiment related to the increase in power program.

On February 9, while the reactor was shut down, a sodium fire occurred in the west-wing operations room of the sodium boiler plant. The fire resulted from the escape of sodium from a valve which was being replaced in the secondary-system plugging loop. An estimated 80 gal of sodium were lost from the system. Damage in the west wing, while unsightly, was not extensive, and cleanup and repair of equipment and systems was begun immediately. On February 16, heatup of the secondary and steam systems was started; heatup of the system to 700°F was completed by February 22, and reactor operation resumed.

The next problem encountered in EBR-II was a small fission-product release.

Reactor Run 27B began on March 1 and was terminated on March 5 when an increase was noted in the ^{133}Xe and ^{135}Xe activity in the primary-system cover gas. The reactor runs which followed were scheduled primarily to determine the identity of leaking subassemblies. Runs 27C, D, E, F, and G were all terminated by the detection of an increase in fission-gas activity. The fuel changes associated with these runs involved the removal of several experimental subassemblies at each shutdown. An increase in the fission-gas activity was noticed when experimental subassembly X028 was being removed from the core. Operation of the reactor with X028 removed identified it as a potential failure. It was later reinserted, and subsequent fission-gas release confirmed that X028 was a leaking subassembly.

During reactor Run 28 all the experimental subassemblies removed during Run 27 were reinserted.

Low-burnup subassemblies, primarily those from Rows 6 and 7, were returned to the reactor first. Rod-drop and physics data were taken before and after insertion of these subassemblies to obtain a series of direct measurements on the effect of experimental fuels, primarily mixed oxides, on the kinetic behavior of the reactor. Run 28C was concluded in June with no further fission-product release.

As an answer to the continuing low power coefficient, the decision had been made to replace the stainless steel radial reflector with depleted uranium, and this was carried out for Run 29. Run 29A was completed on July 5, 1968 to establish the basis for further reactor and kinetics measurements during the change from the stainless steel to the depleted-uranium blanket. The reactor was shut down for the exchange of Row-7 and -8 stainless steel subassemblies with depleted uranium. Measurements of the power-reactivity decrement (PRD) taken prior to the blanket change indicated a value of 51 lh at 45 MW. With Row 7 exchanged, this value was approximately 57 lh. When Row 8 was exchanged the measured PRD at 45 MW had increased to 84 lh. The reactivity losses associated with the substitution of the depleted uranium for stainless steel in Rows 7 and 8 amounted to 355 and 225 lh, respectively. The total change in reactivity of 580 lh compares favorably with the 610 lh estimated on the basis of the results from similar measurements made at the Run 24-25 interface. The power oscillations of approximately 2 MW as measured at 41.5 MW, with a 17-min period, noted in previous runs, were no longer present after the blanket substitution.

The loading changes for Run 29D included the installation of several special flux-wire subassemblies to establish the fission-rate distribution in the core and the inner blanket. Following the flux mapping, fuel handling for Run 29A was completed and the reactor was restarted on August 21. Reactor power was raised to 50 MW for the first time on August 26, after physics measurements at selected power levels.

Following this increase, operation continued satisfactorily at 50 MW through Run 30A, which concluded on September 5, 1968. During Run 30A the reactor loading included one 37-pin 70%-enriched subassembly which achieved 0.85 a/o burnup. Fuel-loading changes for Run 30B included the insertion of an orificed controlled-flow driver subassembly for additional surveillance information for the increase in power program and two newly installed 91-pin 70%-enriched subassemblies. Operation at 50 MW was resumed on September 7. On September 9, a small unusual increase in ^{133}Xe and ^{135}Xe activity was observed in the manually obtained cover-gas samples.

After verification of this observed increase in fission products, the reactor was shut down on September 11. During this period there were no indications of a significant increase in either the Fission Gas Monitor (FGM) or Fuel Element Rupture Detector (FERD) fission-product detectors. An examination of the data indicated that the fission-product increase was about 80% above background, was not a gas release, and involved a small continuous loss in sodium bond.

The reactor has been operated intermittently since this time in order to locate the source of fission-product leakage. Incremental removal of all the experimental subassemblies has been followed by periods of reactor operation at 30 MW to determine if a leaking subassembly is still present. To this date, all the experimental fueled subassemblies have been removed and replaced, and all the standard core driver subassemblies have been removed and replaced with no evidence of a decrease in the fission-product signal. The reactor is currently being operated under special surveillance at 50 MW to obtain additional information on the source of fission products and to continue the fuels-irradiation program.

Although problems have been encountered, operation of the plant has improved considerably over that in CY 1967. The plant utilization was low primarily in the first quarter, due to the fission-product releases and the several incidents which occurred early in the year. During the second and third quarters, the plant utilization had increased to where the reactor was operated at full power for approximately half of the time. A decrease in the fourth quarter again was related primarily to the problems associated with the present fission-product source.

The total integrated reactor power as a function of time for 1968 is shown in Fig. 1.

2.1. Primary Systems

Performance of the components of the primary sodium system has been excellent. Although both primary pumps failed because of shaft binding during preoperational testing,⁽³⁾ operation since that time has been extremely reliable. The only significant interruption in over 19,700 hr of operation of the primary pumps has been due to component failures in the electrical control system.

During 1967 corrosion of the copper bus bars on the electromagnetic auxiliary primary pump caused an increase in the copper content of the primary sodium. This problem was detected by the observation of copper deposits in the primary plugging-meter piping. The copper bus bars were removed and encased in stainless steel jackets. Since that time the

FIG. 1. Operating History of EBR-II during 1968.

copper content of the primary sodium system, after having been reduced by cold trapping, has remained relatively constant at about 0.25 ppm. Examination of the stainless-clad bus bars after one year of operation indicated them to be in excellent condition, with no evidence of further corrosion.

The beryllium-copper bushings which were originally installed on the control subassemblies were also subject to corrosion. These were replaced on an experimental basis with bushings made of aluminum bronze. Examination of these bushings on the experimental control rods after removal from the reactor have shown the aluminum-bronze bushings to be extremely resistant to corrosion.

During June, operation of the gripper jaws on the Mark-II oscillator rod became very difficult. Subsequent tests demonstrated the presence of frozen sodium in the region above the seal bellows. The

oscillator drive was subsequently removed. The cause of the bellows failure is being investigated, but has not as yet been determined.

A nuclear-instrument thimble designated 0-1, which originally contained one channel of the operating instrumentation, has been designated an instrument test facility. By removal of the operational instrumentation and by providing control of the cooling air the thimble is now suitable for testing neutron chambers and cabling in a temperature environment from 100 to 675°F. Modifications now planned will provide internal heating with an eventual goal of 1200°F.

The rotating plug seals, which are a trough-and-blade assembly filled with a low-melting alloy of tin and bismuth, are designed to contain the primary cover gas (argon) and permit rotation of the plugs for fuel handling. These seals have been operating

FIG. 2. Profile View of Damage at Upper End of Control-rod Thimble from Reactor Grid Position 5-C-3. Damage is on flat which was adjacent to position 5-C-4.

satisfactorily. Past experience had shown that the tin-bismuth alloy, which is heated to above its melting point (280°F) during the fuel-handling operations, oxidizes on the outer surface of the trough, and the accumulation of this oxide could prevent or impede rotation of the plugs. In 1966 vertical access holes to the seal were provided. A routine procedure of vacuum cleaning and brushing, now scheduled after every two to three fuel-handling operations to remove oxide, has improved the reliability of plug rotation. Seal melting requires 1–2 hr as contrasted with periods of up to 12 hr required previously.

This method of seal cleaning satisfies the current requirements for fuel handling, but is obviously not the long-range solution to the seal-plug problem. Additional research and development efforts will continue. A mechanical screw conveyor, for example, is being investigated to determine its feasibility for the

removal of oxide from the seal trough. Coatings such as silicon oil may possibly be employed to prevent oxidation. However, the effects of such material when mixed with the oxide already present and the reactions with the sodium and seal alloy are being carefully studied before the final solution is determined.

A power-increase program has as its final objective the operation of EBR-II at a power level which produces the best overall gain in the utilization of the reactor as an irradiation facility. In order to determine some of the parameters and study their effects on plant factor and the availability of the reactor for irradiations, a fuel-surveillance program has been set up utilizing subassemblies of higher than normal enrichment and subassemblies containing special orifice plates for reduced coolant flow, to simulate fuel temperature conditions at 62.5 MW. Data obtained to date (up to 0.85 a/o burnup), although preliminary, have indicated an increased fuel swelling rate as the fuel temperature is increased. The two 91-pin 70%-enriched subassemblies and the one controlled-flow subassembly have not as yet completed their target exposures because of the limits placed on reactor operation by the present fission-product source. This program will continue when normal operation is resumed.

The primary cold trap, which had been installed during original construction, showed intermittent

FIG. 3. Elevation View of Damage at Top of Control-rod Thimble Removed from Reactor Grid Position 5-C-3.

plugging during operation in 1967. Since removal and replacement would require several months and would remove the only sodium-sampling and plugging meter from operation, backup systems were provided. A plugging meter and sampling system were attached to the FERD sodium loop in order to monitor primary-system chemistry during the cold-trap replacement.

Although the primary cold trap has been out of service since the end of June 1968, plugging determinations have indicated only a slight increase in plugging temperature during this period ($<225^{\circ}\text{F}$ to 260°F). Other trace impurities have remained relatively constant.

The cold-trap replacement included a complete redesign of the piping so as to permit trap removal without piping removal. All piping connections to the cold trap are now through conoseal mechanical connectors. These connectors are arranged so that remote disconnection is possible in the event that removal of a highly contaminated trap becomes necessary. The new primary cold trap with its associated plugging meter and sampling system is expected to be in operation by mid-November.

2.2. Secondary and Steam Systems

Operation of the secondary sodium system has also been excellent. After the initial problems with the secondary sodium pump,⁽⁷⁾ performance has been

FIG. 4. Dimensions of Deformation of Control-rod Thimble Removed from Reactor Grid Position 5-C-3.

FIG. 5. Probable Sequence of Events Which Led to Damage of No. 12 Control-rod Thimble.

FIG. 6. Operation of Holddown Mechanism Showing Control-rod Thimble in Improper Position.

very reliable under the flow conditions required for 45- and 50-MW operation ($\sim 75\%$ flow).

The steam generators and superheaters have performed in an excellent manner. No evidence of leakage of water to sodium has been encountered. Some steam and water leaks have occurred from flanges

FIG. 7. Test Specimen: Top View of Simulated Damaged Control-rod Thimble.

and valves in the steam system. In general, one would expect a relatively larger number of such failures in an experimental plant because of the frequency of changing power levels and temperatures. In portions of the system where the temperature remains constant, such failures have been extremely rare. The cold trap of the secondary sodium system is operated only intermittently to maintain the plugging temperature between 230 and 280°F. Oxide and other contaminants have not been a problem in the secondary sodium system.

2.3. Fuel Handling

Operation of the fuel-handling system and components has improved as a result of modifications and improvements. Replacement of the fuel-unloading-machine (FUM) gripper has essentially eliminated unscheduled downtime for that component. The FUM gripper was redesigned to operate in a gas and sodium environment, and to tolerate a certain

amount of sodium oxide on its working parts. The new gripper satisfied all the original design criteria and has proven to be highly reliable. Average fuel-handling operations involving transfers from the core to the storage basket and return require approximately 72 min. Fuel-handling operations for runs 31-C and 31-D were each accomplished in approximately 20 hr.

The EBR-II fuel-handling system has transferred 3,068 subassemblies into and out of the core since the start of the approach to power in 1964.

No significant breakdowns or delays have been experienced with the fuel-handling system during 1968. Much of the equipment used in this system is unique, and EBR-II experience is among the first for components operating continuously under sodium at these temperatures. EBR-II has made and will continue to make an important contribution to the LMFBR Program in the fuel-handling area.

The damage to a control-rod thimble (see Figures 2, 3, and 4) previously discussed was evidently caused by several factors present during normal fuel-handling operations. A probable sequence of events is shown in Fig. 5.

When a subassembly is to be removed from a position adjacent to a control-rod thimble, for example position 5-C-4, the relative position of the holddown, the control subassembly, and the control-rod thimble are shown on Fig. 6. Because of the clearance between the control rod and its thimble and between the thimble and the adjacent subassemblies, it is possible for the thimble to move approximately 0.054 in. into the hex space to be occupied by a subassembly in position 5-C-4. When the replacement subassembly is lowered into this position, it was possible in the past for the hex can, because of the maximum accumulation of tolerances, to protrude 0.018 in. This protrusion might catch on the upper lip of the thimble. Laboratory tests (see Fig. 7) have indicated that the noted deflection in the thimble upper edge can be produced by an initial force of approximately 500 lb. It is interesting to note that prior to this incident the push-force-limit device on the fuel-handling gripper had originally been set at 500 lb. As a subassembly is lowered the damage to the thimble would progress as illustrated.

In order to prevent additional failures of this type several remedial steps have been taken. (1) The high and low push-force trips and alarms for the fuel gripper have been reduced to 210 and 250 lb, respectively. (2) Fabrication procedures for subassemblies have been revised to prevent any over- of the hex can and the lower adapter. (3) A plant modification has been approved that will provide a tapered hex collar on the top of each control sub-

TABLE 1. SUMMARY OF FISSION-PRODUCT RELEASES

Date	Time of Release	Reactor Power Level (MWt)	MWdt of Operation Since Previous Release	FGM ^(a) Response	FERD ^(b) Response	Approximate Increase in Xenon Activity, ($\mu\text{Ci/cc} \times 10^3$)		Indication of Sodium-bond Loss	Comments
						¹³³ Xe	¹³⁵ Xe		
12/6/67	1800	45	430	Negative	Negative	270	160	Analyses of xenon buildup indicates probably sodium-bond loss	Following 11/24 release and removal of suspect subassemblies, approximately 10 days of 45MW operation logged prior to 12/6 release.
3/5/68	1500	45	636	Small	Negative	75	25	None	
3/11/68	0900	30	52	Indefinite	Negative	25	1.5	None	Occurred during reduced-flow testing following series of several scrams
3/12/68	0800	0	10	Negative	Negative	25	<0.5	None	Apparently associated with changes in primary coolant flow
3/12/68	1500	0	0	Negative	Negative	7	<0.5	None	
4/6/68	0400	45	286	Large	Negative	75	95	None	Approx 300 MWd logged between 3/30 and 4/6
4/6/68	1015	0	0	Small	Negative	130	135	None	Followed immediately after stopping of primary coolant pumps
4/11/68	2015	0	90	Barely perceptible	Negative	130	22	None	Followed reduction in flow after scram from 45 MW
4/16/68	0230	0	68	Negative	Negative	125	17	None	Followed reduction in flow after scram from 45 MW
4/19/68	1215	45	49	Small	Negative	90	25	Radiochemical analysis of primary coolant sodium and analysis of xenon decay inconclusive; cannot be verified that bond loss did not occur	
4/19/68	1330	0	0	Large	Negative	150	140		Occurred after rapid shutdown following 1215 release and subsequent stopping of pumps
4/20/68	2000	0	0	Negative	Negative	35	50		Followed lifting of X028 during fuel-handling operations
5/6/68	0539	45	96	Large	Negative	30	140		After replacing X028 in the reactor

NOTE:

(a) Fission Gas Monitor.

(b) Fuel Element Rupture Detector (monitor of delayed neutrons).

assembly to reduce the clearance between the subassembly and the thimble. This will prevent thimble motion while removing adjacent subassemblies.

Fission-product Release

Since the initial startup of EBR-II, the reactor has experienced three or perhaps four instances of failed fuel. Two of these have been previously re-

ported.⁽⁹⁻¹⁰⁾ In both the breaks were annunciated by significant increases in the fission-gas monitors and by cover-gas analyses. In neither case was a signal observed on the delayed-neutron monitor FERD. The process of locating the leak has consisted of detective work in trying to correlate the signal responses with the type of failure and then predicting which subassemblies are most suspect. After the suspects

are listed in the order of their probability of being the leaker, the actual isolation of the leaking subassembly has been a matter of removing one or more suspects, followed by reactor operation to determine if the leak is still present. In the previous examples it took only two and one iterations, respectively, to locate the leaker. Since that time the reactor has experienced two more fission-product releases, but the isolation of the leak has not been quite so straightforward, even though the method of isolation has been essentially the same.

On December 6, 1967, during the middle of Run 26-C, it became evident that there was an unexplainable increase in the cover-gas activity. Neither the FGM nor the FERD system indicated any comparable increase, but subsequent analysis of the bulk sodium for the 8-day ^{131}I did show a significant increase. The reactor continued in operation for three or four days after the first leak indication (150 MWd) with no other indication of a leak other than the increase in cover gas and the iodine content in the bulk sodium. At the conclusion of run 26-C the reactor system was put into a search mode, i.e., the full effort of EBR-II operation was devoted to locating the source of fission products. After each loading change in which one or more suspect subassemblies were removed, the reactor was brought slowly to power with careful surveillance of cover gas, fission-gas monitors, sodium activity, and the FERD system. The reactor was run until either another leak occurred or it was concluded that the leaker had been removed. After eight separate loading changes the leaking subassembly was finally isolated. Table 1 lists the fission-product releases from the reactor during this period of time.

Several observations can be made from Table 1. (1) After the first indication of sodium-bond loss, the reactor operated for 636 MWd before another release was noted, and then was in the form of a gas release. A total of 1287 MWd was acquired on the reactor before the subassembly was finally identified. (2) Periods as short as 10 MWd and as long as 286 MWd transpired between releases. (3) In only two cases were significant signals received on the FGM. No signals were ever observed on the delayed-neutron monitor. This apparent discrimination against the relatively short-lived isotopes indicates a holdup process that strongly suggested that the leaker was doubly encapsulated and that there was no direct exposure of fuel to coolant. The leaking subassembly was identified as X028, which contained 15 capsules of the ternary alloy U-15 w/o Pu-10 w/o Zn and four capsules of the standard uranium-fissium alloy. The subassembly had operated for a total of 1710

MWd and to a maximum burnup level of approximately 0.9%. It has been noted that this experiment was loaded just prior to the gas release of November 22, though it has never been established that there was any relationship between the release of November 22 and experimental subassembly X028.

Experimental subassembly X028 was sent to the Fuel Cycle Facility for examination. Visual examination of the unwashed capsules revealed no abnormalities. However, on weighing them, one capsule, BC02, was found to be 10 g heavier than its pre-irradiation weight. The 19 capsules were neutron radiographed and 18 revealed no failures. Capsule BC02 showed evidence of melting through the fuel-element cladding several inches from the top and bottom of the fuel column. Two minor separations at the fuel midplane were also evident; however, the cladding wall in that area appeared intact. The thin vanadium liner between the cladding and the capsule wall appeared to contain the melting. The cause of the failure has not yet been identified.

Recently, during run 30-A, shortly after reactor power was raised to 50 MW, an increase in the activity level of the cover gas was again noted. Again there was a corresponding increase in the ^{131}I content in the bulk sodium but no corresponding signal increase from either the FGM or the FERD system. During the subsequent 1400 MWd of operation, all the experiments and all the driver subassemblies were removed from the core without identification of the fission-product source. During this period the activation level of the cover gas had not been reduced since the initial increase. The indications are that the activity is the result of a sodium leak, since no direct gas release has yet been observed. These results strongly suggest the possibility that the source is a blanket subassembly.

2.5. Reactor Kinetics

One of the principal concerns of reactor operation is an understanding of the reactor kinetics, i.e., the time response of the system as a function of reactivity perturbations. From an operating point of view, two important factors can be obtained from an understanding of the reactor kinetics. The first is a measure of the stability of the system throughout the entire frequency range of reactivity perturbations. The second is to identify the feedback parameters that limit the magnitude of an accident should an excess amount of reactivity be inadvertently added to the reactor system. During initial startup of EBR-II a rod oscillator was used which enabled the transfer function to be determined for the reactor for a series of flow and power conditions. From these initial

Measurements it was determined that the reactor itself was a very stable system, and that there did indeed exist a prompt negative feedback, attributed principally to fuel expansion and coolant expansion, which provided a prompt shutdown mechanism. Unfortunately, before the entire series of experiments had been completed the oscillator became inoperative.

During the past year the principal method of monitoring the kinetics of EBR-II has been by means of the rod-drop technique.⁽⁹⁾ A calibrated low-worth control rod is scrammed from the reactor in a time of about 150 ms. The resulting power trace is analyzed by means of inverse kinetic computer codes, and the time response of the feedback parameters is obtained. This rod drop is only applicable in the time range of from ~0.1 second to 100 seconds.

These data have been treated in two ways. First, by inspection of a set of data under a given set of conditions of power and flow, it is possible to establish the basic stability condition of the system. Further, comparison of one set of data to subsequent data is a way of monitoring for possibly significant changes in the kinetics of the system. The second way of data treatment is to attempt to curve-fit the feedback data with a feedback model, and hence

assign effective amplitudes, time constants, and time delays to fit the data best. A considerable number of rod drops have been performed during the past years at various power levels and reduced coolant flow with the stainless steel reflector and with the uranium blanket. The detailed results with the data from Run 26 are being reported by R. Hyndman *et al.*, and are soon to be released. In Hyndman's report a comparison is made between rod-drop data and a limited number of oscillator results. Feedback models for the 22.5-MW and 41.5-MW full-flow conditions have been proposed which fit the measured data with four terms, as shown in Figures 8 and 9, which show representative rod-drop results. The upper curve is the power trace and the lower curve the resulting feedback obtained from the inverse kinetic calculations.

The four parameters have tentatively been associated with the following physical quantities: (1) axial fuel expansion, (2) sodium expansion and expulsion, (3) normal core bowing, and (4) bowing action of Row 8 stainless steel reflectors.

Unfortunately, the data can be accumulated much faster than it can be completely analyzed, but complete reports of all rod-drop data are now being prepared. The above models detail the general fea-

FIG. 8. Rod-drop Data for Run 26B at 41.5 MWt and 100% Flow.

FIG. 9. Rod-drop Data for Run 26B at 22.5 MWt and 100% Flow.

tures of all the reactor loadings during Runs 25 through 29-A, the period in which the stainless steel reflector was in the reactor. After the stainless steel reflector was changed to the uranium blanket in Runs 29B and C, the feedback was considerably altered. It was observed that the prompt feedback, i.e., the data obtained in the first second of time, is about the same in magnitude before and after the change-over, but the delayed feedbacks are considerably altered. This is reflected in the fact that the stable power coefficient is substantially greater for the reactor after the stainless steel reflector was replaced, a result expected on the basis of the reverse bowing model proposed for the stainless steel reflector. There is a continuing effort in further developing the rod-drop method as well as examining other methods for determining the kinetics of the system.

2.6. Fission-rate Distribution

Subassemblies containing flux-wire detectors were used to measure the U-235 fission-rate distribution in the reactor during Run 29-D. The measurements were made in two steps, one at the beginning of the run and one at the end. The measurements involved a total of 67 detectors distributed over 12 core

and blanket positions. The two separate runs of one hour at 50 kW were normalized to each other by placing a detector in the same position during both runs. An attempt was made to locate the flux-wire detectors near experimental subassemblies. Relative fission rates were determined by counting the fission-product gamma activity in the energy range from 0.5 to 0.85 MeV. The relative data as a function of position from the geometrical center of the reactor are plotted in Fig. 10.

The tilting of the neutron flux in the reactor core as a result of specific experimental loading arrangements has been apparent for some time. Although it would be possible to measure this tilt for each experimental loading by means of flux-wire irradiation, such as was done in Runs 20, 25, and 29-D, it would be advantageous to determine the shift either analytically or experimentally. Two-dimensional calculations have been under development for some time, but they are not yet ready for general operational use. A promising experimental method is also under development. It uses the variation in individual worths of control rods as a means of locating nuclear center of the reactor. A code has been written which uses the MACH-I real and adjoint fluxes to

calculate the relative worth of each control-rod position from a given center. By a series of iterations the nuclear center is moved with respect to the geometrical center until the variation between the measured and calculated worth of each control rod is minimized.

The U-235 fission-rate distribution shown in Fig. 10 has been replotted in Fig. 11 as a function of the distance from the nuclear center. The agreement between the measured data and the calculated distribution is good.

This technique is now being applied to data from Runs 25 and 20. If the results are satisfactory, this method will be employed routinely to determine fission rates in experiments as well as burnup in driver fuel.

2.7. Replacement of Stainless Steel Reflector with a Depleted-uranium Blanket

The behavior of the PRD following the change from the uranium blanket to the stainless steel blanket has been reported earlier.⁽⁸⁾ A brief review will be sufficient. Following Run 24 the decision was made to enlarge the core to a standard size of 91 subassemblies. This would be accomplished by replacing driver fuel in rows 4 and 2 with either half-worth subassemblies or with sufficient experiments to increase the reactor size to between 88 and 91 subassemblies. This filled the first six rows of the reactor. At the same time, in order to increase the reactivity on the system and eliminate problems of replacing blanket subassemblies in Rows 7 and 8 as a result of their burnup, the decision was made to replace these two rows with a stainless steel reflector. As a result of this change the PRD from 0 to 45 MW was reduced from a value of about 67 lh in Run 24 to 38 lh in Run 25. In addition to a loss in the total amount of reactivity, the shape of the PRD curve was considerably altered. During the early runs the power coefficient ($\Delta k/\Delta P$) was almost linear from 0 to 45 MW, but after the blanket change the coefficient in the region between 10 and 30 MW was considerably reduced and in some cases was almost zero. This change in the shape of the PRD was attributed to what has been called "reverse bowing" of the Row 8 stainless steel blanket. In order to verify that the stainless steel blanket was indeed the cause of the change in the PRD, it was decided to replace the stainless steel blanket with the uranium blanket and to make very careful measurements before and after the change. The blanket changeover was accomplished during Runs 29A, B, and C. Experiments were performed during Run 29A with the stainless steel blanket. In Run 29B, Row 7 of the

FIG. 10. ²³⁵U Fission Rate vs Distance from Geometric Center for Run 29D (Uranium Blanket; 91-subassembly Core).

FIG. 11. ²³⁵U Fission Rate vs Distance from Nuclear Center for Run 29D (Uranium Blanket).

stainless steel blanket was removed and in 29C both rows of stainless steel were replaced with uranium blanket. By carefully adjusting the reactivity between each of these runs it was also possible to measure the worth of the depleted-uranium blanket versus the stainless steel blanket in terms of reactivity. The experiments which were performed before and after the blanket change were as follows: (1) a PRD curve was determined from 0 to 45 MW and back to 0; (2) reduced-flow and constant- ΔT experiments were performed to determine the reactivity coefficient of flow and the fuel-temperature reactivity coefficient; (3) rod drops were performed at various powers and at various flow conditions to determine the linear or non-linear characteristics of the reactor feedbacks; (4) criticalities obtained at each run enabled a measurement of the loss in reactivity due to the replacement of stainless steel with uranium. Table 2 is a summary of these results.

The effect of the stainless steel blanket on the

TABLE 2. SUMMARY OF RESULTS FOR ROW 7 AND 8 BLANKET CHANGE FROM STAINLESS STEEL TO URANIUM

	Run No.		
	29-A	29-B	29-C
Row 7 (material)	Stainless steel	Depleted uranium	Depleted uranium
Row 8 (material)	Stainless steel	Stainless steel	Depleted uranium
$\Delta k/k$ (Ih)	(Ref.) 0	-355	-580
PRD 0-45 MW (Ih)	-51.5	-57.5	-84.4
Constant- ΔT Exp (Ih) ^a	8.3	7.5	8.4

^(a) Gain of reactivity between reactor conditions of 41.5 MW with 100% flow and 22.5 MW with 54% flow; $\Delta T = 122^\circ\text{F}$ for both cases.

total reactivity decrement is certainly obvious. Examination of the complete data shows that the PRD for Run 29C is nearly linear with power. Run 29B is somewhat of an intermediate case, and calculations indicate that some reverse bowing might still occur with one row of stainless steel. In performing the constant- ΔT experiments, the reactor ΔT was fixed at 122°F for two power and flow conditions; 41.5 MW with 100% flow, and 22.5 MW with 54% flow. The difference in reactivity between these two conditions is a measure of the reactivity associated with a change in fuel temperature only. The differences between these conditions in Runs 29A, B, and C were 8.3, 7.5, and 8.4 Ih, respectively. Obviously the fuel-expansion-coefficient effects are not a significant function of the reflector. The rod-drop data for kinetics analyses are still under preparation and will be published at a later date. The worth of the uranium in Row 7 compared to the stainless steel in Row 7 was measured to be -355 Ih. The worth of uranium in Rows 7 and 8 together was -580 Ih. From the preliminary results it is obvious that the stainless steel blanket had a marked effect on the PRD curves, the power coefficient, and the reactor kinetics of the system.

2.8. Experimental Irradiation Program

The use of EBR-II as the prime US fast-flux-irradiation facility for LMFBR candidate fuels and materials was initiated in May 1965. Since that time, ANL has charged a total of 652 experimental capsules or fuel elements into EBR-II in 45 subassemblies. Of the capsules and fuel elements charged, 393 were fuel samples, 241 were candidate cladding-material capsules, and 18 were capsules containing either graphite, insulating ceramics, or poison (control) materials. The 45 subassemblies containing these

irradiations have utilized reactor grid locations in Rows 2 through 7 of the core and inner blanket regions, 30 being in the core and 15 in the inner blanket.

The experimental loading on October 1 is presented in an EBR-II grid chart (see Fig. 12). This chart shows that 14 subassemblies were located in the core at that time. It is estimated that three more experimental subassemblies having reactivity worths about one-half that of a driver subassembly could have been loaded in the core region for that particular loading before reaching the maximum capacity of the core. In addition to the capacity for "low-worth" experimental subassemblies, ranging in number from about 15 to 20, depending on their location and material, the reactor core also can accommodate an undefined number of experimental subassemblies that have reactivity worths about equal to a driver. About 20 "equal-worth" subassemblies can be accommodated, with more or fewer being possible depending on the effects of these subassemblies on the other technical aspects of reactor operation. The near-term equal-worth subassemblies projected for EBR-II are all to contain unencapsulated mixed-oxide fuel elements. The first of these unencapsulated equal-worth subassemblies, X040, is among the 14 fueled experimental subassemblies now in the core.

As an integral part of ANL's irradiation services, the Laboratory has designed and provided the experimental subassembly hardware for all the irradiations. Presently there are six subassembly designs available to accommodate experimental capsules and elements. These are the MK-A-19, MK-B-7, -19, -37, and -61, and the MK-F-37. Up to October 1, the 45 experimental subassemblies that have been loaded into EBR-II were divided as follows: MK-A, 27; MK-B-7, 13; MK-B-19, 2; and MK-B-37, 3. The six types of subassemblies above have capsule or element capacities of 19, 7, 19, 37, 61, and 37, respectively, and can accommodate fuel-element diameters up to 0.300 in. Several more experimental subassemblies that are being designed or are in the final stages of approval will enlarge our experimental capabilities.

As a general projection, we expect to continue the irradiations program at or near full core capacity for low-worth experiments, and at the same time charge oxides before the end of FY-1969. During this time the numbers of mixed-oxide fuel samples will, of course, increase significantly; mixed-carbide samples should increase slightly, and a few mixed-nitride fuel samples will also be initiated. The metal-fuel program will continue in modest numbers except that driver-fuel improvement testing should increase

C# - CONTROL ROD
S# - SAFETY ROD
D - DRIVER FUEL
P - $\frac{1}{2}$ DRIVER FUEL, $\frac{1}{2}$ SST.
B - DEPLETED URANIUM

FIG. 12. EBR-II Loading Pattern for Run 31A (30 Sept 1968 to 4 Oct 1968).

some; cladding-materials irradiation, although expected to be reduced in numbers, still will remain at a significant level; and miscellaneous materials irradiations should increase slightly, primarily in the blanket region.

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Discussion

Mr. Ash (AI): Is there any work underway on a physical model that describes the shifts in the power coefficient that you observed when the stainless steel was in the outer region? Are you trying to understand and explain this?

Mr. Deegan: Yes, considerable work has been done on looking at the things which cause the power coefficient to change. The current explanation is one that involves reverse bowing phenomena in the stainless steel reflector. There are portions of time when it's bowing inward, causing a compaction of the core and hence providing a positive feedback to the system. This occurs apparently in the middle power range. Once you're past that particular power-temperature condition, then the overall expansion is again outward and the power-coefficient curve returns to its normal slope. The work on this is continuing.

Mr. Ash: Has the prediction been good enough for quantification and comparison with experimental evidence?

Mr. Deegan: Analysis has been applied successfully but not perfectly to several cases. An ANL report by Hyndman and Nicholson (ANL-7476) has been

issued, and other work by Bump (ANL-7528) and by DeShong (ANL-7529) will be issued soon.

Mr. Gilbertson (CE): You indicated that you were able to isolate some of the leaky or failed fuel elements fairly rapidly, whereas others took a considerable amount of time. I presume that these were detected by gross fission-product-monitoring devices in the sodium or cover gas. Do you have some sort of local indications? How do you isolate these particular failed subassemblies?

Mr. Deegan: Unfortunately, the only indication we have is an overall monitoring of the reactor cover gas for the sodium activity and so forth. We have no way of pinpointing the location of any one of the leaking subassemblies. As I mentioned, the process involves a certain amount of detective work. You look at the signals and from these you can perhaps decide whether it's a sodium-bond leak or a direct gas release. You also can look at things like the ratio of ^{135}Xe to ^{138}Xe to see if there's been some holdup time and deduce where the leak is coming from. On the basis of this deduction you set up a table which lists the most likely subassemblies. We have been removing these first and operating the reactor to see if the leak is still present. The first two times, this turned out to be a fairly successful technique, and we located the leakers quite rapidly. On both of the last two occasions, this approach has not been very satisfactory.

Remote Retrieval and Removal Operations in the Fermi Reactor

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ABSTRACT

Removal from the Enrico Fermi reactor vessel of a metal object which caused coolant blockage (leading to fuel melting) and of five similar sheets of metal required the design, development, and testing of unusual tools, periscopes, and lighting equipment. This equipment was used at the bottom of the reactor vessel in an environment of argon and sodium vapor at 350°F adjacent to the high radiation field of the reactor core. Access was limited to vertical penetrations containing 1.69-in.-diameter restrictions, and passage through a 14-in. pipe for sodium containing a 10-in.-diameter orifice and other obstructions. The first object was removed successfully by use of these tools. These tools were improved, and additional tools provided for detaching and removing the remaining five segments. Their use was demonstrated in a full-size mockup of the bottom and top portions of the reactor.

Background

The Enrico Fermi reactor was shut down on October 5, 1966 to investigate an unusual decrease in reactivity, accompanied by detection of fission product leakage, observed at 34 MWt during a routine startup. Subsequent investigation disclosed that the reactivity loss was due to partial melting of the fuel pins in a pair of core subassemblies, caused by blockage of the sodium coolant to these subassemblies. The cause of this blockage was found to be a segment of sheet metal which became detached from its installed location in the high-pressure plenum and was carried by the flowing sodium to the underside of the core support plate. Removal from the reactor of this segment of metal, and also of five similar segments, has required unusual remote operations utilizing a variety of specially designed tools.

Following securing of the plant and raising of the core holddown, an investigation was made of

the condition of the core. This was done by lifting each subassembly in the core and in a portion of the blanket a fraction of an inch while measuring the lifting force and observing for pairs welded together. This testing was done under sodium by means of the offset handling mechanism (OHM), modified to improve its load-sensing capability. By this means, one pair of subassemblies were found to be stuck together.

The reactor vessel was then partially drained of sodium and the stuck pair separated by a manually operated tool. These and several neighboring subassemblies were withdrawn from the reactor and shipped to Battelle Memorial Institute, where they were destructively examined in an inerted hot cell.⁽¹⁾ These examinations indicated that the reactivity change was accounted for by the observed fuel redistribution, but that the flow blockage must have been exterior to the subassemblies, i.e., in the high-pressure plenum. Following completion of draining the reactor and fabrication of an extension for an existing periscope, inspection of the plenum revealed an object evidently capable of blocking flow to a pair of subassemblies. Planning and design of methods and tools for removal of the object were then undertaken.

Requirements for Access

As shown in Fig. 1, the object was located in the lower plenum of the reactor, 35 ft below the top of the rotating plug. The plenum is an 8-ft-diameter cylinder, 1½ ft high, and having three 14-in. pipes for flow of sodium welded to nozzles on its periphery, through which 8 million lb/hr of sodium enter during reactor operation. Above the support plate the radiation level rises to about 10,000 R/hr at core midplane. Temperature throughout the reactor is maintained at 350°F to prevent damage due to freezing and thawing of the sodium remaining in the system.

FIG. 1. Access Routes for Object Removal.

Access into the lower plenum was possible downward through the rotating plug and also by penetrating one of the 14-in. pipes. From above, a pair of 6-in.-diameter penetrations through the rotating plug were available, giving access to three sides of the cone in the lower plenum. (A third penetration, giving access to the fourth side, was occupied by the core sweep mechanism. This was not removed until the start of operations to remove the five remaining segments.) Valved standpipes were installed on these penetrations to permit insertion of periscopes, lights, and tools into the reactor, utilizing gland seals above the valves. Access through the core was provided by removal of subassemblies located over the work area. The passage of tools through the core support plate was restricted by the 1.69-in.-diameter holes in the lower plate.

In order to enter tools of larger diameter, and to remove the first object and the remaining five plates intact, a second access was provided by cutting an opening on an elbow in one of the 14-in. pipes, as shown on Fig. 1. The size of tools or objects which could be handled through this access route was restricted by a 10-in.-diameter orifice in the pipe, located just outside the lower plenum, and by a flow divider which vertically bisects the opening of the 14-in. pipe into the plenum. In order to prevent the spread of radioactivity and also to maintain the inert atmosphere within the pipe, all operations through the elbow penetration were performed through a glovebox clamped to the elbow, with the aid of plastic bagging for long tools protruding out of the glovebox.

Identification of the Object

Identification of the object found in the plenum was accomplished by study of photographs of the object. This required tumbling the object into a variety of positions conveniently located for viewing. For this purpose a tool was improvised, consisting of a small conveyor chain modified to flex only in one direction, and mounted to advance or withdraw from one side of a tube passed down through the support plate. A flexible wire hook was attached to the outer end of the chain to engage the object. This tool is shown manipulating the object in Fig. 2.

Since no periscopes were available which combined the required long length and small diameter (40 ft by 1 5/8-in. diameter), extensions were designed and fabricated for several existing scopes. These proved unsatisfactory, so new scopes were designed and built by the plant staff.⁽²⁾ These gave excellent results. A high-intensity light, mounted on an articulated arm, was provided for the viewing and removal operation, and a special camera was adapted to the photographic requirements. This was a Nikon camera system normally used with a microscope. It provided an image splitter to allow viewing the image through the lens, and a computer which automatically timed the exposure within a range of 1/100 sec to 10 min.

With this equipment more than 100 photographs of the object were obtained. Several pairs of these had stereoscopic qualities, and were analyzed by photogrammetric techniques by Battelle Memorial Institute. This analysis enabled construction of replicas of the object, first in its observed contorted form and finally in flattened-out blank form. Comparison of such a replica with a mockup of the lower plenum, provided for object-removal-tool testing, suggested that it could be related to the conical flow guide in the center of the lower plenum. This led to the identification of the object as one of six sheets of 0.040-in.-thick zirconium (see Fig. 3) attached by three screws to the cone to protect it from erosion by molten uranium in the event of a major meltdown. The identification was confirmed by observing that a segment was missing from one sextant of the cone, although the three attachment screws were still in place.

Removal of the Object

In addition to the light and scopes mentioned above, removal of the object required two unusual gripper manipulators.⁽³⁾ One, called the spine tool, consisted of a series of discs strung on a flexible core and having four steel cables passing through

FIG. 2. Tumbling Tool.

holes in their outer portion. This string of discs, resembling a spine both in appearance (see Fig. 10 below) and in its flexibility of movement, was mounted on the end of a long tube passed down through the rotating plug and the core support plate (see Figure 1). A small, parallel-jaw gripper was mounted on the lower end of the spine, with its jaws controlled by a wire cable passing up through the central core of the spine. Flexing of the spine was accomplished by tightening one, or an adjacent pair, of the four outer cables. This tool was used to pick up the object and move it horizontally across the plenum.

The other special tool was used to enter the lower plenum via the 14-in. pipe, grip the object, and pull the object out intact via the 14-in. pipe. The key element of this tool was a length of 5-in.-diameter

flexible metal hose. When this hose passed around the lower pipe elbow it retained a curved configuration which permitted its forward end to pass over the 10-in.-diameter orifice without interference. Then, when it came into contact the flow divider which bisects the opening into the reactor, twisting it 90° moved its forward end to one side of the flow divider so that it could be advanced into the plenum. The flexible hose was attached to a 1¼-in.-diameter spring cable (sewer-cleaning "snake") which provided both push-pull and rotational control of the hose. A standard parallel-jaw gripper was mounted within the flexible hose on a trolley controlled by a 7/16-in.-diameter spring cable threaded through the 1¼-in. hose-control cable. This tool was to receive the object from the spine tool, pull it within its 5-in. hose (where it would be trapped by spring fingers located

Fig. 3. Construction Photo of Conical Flow Guide.

in the entrance of the hose), and so permit removal of the object out via the 14-in. pipe.

Development and testing of the removal tools was accomplished in a full-size mockup of one-half of the lower plenum and one of the 14-in. pipes. Although the full-length versions of the tools were tested in the mockup, no attempt was made to demonstrate the procedure under full reactor simulation, i.e., using actual viewing and lighting equipment. The actual operation of removing the object from the reactor was successful, although hampered somewhat by frequent failures of the light and by the discovery that the object was too large to be fully drawn into the 5-in. flexible hose. This latter problem was overcome by adding fangs to the gripper jaws, to ensure that the object would not slip from the jaws during transit up the 14-in. pipe.

Following removal of the object, the penetration of the 14-in. pipe was closed and the reactor was refilled with sodium, to clean up the system after nearly a year in the drained condition, and to permit in-reactor fuel handling.

Removal of Remaining Segments

Following identification of the object, the other accessible sextants of the cone were inspected. As mentioned above, one sextant had no zirconium sheet, although the three screws were still in place. Two other zirconium segments were found to be in place and securely attached. One was found to be in place, but had one of its three screws missing. The other two sextants could not then be inspected because access penetrations were occupied by installed equipment. Review of the safety analysis for the plant confirmed that the zirconium clad on the cone is not required for safe operation; it was decided to remove the remaining five segments to ensure that they could not become flow blockades.

Study of several general concepts for removing these segments led to adoption of the plan to remove them by removing the screwheads, dislodging the segment from the cone, and removing it out the 14-in. pipe in one piece, using the same tools employed to remove the object. In addition to providing methods and tools to remove the screwheads and to dislodge the

FIG. 4. Mockup of Top of Plug.

segments, improvements to existing equipment were undertaken to extend the life of the light, to improve the manipulation and response of the spine tool, and to provide a more powerful and more maneuverable gripper for the 5-in. flexible hose. Further, the experience with the first removal operations, together with the evident added complexity of removing the segments from the cone, emphasized the need for a more complete mockup of the reactor and plug top, and for a rigorous demonstration of the procedures to be employed.

Mockup Improvements

A rigid platform was installed above the lower plenum mockup at an elevation corresponding to the top of the rotating plug. On this platform was installed a plate carrying simulations of the three valved

standpipes mounted over the penetrations through the rotating plug, together with simulations of the OHM and other restrictions to the work space (see Fig. 4). The mockup of the rotating plug was pivoted on the platform, so as to simulate the rotation of the plug above the support plate and lower plenum.

The lower plenum mockup (see Figures 5 and 6) was expanded from semicircle to full circle, with one-half on casters to permit access into the plenum. The conical flow guide and the core support plate were made of metal and rigidly anchored. The lower plenum was made lighttight, so that viewing could be tested under actual lighting conditions. Dummy subassemblies were positioned on the support plate to outline the actual core vacancies to be provided in the reactor for tool access. The segments to be removed from the mockup were accurate zirconium replicas of the originals, and were attached to the

FIG. 5. Mockup of Top of Lower Plenum.

stainless steel cone by $\frac{1}{4}$ -in. zirconium screws having their heads tack welded against rotation.

Screwhead Removal

From numerous methods considered for removing either the screwheads or the complete screws, three methods were selected for development and comparison: milling (or drilling), chiselling, and electric-arc melting. Milling or drilling was found to be feasible, but either it resulted in formation of an unacceptable amount of debris, or else it required very high tool

pressure. Hence this method was abandoned early. Chiselling was found to be feasible, and a straight tool and an articulated tool were designed and tested. This tool was about 40 ft long by $1\frac{5}{8}$ in. in diameter, and had a pneumatic cylinder at its top end and an articulated arm at its lower end, capable of folding from 0° to 10° . The pneumatic cylinder accelerated a 3-ft-long steel bar downward onto the upper end of a series of rods housed within the outer tube and resting on top of the chisel at the bottom. The pneumatic blow was transmitted as a shock wave to the

FIG. 6. Mockup of Interior of Lower Plenum.

chisel, without causing reaction forces in the tool structure. This tool, when equipped with a spring-steel cup alongside the chisel point, was successful in capturing the cut-off screwhead so it could be withdrawn with the chisel. However, the chisel had several shortcomings: it usually took more than one blow to completely sever a screwhead; sometimes enough of the screwhead remained below the cut line to make subsequent dislodging a formidable problem; and the cut-off screw shank remaining in the cone might be loose and subsequently vibrate out of the cone. Despite these shortcomings, the chisel tool remained a potential candidate until the characteristics of the arc-melt tool were completely demonstrated.

The electric-arc melt tool consists of a $\frac{1}{4}$ -in.-diameter electrode mounted concentrically at the lower end of a 40-ft-long by $1\frac{5}{8}$ -in.-diameter articulated tool (see Figure 6). The electrode holder is, in fact, identical to the chisel tool holder, with the electrical conductor in the center in place of the energy-transmission rods. In addition to the 0° to 10° articulation

of the lower arm, the electrode has a $\frac{1}{4}$ -in. adjustment allowing it to be advanced or retracted axially. The arc melting process consists of positioning the electrode about $\frac{1}{16}$ in. above the screwhead, drawing a high-frequency arc across the gap (which enables the electrode positioning to be checked optically, and also creates an ionized arc path which provides reliable initiation of the melting arc), then melting with a 0.4-sec pulse of 1,350 A direct current (negative polarity on the electrode). This requires an open-circuit voltage 100 V on the power supply, which is supplied by a KSM Type 77 welding machine.

Figure 7 shows the results of arc melting a screwhead. A crater has been blasted into the stainless steel where the screwhead had been. The melted zirconium (screwhead) and stainless steel (cone) have run downward, and solidified on the zirconium plate. Tests of this process in simulated reactor environment showed several desirable characteristics: there was virtually no splatter of debris; between 85% and 100% of the debris welded itself to the zirconium plate,

FIG. 7. Results of Arc Melting a Screwhead.

with a good enough bond that it should remain in place during removal of the segment from the reactor; the zirconium screw shank was lightly welded to the stainless steel cone by the pulse which melted the screw, and a more secure bond was created by a 0.6-sec pulse of 340 A direct current; finally, the process created only a slight bond between the zirconium segment and the stainless steel cone, so that dislodging the segment was not difficult.

Comparison of the performance of the arc melt tool with that of the chisel showed the arc melting process to be preferred. The 0% to 15% of debris not retained on the segment, in the arc melt method, is planned to be removed from the floor of the plenum by a vacuum cleaning system now under development. (This quantity was found to be proportional to the amount of sodium around or under the screwhead.) This comparison established arc melting as the method for screwhead removal.

Dislodging the Segments

One basic tool was devised for dislodging the segments from the cone following screwhead removal. This tool has a variety of interchangeable devices at its lower end: chisels for bending or prying, hooks for pulling or twisting, and a serrated foot for pushing downward. These interchangeable devices are

mounted on a short length of flexible spiral steel cable (sewer-cleaning "snake"), which has been provided in three interchangeable diameters: 1¼, 1, and ¾ in., and in two configurations: straight and with a 10° bend. The flexible cable is mounted on the lower end of a long tube and is operated from above the rotating plug. An outer sleeve can be lowered over the flexible cable to straighten it out for passage through the core support plate.

Tests of these tools in the mockup showed that one of the chisels, mounted on the bent 1¼-in.-diameter spring cable (see Fig. 8), was the only tool needed for the dislodging operation. In some instances, it was found that a light tap by the arc melt tool sufficed to dislodge the segment following screwhead removal.

Removal of the Segments

Removal of the detached segments from the reactor will be done with the spine tool and the 5-in.-diameter flexible hose, the same tools used to remove the first segment (i.e., the "object"). Two major improvements were made to the spine tool: a hydraulic drive on the spine flexing cables, to allow the operator at the periscope to control the travel of the gripper, and a rotatable mounting for the parallel-jaw gripper,

FIG. 8. Dislodging Tool in Position.

FIG. 9. Removal Operations on Plug Top.

to allow the gripper to be rotated about the axis of the spine tool. The hydraulic drive consists of four horizontally mounted hydraulic cylinders having their piston rods connected to chains which pass over sprockets at the top end of the spine tool and there connect to the four wire cables which flex the spine. The control system of the hydraulic drive is arranged to give the operator full control with a midget "joystick" control (visible in Fig. 9 on the counterweight of the OHM).

Any possible leakage of the hydraulic system is prevented by at least two barriers from entering the reactor, and by at least one barrier from spilling onto the top of the rotating plug. Further, any leakage drains back to the power unit containment tank through a transparent hose, and any large hydraulic-system break causes a low-pressure shutdown of the power unit.

The rotatable mounting for the parallel-jaw gripper at the lower end of the spine tool consists of a spiral-grooved plunger which operates rotating ratchets within the gripper body. The plunger is operated

by the gripper-closing cable, and is arranged so that rotation and gripper closing do not occur simultaneously. Figure 10 shows the spine-tool gripper moving a zirconium segment toward one of the flow dividers in the lower plenum of the mockup.

The principal improvement to the 5-in. flexible-hose tool was the development of an improved gripper. The new gripper, shown in Fig. 11, is capable of advancing out of, and returning into, the flexible hose. The gripper can be rotated independently of the hose, which positions the gripper jaws or causes the gripper to roll sideways. The new gripper has a jaw-closing linkage of high mechanical advantage, giving a much stronger grip than the previous gripper. The gripper jaws are closed by cable tension and opened by springs in the gripper. Figure 12 shows the operating cables for the flexible hose and gripper being operated at the glovebox over the penetration into the 14-in. pipe of the mockup.

In order to obtain longer and more reliable performance from the light used in the reactor, the causes of previous lamp failures were investigated

FIG. 10. Spine Tool Moving Segment.

FIG. 11. Flexible Hose and Gripper.

FIG. 12. Flexible Hose Operations at Pipe Elbow.

with the assistance of the lamp manufacturer. It was found that cooling of the bulb was needed, and that exposure of the bulb to sodium vapor was very undesirable. The design of the light was then revised to add an outer shroud of fused quartz around the lamp, and provision for a flow of argon down the light pole and through the annulus between the lamp and the shroud. The revised light is seen on the

left in Fig. 6. Two spare units were built of the revised light assembly, to reduce downtime during the reactor operations. (For the same reason, additional field-fabricated periscopes were provided.)

Environmental Tests

The arc melting process for screwhead removal was developed in an extensive series of tests con-

ducted in an argon environment.⁽⁴⁾ Most of the tests were done in argon at ambient temperature, but correlation tests were run in air at ambient and also in 350°F argon over a pool of molten sodium. The articulated elbow used on the arc melt and chisel tools was tested at 350°F over molten sodium, and the full-length version of these tools and the spine tool were tested in another rig in the same environment. Two of the improved-design lights were tested over molten sodium in 350°F argon to determine the effect of various flow rates of argon coolant.⁽⁵⁾

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Rapsodie's First Year of Operation

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ABSTRACT

The fuel of Rapsodie's first core achieved a burnup of 40, 457 MWd/t on September 25, 1968. This burnup corresponded to a total of 6183 MWd of thermal energy, or 286 equivalent days at full power. Since the beginning of routine operation in August 1967 the reactor behaved perfectly, with an availability of 91%. The fuel irradiation performance has been very good, since releases of gaseous fission products have occurred only rarely. These releases have by no means interfered with normal reactor operation.

1. Introduction

Rapsodie (see Figs. 1, 2, and 3) first reached criticality on January 27, 1967. After completion of preliminary core physics testing, the reactor was brought to a nominal power of 20 MW on March 17. The following months were devoted to completion of various tasks, to an intensive incident-simulations campaign, to a study of normal operating conditions, and finally to a short period of operation under these conditions.

Routine operation started on August 30, 1967. At that time, the energy produced was equivalent to 18.6 days operation at nominal power, and the burnup of the central subassemblies had reached 2.300 MWd/t.

The very satisfying results obtained during the first run allowed an upgrading of the power level of the reactor from 20 to 24 MW by increasing the sodium flow in the core (which could be done without any equipment modifications) and by slightly decreasing the inlet temperature. After brief testing at 26.5 MW on November 8 and 9, operation at 24 MW was finally adopted.

Although this paper is entitled "Rapsodie's first year of operation," results of 13 months of operation are presented, to include the month of September 1968 (see Figs. 4 and 5). The reactor was shut down at the end of September for maintenance and modifications.

2. Highlights

Since August 30, 1967, six irradiation runs have been completed.

The first, which ended on November 11, 1967, lasted

1564 hr and was run at a power of 20 MWt. Four spurious scrams and eleven setbacks, mainly due to failures of the control circuit (and particularly to the terminal ventilators), interrupted the run. The reactor was shut down from November 11 to December 23, 1967 for work on the facility. Such a long outage was justified to permit modifications which could not be performed prior to normal operation, on account of design, fabrication, or delivery delays. During this period we modified the preheating and emergency cooling circuits (by replacement of blowers), the emergency power system, the control of various parts of the secondary circuits, etc.

The second run, started December 23, 1967, was over on March 8, 1968. It lasted 1730 hr and achieved a burnup of 20,000 MWd/t, after having been interrupted by 8 scrams and 2 setbacks mainly associated with the on-line digital-computer system. For the first time, an increase of activity was noted in the argon blanket, due to a release of fission products from a fuel pin.

The third irradiation run lasted 1672 hr and was interrupted by only a single shutdown. On the other hand, the sodium plugging of the argon collector—and the May-June political events—resulted in a outage of 111 hr. On two more occasions we observed characteristic releases of gaseous fission products. The burnup reached 30,000 MWd/t, and the peak integrated flux attained 3.14×10^{22} nvt.

With the fourth irradiation run, a new mode of operation was initiated, involving the use of special car No. 4, which permitted the in-pile transfer of subassemblies under sodium from core to periphery, thus leading to shorter irradiation cycles. The 4th, 5th, and 6th irradiation runs increased the burnup from 40,000 MWd/t.

A period of approximately 4 weeks during which the reactor was shut down began September 25, 1968 for modification purposes. During this annual inspection, we have undertaken: important modifications of the argon circuits; examination of both of the control-rod mechanisms which have been in the reactor for over 15 months without inspection; examination of a primary pump; and important auxiliary-circuit modifications (such as air conditioning and

FIG. 1. Rapsodie Facility.

FIG. 2. Reactor Plant (Outside).

shield cooling). After this modification period the reactor will resume routine operation at the beginning of November, following neutronic and γ -measurement experiments.

Finally, between August 30, 1967 and September

25, 1968 the reactor achieved 6183 MWd thermal energy, equivalent to a full power run of 286 days. Since initial routine operation in August 1967, the reactor totaled 6777 hr of operation, out of a possible 7425 hr, and thus represented an availability of

FIG. 3. Reactor Plant (Inside).

91.3%. During the same period the plant factor (that is, the ratio of the number of equivalent days at nominal power and the total number of days) reached 69%.

3. Operating Experience

3.1. Reactivity

Calculation of the core reactivity is based on curves of control-rod worth and is checked daily.

The zero-power reactivity decreases nearly linearly with an approximate slope of $(d\rho/dt)\phi = 10.6 \times 10^{-5} \Delta k/k/\text{EFPD}$ (where EFPD means equivalent full power days), in agreement with the calculated values.

During uninterrupted operation the full-power reactivity decreases quasi-linearly with a slope $d\rho/|dt|$

close to $(d\rho/|dt|)\phi$. Discontinuities appear during thermal cycles upon return to power following reactor shutdowns (see Fig. 6). This results in a decrease of the power coefficient. This foreseen and calculated decrease is due to the fact that, after having been irradiated for a certain time, the fuel becomes bonded to the cladding and can no longer expand freely.

The power coefficient is now levelling off at $7 \times 10^{-5} \Delta k/k/\text{MW}$; the predicted value was $14 \times 10^{-5} \Delta k/k/\text{MW}$.

The insertion of some additional subassemblies

OPERATION TIME (h)	5 777	EQUIVALENT FULL POWER DAYS	268
FUEL HANDLING (h)	348	LOAD FACTOR (%)	69
MAINTENANCE (h)	1 604	BURN UP (MWd/T)	40 457
UNIQUE STOPS (h)	547	CENTRAL INTEGRATED FLUX	(nvt) $4.25 \cdot 10^{22}$
AVAILABILITY (%)	91.3	NUMBER OF IN-PILE EXPERIMENTS	31
(WITHOUT MAINTENANCE)		(DURING THE 6TH IRRADIATION CYCLE)	
SET BACK	14		
SCRAM	16		
HOURS OF OPERATION OF SODIUM PUMPS			
PRIMARY PUMPS	1st: 12 398	SECONDARY PUMPS	1st: 16 045
	2nd: 13 577		2nd: 14 825

FIG. 4. One Year of Operation of Rapsodie.

FIG. 5. Integrated Power Production for Rapsodie.

leads to an increase of the power coefficient which subsequently decreases. The general shape of the curve is asymptotic.

The importance of the evolution of the power coefficient has led, upon several occasions, to special measurements in order to separate the component attributed to sodium and structural expansion, and, secondly, the component due to fuel expansion (independent of structural expansion) (see Fig. 7). The following table summarizes the results obtained:

Period	Irradiation (Number of Days at 24 MW)	Mean Values of the 2 Fractions	
		Sodium + Structures, 10^{-5} $\Delta k/k/\text{MW}$	Fuel, 10^{-5} $\Delta k/k/\text{MW}$
Startup of the reactor	< 10	10.6	20 to 25
End of the 1st run	65	11.5	± to 5
End of the 2nd run	135	7.5	0
End of the 3rd run	202.1	6.7	0
End of the 4th run	221.7	6.7	0
End of the 5th run	250.8	6.3	0
End of the 6th run	273.4	7.5	0

The loss of reactivity feedback due to the first component is nearly proportional to the difference between inlet and outlet temperatures. We have not observed variations in the slope of the curve corresponding to a given temperature difference at which the clearance between the subassemblies would disappear. Increased feedback was expected beyond this temperature difference, due to the bowing of the subassemblies. Considering the whole core, the bowing effect appears progressively.

Fig. 6. Power Coefficient of Rapsodie.

Fig. 7. Power-coefficient Analysis of Rapsodie.

Fig. 8. Activity of Primary Cover Gas of Rapsodie.

So far no explanation has been given for the variations of the first component of the power coefficient.

The second component of the coefficient vanished as the fuel became more and more bonded to the cladding. We observed with satisfaction that this reduction appeared only during power rises and never during reactor shutdowns. This shows that the oxide pellet did not separate, which is confirmed by the regular form of the reactivity loss as a function of time at zero power.

3.2. Fission Products

In the Cover Gas

Fission product monitoring (FPM) of the argon cover gas showed fission product release on various occasions (see Fig. 8). The analysis of gas samples confirmed the indications given by the detectors, and with the help of these two means, the development of the situation has been perfectly followed. The following table summarizes the most significant results:

Date:.....	Feb 16, 68	Apr 18, 68	May 1, 68	Sept 1, 68	Sept 14, 68
^{133}Xe , $\mu\text{Ci}/\text{m}^3$	$1.8 \cdot 10^6$	$1.7 \cdot 10^6$	$2.4 \cdot 10^6$	$2.4 \cdot 10^6$	$5.2 \cdot 10^6$
^{135}Xe , $\mu\text{Ci}/\text{m}^3$	$0.18 \cdot 10^6$	$0.14 \cdot 10^6$	$0.24 \cdot 10^6$	$0.24 \cdot 10^6$	$0.94 \cdot 10^6$
FPM ^(a) signal (imp/s)	1650	950	1100	6500	14000
Max fuel burnup (MWd/t)	17400	24000	26000	37200	39000
Historical events	After 35-day run interrupted by 3 stops of a couple of hours each	After 13-day run	After 8-day run	After 6 days of non-operation	After 13 days of operation at 24 MWt
Circumstances	After a scram during power rise	During normal operation	During normal operation	During rise to power after a scram	During rise to power following a setback

(a) FPM: Fission Products Monitoring.

The ^{133}Xe to ^{135}Xe activity ratio is approximately 10. The fission yields of these isotopes being nearly equal, this ratio shows a significant delay between their formation and their detection, probably due to gas retention in the fuel. The burst width of the FPM signal shows that the release is not instantaneous, and the level of the FPM signal following the peak shows that cladding tightness failures persist after the initial burst.

An increase of the signal is generally observed just after the shutdown of the reactor.

During the first fission product releases, the contamination level of the building atmosphere was very low, but nevertheless permitted the location and sealing of several small leaks in the primary argon circuits. Following these repairs and improvements of the building partitions, the atmosphere was no longer contaminated by fission products.

In Sodium

The system for detection of delayed neutrons has never given any signal.

The analysis of the fission products transport has been undertaken in two directions simultaneously: increase in the number of sampling points on the reactor, and improvements of methods for radiochemical analysis.

A cell in which rapid sodium samples from the primary circuit can be obtained has been built. A device allowing collection of sodium samples from the reactor vessel during shutdown has been put into service. A new device, derived from the latter, will permit sample collection at power.

This fairly extensive experimental program is being initiated.

3.3. Operating Reliability

Fourteen setbacks and 16 scrams occurred during this first year of operation. Only two setbacks may be attributed to safety reasons. These were caused

by the failure of the Center's main electrical supply during storms in the Cadarache region.

The following table provides a breakdown of the various causes for action by the protective system.

Cause	Scrams	Setbacks
Computer system	13	5
Equipment failure	1	8
Human error	2	1

One of the two scrams associated with operator error occurred during a reactor startup (due to a delay in the inhibition of a safety threshold on a startup neutronic channel). The other occurred at 24 MW (cutoff of the electrical supply on an electromagnet holding a control-rod mechanism).

The high number of safety actions caused by the on-line computer system is mainly due to an intermittent defect which took very long to locate and repair. No spurious safety action has since been recorded.

Equipment defects were the source of 8 setbacks and 1 scram during the first irradiation cycle. A certain number of modifications were carried out during the following working period (particularly on the control of air-blast heat exchangers). This proved to be worthwhile since all these failures have practically disappeared.

Finally, Rapsodie has experienced, as is quite usual for all plants during startup, a relatively high number of spurious shutdowns. Equipment improvements and better knowledge of the plant by the operators have contributed to a reduction in the number of undue safety actions from 15 during the first irradiation cycle to 1 during the 6th cycle.

3.4. Component Behavior

Equipment and components have performed well; overall experience has been completely satisfactory.

Distortions of the reactor vessel induced by temperature gradients have been regularly checked and have not given rise to any abnormal observations. Rotating plugs gave satisfactory performance with only one exception, described later. The fusible metal seals performed perfectly. Their surfaces, checked now and then, are quite clean. The Fermi-type rubber seal which backs up the fusible metal seals exhibited an increase in the leak rate, which stabilized to an acceptable value.

As in the case of vessel displacements, examination is made regularly to determine if any expansion of the pipes carrying sodium has occurred. Any expansions observed have been identical to those noticed during the test period (for both direction and amplitude).

It is interesting to point out the following operation times of the main sodium pumps:

Primary pumps	{1st	12,400 hr
	{2nd	13,600 hr
Secondary pumps	{1st	16,100 hr
	{2nd	14,800 hr

Electromagnetic pumps, purification devices, purity control, cold traps, and plugging meters have also given complete satisfaction. The following points might be noted:

The cold traps have run continuously except for one test period concerning one secondary circuit. Even during this period the sodium purity remained at a satisfactory level.

A secondary-circuit cold trap recently showed signs of being nearly plugged, but this device was not subjected to the same design modifications as the others. These will be incorporated.

The auxiliary circuits, namely, for nitrogen preheating, emergency cooling, nitrogen cooling of the shield air conditioning, the secondary thermofluid, and Freon circuits, as well as the tertiary water circuits, have, as a whole, operated very well.

The pressure regulations of the different gas circuits behaved very well in spite of their complexity.

In fact, the water-treatment system was a major source of our difficulties.

Components such as fuel-handling mechanisms, as well as storage of new and irradiated subassemblies and decontamination procedures, have undergone a great number of adjustments throughout the first year of normal operation.

Flow and temperature measurements have given trouble except for the reference junctions, due to poor quality of construction. Temperature measurements at the subassembly outlets have always been available. In fact, only 5 out of 85 subas-

sembly thermocouples had to be changed. On the other hand, continuous sodium-level detectors have not given complete satisfaction.

The ionisation chambers installed on Rapsodie exhibited substantial drifts in time (up to 1 MW per week). The cause has just been determined. After some complementary tests for confirmation, we firmly hope that an improvement in the construction of the chambers will eliminate the defect.

The on-line digital-computer system gave rise to a relatively high number of spurious scrams before we were able to locate the defect which was the source of these intermittent difficulties.

We hope for a considerable reduction in failures in the future. The problem of preventive type of maintenance for the computer has not been solved yet. In fact, we are still wondering if such a maintenance is any good.

3.5. Incidents

Only four incidents prevented operation of the reactor over periods exceeding a few hours. None represented a serious problem. The first three, described below, required reactor shutdown for less than one week; the fourth required about two weeks.

The first occurred during the last half of November 1967. At the very beginning of a fuel-handling cycle, it was found impossible to rotate the main plug. Previously, some sticking had been noticed during the rotation of the plugs, particularly the small one. Even though this did not prevent fuel handling, we wondered about the possible causes, such as damage of the fusible metal seal or deposits of sodium and oxide compound between the two plugs and between the main plug and the vessel. The increased difficulties encountered during rotation revealed, in fact, that the chosen mode for cooling the plugs was not adequate. At certain angular positions and at an approximate temperature of 100°C at the level of the redans condensation and freezing of sodium (present in these regions in a vapor state) resulted. The nitrogen cooling circuits were used to heat the plugs, and the situation was thus rapidly corrected. Afterwards, the plug-cooling procedures were modified. During the last days of a reactor run, the plug temperatures are allowed to increase; this has served to reduce these difficulties considerably.

Since the incident was due to deposits of a mixture of sodium and sodium oxide, even heating did not completely eliminate the sticking. Frequent rotations during plug cooling, following preheating, insure subsequent free rotations.

The second incident occurred on December 2, 1967, and required a few days study to analyse and clearly determine its cause. Difficulties were first encountered

in positioning the "holddown tube" (which prevents the lifting of adjacent subassemblies during fuel handling). It was found that the fuel-handling gripper could not be lowered. Significant damage had occurred in two steps. Due to misalignment, the hold-down tube did not center itself as it should have (by sliding down on the head of the subassembly), because its lower extremity was jammed in a groove formed by the reference mark of the subassembly. Excessive pressure by the holddown tube twisted the head of the subassembly (see Fig. 9) and damaged the bottom of the holddown tube.

This deformation was later responsible for the accidental partial withdrawal of the reflector subassembly adjacent to one which was supposed to be withdrawn. During the plug rotation which followed, both the head of the subassembly picked up and the lower extremity of the holddown tube were twisted.

Both subassemblies with twisted heads were rapidly withdrawn, thanks to a simple makeshift fabricated for this purpose.

In order to prevent such incidents the bottom of the holddown tube was modified:

- a) to assure that it is properly centered when sliding down on the head of subassemblies, and
- b) to limit the forces exerted on the subassembly heads.

The third incident had been anticipated. It was due to a gradual plugging of the argon cover-gas lines of the primary sodium system, caused by deposits of a mixture of sodium and sodium oxide.

The initial partial plugging was detected when it

FIG. 9. The Twisted Head of the Blanket Subassembly.

was impossible to equilibrate the argon pressure in the separate vessels of the primary-sodium circuits. It should be noted that argon samples are continuously monitored (either on the reactor or on one of the intermediate exchangers) in order to detect fission products. This argon is returned to the argon collector; thus argon circuits which are plugged would show a pressure difference. The first attempt for cure involved heating the argon circuits, simultaneously creating a slight but rapid over pressure.

Following an accidental admission of some air in the argon circuit, argon flow was no longer possible in the fission product monitor. Thus the reactor had to be shut down.

Use was then made of a system which had been provided for this purpose during the design of the reactor circuits. The argon circuits were unblocked by raising the sodium level in certain tanks to overflow into the argon circuits (which for this operation were linked to assure communication between the different tanks). The operation was carried out by first raising the sodium level in the intermediate heat exchanger and then in the primary pump. In the first case, the first trial was successful. In the second case, only a very small quantity of sodium could flow after the first attempts, but a satisfactory result was obtained the next morning after a second trial. In fact, the method used does not completely wash the argon circuits. We only obtained an improvement which permitted reactor operation once again. A circuit modification has been designed in order to allow continuous sodium circulation instead of overflow and gravitational discharge.

The last incident which caused reactor shutdown, was the release of a control rod by its mechanism. The gripper which had its jaws twisted could no longer hold the head of the rod firmly. This was discovered during a startup of the reactor, when it was noticed that the count rate of the fission chamber did not vary when lifting the mechanism. The faulty mechanism was replaced by a spare one, and its twisted jaws were subsequently straightened in a cell. It seems that the failure came about gradually. Under heavy strains, due to the different expansions of various parts of the mechanism in transient thermal modes, a certain torque is applied to the jaws of the gripper. During the first few months following the installation of the mechanisms on the reactor, the opening and closing of the gripper were motor driven. It is possible that during this period and on account of defective operation of the *torque limiter*, an excessive force might have been applied to the gripper. Motor drives on the gripper have been removed.

Considering this incident, the designs of both rod

head and gripper jaws do not seem to be the best one. Since it is the same handling machine which handles both control rods and subassemblies, we would have encountered a very complicated problem in modifying the design of the rod head.

3.6. Modifications

Many modifications have been incorporated during the first year of routine operation, in addition to those carried out during the construction and test periods. The reasons justifying these modifications can be classified as follows:

- to increase the plant factor;
- to increase safety;
- to obtain additional flexibility;
- to eliminate materials of either little or no utility;
- to simplify components which work well but which are far too complex or too expensive to maintain.

Since the end of the construction a few hundred modifications have been carried out, including 134 for fuel handling and storage equipment alone. These modifications range from inexpensive (and realized in less than an hour) to those costing tens of thousands of francs. We will describe only the most important ones.

Control System

Among the improvements of the instrumentation and control system, we mention:

- suppression of the automatic control system;
- the installation of mutual-inductance sodium-level detectors;

installation of more accurate temperature sensors with shorter time constants, necessary for reactor operation and for heat-balance calculations;

a numerical display of the sodium outlet temperatures from subassemblies on a synoptic, representing the core of the reactor; the data displayed (85 subassemblies, one inlet and two outlet temperatures, 2 sodium-flow measurements) are regenerated every second by the computer;

the replacement of the circuit and panel for startup by a more elaborate circuit and panel, which includes the startup conditions for fuel handling;

the removal of flow regulation on the main pumps (good operation of the regulators had never been achieved; they were originally intended to perfect operating conditions, which already proved to be very satisfactory);

modification of the emergency power-supply systems, which were far too complex and did not possess a sufficient degree of safety of operation; this modification resulted in a reduction (from 600 to 300) in the number of relays required for this system.

Special mention should be made of the many simplifications carried out for the protective-system logic. Now it has been reduced to the simple coupling between air-blast heat exchangers and reactor core.

Ventilation System

The conditioning of the atmosphere of different sectors in the reactor building has undergone certain modifications, the most important being the nitrogen filling of the sodium-circuit cells. These cells will be filled, if not with pure nitrogen, at least with impoverished air ($O_2 \leq 5\%$). Other modifications will attempt to:

limit contamination due to leaks on the primary argon circuits (see above);

make use of the lower zone of the building as a buffer in case of upper-zone contamination;

permit a controlled release of any eventual contamination of the building atmosphere by remote control;

offer the possibility, in case of serious contamination, to change the iodine filters automatically.

The high-speed blowers on the nitrogen-preheating and emergency-cooling circuit, supplied with 200-Hz current and rotating on gas bearings, have been replaced, for simplicity, by ordinary fans. Similarly, the blowers for the nitrogen-cooling circuit of the shield have been replaced, which were far too powerful for the desired flow. Furthermore, leak tightness of the circuit will be improved by this modification.

Fuel-handling System

Concerning fuel handling, the most important event was the construction of a new fuel-handling machine (FHM) as a back up for the PFHM* and capable of performing subassembly loading and unloading. Furthermore, it may transfer subassemblies from one position to another in the reactor, keeping them under sodium during this operation, which the PFHM cannot do. This new FHM may thus unload the core and transfer subassemblies of high decay heat to the vessel periphery. After a short test on the Rapsodie mockup and some further adjustments, the FHM has been operating perfectly.

We might finally note the initial operation of the dismantling cell, in which active components may be nondestructively examined, repaired, and subjected to postmortem examination. This cell remains to be completely equipped. It has been used during failure of the control-rod mechanism, which has been described.

* PFHM: Primary Fuel Handling Machine.

	NOMINAL VALUES	'FORTISSIMO'
POWER	24 MW Th	37.4 MW Th
MAXIMUM NEUTRON FLUX	$1.9 \cdot 10^{13}$ n/cm ² s	$3 \cdot 10^{13}$ n/cm ² s
CORE		
VOLUME FISSILE ZONE ONLY	48.6 LITER	42.7 LITER
NUMBER OF FISSILE SUBASSEMBLIES	64	59
FUEL		
PELLET DIAMETER	5.57 mm	4.23 mm
HEIGHT OF FISSILE COLUMN	34 cm	32 cm
NUMBER OF PINS PER SUBASSEMBLY	37	61
VOLUME PERCENTAGE OF Pu O ₂	25 %	30 %
IN THE MIXED OXIDE Pu O ₂ UO ₂		
URANIUM ENRICHMENT	60 %	85 %
THERMAL AND COOLANT CHARACTERISTICS		
INLET SODIUM TEMPERATURE	405 °C	410 °C
OUTLET SODIUM TEMPERATURE	495 °C	530 °C
PRIMARY SODIUM FLOW	890 m ³ /h	1060 m ³ /h
MAXIMUM LINEAR POWER ON THE DRIVER FUEL	380 W/cm	400 W/cm
MAXIMUM POWER DENSITY	630 KW/l	1080 KW/l

FIG. 10. Main Characteristics of Rapsodie.

4. Core Performance

The operating conditions of the reactor are for the most part determined by the characteristics of the fuel pins. In particular, it is the cladding hot-spot temperature that determines the sodium inlet temperature. Once the hot-spot temperature is chosen (650°C for the first core), the mean temperature elevation of the sodium in the core is fixed. Normal operating conditions for the core with 66 fuel elements are shown in Fig. 10 as nominal values; also shown are the characteristics of the so-called "fortissimo" operation.

The progressive introduction of experimental fuel subassemblies "Orphee," for which the coolant flow was calculated with all the "Orphee" elements in the core, required us to lower the sodium inlet temperature to 395°C when only the first two "Orphee" subassemblies were placed; with five "Orphee" subassemblies in the core this temperature could be raised to 400°C. When the last four "Orphee" subassemblies will be loaded, the sodium inlet temperature will be raised to 405°C.

4.1. Fuel

Since the startup of the reactor, fuel subassemblies have been periodically taken out for examination in the hot cells.

EXAMINATIONS CARRIED OUT ON RAPSDIE
FUEL SUBASSEMBLIES

Number of Fuel Subassemblies	Position in the Core	Burnup (MWd/t)	Linear Rod Power (W/cm)	Integrated Flux (nvt)
11.01.0002	Row 01	150	330	0.153×10^{22}
11.01.0019	Row 01	2300	330	0.240×10^{22}
11.01.0004	Row 01	10000	330	1.02×10^{22}
11.03.0015	Row 04	13580	246	1.39×10^{22}
11.01.0018	Row 01	20160	380	2.05×10^{22}
11.01.0008	Row 01	29780	380	3.06×10^{22}

Each fuel subassembly has been subjected to following examinations (we only give a summary, without going into details):

Structural Materials:

(Hexagonal tubes, supporting structure for the fuel pins, top and bottom of the fuel element); visual examination, metrology.

Fuels Pins:

Visual examination, metrology, radiography, gamma scanning, leak test, measurement of fission gas pressure in the pin, analyses of the gas.

Oxide Pellets:

After decladding of fuel pellets, examination of the pellets, in particular their microstructure.

Cladding Material:

After removing the fuel pellets the claddings are sliced and specimens are obtained for creep testing at 650°C and a stress of 14 kg/mm².

The results obtained are as follows:

Structural Materials:

So far examination has shown nothing of great interest. All specimens looked excellent. There were no unexpected deformations. Some discoloration appeared on the stainless steel parts that were exposed to the highest temperatures during operation. We may mention that the extraction of the pin cluster has not required thus far a longitudinal cut of the hexagonal tube.

Fuel Pins:

Concerning the fuel pins, no rupture or gross deformation has yet been observed. The absence of pressure in certain fuel pins, irradiated to 10,000 MWd/t and 13,580 MWd/t, indicates very clearly a defect in the leak-tightness of the fuel pins. In particular, they do not appear to have experienced any sodium logging.

As we stated earlier, the fuel pins are subjected to a very precise metrology. Up to 20,000 MWd/t these measurements have not shown any deformation. Beyond 20,000 MWd/t, measurements of outer diameter of the pins indicate slight deformations of some, mainly for those at the outer edge of the fuel bundle (pins in direct contact with the hexagonal tube). At 30,000 MWd/t these deformations appear more clearly, but they remain still within the possible error of the measurement.

We noticed also a very slight elongation of the fuel pins, of the order of 0.1%, at 30,000 MWd/t.

Oxide Pellets:

Concerning the fuel pellets the following results can be given:

The height of the fuel column has increased, as observed by radiography. This increase became apparent from the very first examinations at 150 MWd/t, and it rapidly reached a maximum of 0.3 to 0.4%.

A gap between fuel pellet and cladding remained up to 10,000 MWd/t. From 13,000 MWd/t onwards it disappeared, along the whole length of the fuel column. But at 13,000 MWd/t the decladding of the fuel pellets is still easy. It becomes rather more troublesome at 20,000 MWd/t. At 30,000 MWd/t the oxide is strongly bonded, even after opening up the cladding longitudinally.

The structure of the oxide changes, of course, a great deal, not as a result of burnup, but as a function of linear rod power. All the fuel elements in the first hexagonal row (row 01) have pellets with a central void of $\phi \sim 0.7$ mm (except the top and bottom pellets). The void is surrounded by a zone of columnar grains with an outer diameter about half that of the fuel pellet. Then comes a zone with increased grain size and finally an outer zone that has the original grain size.

In fuel elements further away from the center of the core, the fuel pellets have neither a central void nor a zone of columnar grains.

The ratio of released fission gas to the theoretical amount of gas formed in the fuel pin varies considerably. This ratio varies with both the linear rod power and the burnup. The values obtained are in good agreement with those obtained from mixed-oxide fuel-pin irradiations in DFR.

Cladding Material:

From the outset, creep tests of cladding specimens have shown an appreciable decrease in ductility. This, however, was initially due to improper cleaning of the cladding, which caused an accelerated corrosion of the cladding, probably due to residual sodium hydroxide. Beginning with the 13,000-MWd/t fuel subassembly the procedure for sodium cleaning was improved.

We have found an important decrease of the plastic deformation of the cladding at rupture stress, indicating an embrittlement as a result of irradiation. This embrittlement does not seem to increase with time; ductility at 30,000 MWd/t roughly equals that at 20,000 MWd/t. The creep velocity did not change from that of nonirradiated specimens.

Considering these different results we thought that our fuel elements could be left in the reactor beyond

a burnup of 30,000 MWd/t. At the end of the 6th reactor cycle, on September 25, 1968, a burnup in excess of 40,000 MWd/t was reached for the central fuel elements in the Rapsodie core without incidents.

4.2. Control Rods

Postirradiation examination of seven control rods yielded various observations concerning the behavior of the boron carbide after burnup to 2% of B-10. The explanation of the phenomena observed is difficult, however, due to the fact that only a small, but variable, part of the rod is in the core and that the reactor power changed from 20 to 24 MWt.

Fragmentation of Boron Carbide

The lower boron carbide pellets fragmented very badly. The upper pellets were intact. Thermal shocks during reactor scrams are probably the cause for this phenomenon.

Production of Free Helium in the Control Rods

Helium, formed by the (n,γ) reaction in boron-10, is for the greater part retained in the carbide. With increasing temperature of the carbide a greater amount of helium is released. The helium pressures, measured in the control rods, seem to indicate a release rate of helium proportional to the burnup rate of boron-10.

Swelling of Boron Carbide

A metrology of the control rods revealed a slight increase in diameter for the rods along the boron carbide column, which had the highest burnup. We did not know whether this phenomenon should be attributed to swelling of the boron carbide or to the already mentioned phenomenon of fragmentation of the boron carbide.

5. Irradiations

5.1. Fuel Material

The irradiation of experimental fuel subassemblies up to now has essentially been carried out to verify the performance of the Phenix fuel element. A first series of irradiations—called "Aurore": 6 fuel subassemblies, each having 27 fuel pins—was started during 1967. Four fuel subassemblies were loaded September 1, 1967 and the two others December 1, 1967. The fuel pins contain fuel pellets of various density. These include pellets of the same density as that foreseen for Phenix, which is 85% of theoretical density, pellets with 80% of theoretical density, and also pellets of 95% theoretical density, but with a

hole in the center, which brings the overall pellet density to either 80% or 85% of theoretical density. Of the $6 \times 27 = 162$ fuel pins, 144 are clad with type 316 stainless steel, our reference material, and 18 pins with Hastelloy X.

The principal aim of this series of irradiations is to try out the different materials in the reference fuel pin for Phenix under realistic operating conditions:

Max rod power	430 W/cm
Max nominal cladding temp	640°C
Hot-spot temperature	700°C

We have also tried to reproduce the appropriate material stresses in the various parts of the fuel pins.

Two Aurore fuel subassemblies have recently been unloaded from the reactor for postirradiation examination after having reached a burnup of about 40,000 MWd/t.

A second series of irradiations—called “ORPHEE”—was started in 1968. The principal aims are the following:

- to irradiate fuel pins which are as similar as possible to a Phenix fuel pin; therefore these fuel pins have an incorporated lower axial blanket (“integrated” pins);

- although “Aurore” fuel pins were calculated with respect to the maximum nominal cladding temperature and the hot-spot temperature of a reference pin, the mean sodium temperatures in the Aurore elements were lower than in Phenix. It was therefore decided to increase these mean sodium temperatures. In doing so, we arrived at a hot-spot temperature of 720°C, and a sodium outlet temperature of 565°C.

We are also investigating new variations in mixed oxides and clad materials:

Oxides: Since Phenix has various percentages of plutonium oxide in its fuel pellets, it became important to try out mixed oxides up to 27% PuO₂. We decided to irradiate a mixed oxide containing 30% PuO₂.

Cladding: It appeared of interest to study an alternative stainless steel, Type 318, with which encouraging results have been obtained by the British.

To conclude, the “Orphee” irradiations comprise 10 fuel subassemblies, each containing 37 fuel pins. Two subassemblies were loaded in the reactor in July 1968, three others in August 1968. Another four are being loaded in October 1968, the last one in February 1969. Each of these fuel subassemblies are scheduled for unloading for postirradiation examination when they reach 60,000-MWd/t burnup.

5.2. Other Materials

Several irradiations of structural, shielding, and other various materials have been incorporated in Rapsodie since the end of the test period, namely, August 1967. More than 30 irradiation devices have been placed in the reactor since this period.

Structural Materials

Irradiations of structural materials, essentially stainless steel of Types 316 and 318 and various alloys such as Hastelloy X and Nimonic 80 A, were undertaken in order to obtain the following information:

- influence of irradiations and of sodium on mechanical characteristics;
- influence of impurities on postirradiation mechanical characteristics;
- swelling under strains and irradiations;
- characteristics of cladding tube assigned to experimental fuel-pin manufacture;
- behavior of stainless steel welds;
- surveillance of the steel of the Rapsodie vessel.

Shielding Materials

The present irradiations attempt comparative post-irradiation studies on dimensional variations, gas release, and thermal conductivity of the following materials:

- nuclear graphite;
- boron carbide graphite, boron carbide, boron nitride, and rare earths;
- sintered boron carbide.

Other types of irradiation experiments are performed in order to study:

- graphite behavior in sodium;
- graphite creep;
- variations of mechanical characteristics of 2% boron steel (tensile strength and Charpy impact strength).

Finally, samples of absorbing concrete, called SERCOTER concrete (serpentine, corundum, and rare earths), identical to that of the reactor shield, have been placed in this shield.

Various Materials

We are also investigating, with a certain number of devices, two methods for temperature measurements: one with the help of “Templugs,” the other with samples of oxidized zirconium-copper alloys.

6. Conclusions

The results of Rapsodie's first thirteen months of operation are rather satisfactory. The irradiation performance of the fuel pins has been successful.

Future operation of Rapsodie is currently being studied. A project under design and which is to be completed by 1970 is intended to improve Rapsodie's capability as an irradiation facility. Through minor modifications of the sodium circuits and the replacement of the actual core by a new one having fuel pins of smaller diameter but of higher enrichment, the power could be increased to the range from 35 to 40 MW with a maximum flux approaching 3×10^{15} n/cm²/s.

Discussion

Mr. Wirtz (Karlsruhe): In Fig. 10 you showed a "nominal" and a "Fortissimo" operation. What is the difference?

Mr. Denielou: The difference is mainly in flux and power density. In fast flux, the factor is about a 1.6 increase.

Mr. Ash (AI): Can you elaborate a little on the experience with your thermocouples? What experience have you had in getting off-normal indications? Do you take protective action if you get it, how do you set this trip setting, and so forth?

Mr. Denielou: Our experience with thermocouples at the outlet of fuel subassemblies has been very satisfactory. We have changed only five thermocouples out of 165 during the total operation of the reactor. We have not noted any variation in the indications; they correlate very well with each other and with the neutronic measurements. In fact, we operate with roughly 160 chances for scramming the reactor, and we haven't had any trouble coming from this fact. We had undue scrams coming from erroneous temperature indications, but they came from the control system and mainly from the fast digital computer or from the cold-junction box. The cold-junction box gave us many more problems than the thermocouples inside the reactor.

Mr. Ash: Do you trip on an absolute level, do you take differences between the channels, or what?

Mr. Denielou: We have a program of comparing at any time the sensor temperature with the one which ought to be in this place according to the flow and power.

Mr. Persiani (ANL): Do you have an explanation for the loss of fuel-expansion component of your power coefficient?

Mr. Denielou: I'm afraid it's not a fairly comprehensive and quantitative explanation. I must remind you that at the beginning of their life, our fuel elements and our fuel pins were made with nondished pellets. They expanded along the axial fiber hot temperature. Now the cladding and the pellets are bonded, so that the pellets cannot expand freely, especially as the UO₂-PuO₂ are put in a plastic

state. It's easy to understand that it disappears. The difficulty is to understand the right timing. The curve can almost be fitted with an exponential, but it's difficult to find theoretically the value of α in $-at$, because there are so many processes involved—a lot of pins with a different burnup, with different neutronic importance, etc. We have not been able to produce a consistent theoretical explanation of the curve.

Mr. Kintner (AEC): What percentage of your fuel pins do you now believe are leaking? Second, have you made any changes in the specifications for the fuel or cladding between the present core and the Fortissimo core?

Mr. Denielou: To answer the first question, a few per thousand, but less than 1%. To the second question, I know that our people are anticipating some slight change in the cladding for Fortissimo.

Mr. Kirchenmayer (AEG, Germany): In your Fortissimo core, there is only about 10% of uranium-238—that means there is almost no Doppler coefficient, almost no fast power coefficient of reactivity. Do you know how large the Doppler coefficient is and whether you expect some problems in licensing of this core?

Mr. Denielou: The answer is clear. Nowadays we have no Doppler coefficient, and if we have some, it's positive, so the situation cannot be worse. We are already licensed. Actually, people didn't care very much, because we knew that it is a characteristic of Rapsodie which is not safe. Rapsodie is an experimental reactor; this has been accepted. It wouldn't be accepted for a large fast reactor.

Mr. Rolf (ComEd): You indicated that the digital computer is part of the safety system in monitoring the thermocouples and giving your scram signal. You have redundancy in the thermocouples; do you have redundancy in the computer—do you have two computers, and do you scram on a computer malfunction?

Mr. Denielou: There has been strong discussion about duplicating computers and so on. In fact, the philosophy has been to have one computer working well, instead of two computers but the system not functioning well at all. We had some difficulty and we succeeded in repairing the computer. It was not the computer which was faulty, but you may notice that it is never the computer which is faulty. It's something in between, and in fact, in our case it was something in between the thermocouple and the computer which was faulty. It was an intermittent defect, which appeared and then disappeared; we had about eight months of tracking and detective work before finding it.

Mr. Rolf: Do you scram when the computer goes down?

Mr. Denielou: When we see that the computer is faulty, we do not scram; we reduce the power and we allow maybe one hour or two without the computer.

Mr. Wyckoff (ComEd): How do you know if the computer is faulty?

Mr. Denielou: When the internal control of the computer tells the computer to say "I am out of order."

Mr. Lagowski (APDA): What has been your experience with the replacement of thermocouples?

Mr. Denielou: The experience has been very good. We have been able without trouble to remove the thermocouple from the tiny thimble, and to insert a new one. We had feared very much about the friction coefficient after operation at temperature.

Mr. Lagowski: These are closed thimbles?

Mr. Denielou: They are closed at the end, yes.

Mr. Loewenstein (ANL): What kind of measurements do you make regularly to satisfy yourself that the reactor is behaving properly, in addition to the measurement of the reactivity from low power to full power?

Mr. Denielou: The most important is a very detailed thermal balance twice a day, sometimes more often. This is sort of a liturgy, but they have been achieving very good precision in thermal balance, in the vicinity of, let us say, 1% and probably better. So this has proven to be very efficient; for instance, in one case we experienced drift in the neutronic chambers. These drifts could be traced very carefully due to our good thermal balance; it showed that all our ionization chambers were wrong, and also that all ionization chambers of all French reactors were wrong. But the other reactors did not have a precise enough thermal balance to notice it, you see. We also have visual survey of all parts. All the data logging is done either by the computer or visually by the operators and analyzed by the shift.

Mr. Loewenstein: Do you perform any rod-drop measurements or any noise measurements?

Mr. Denielou: We have been performing mainly power-step experiments which proved very useful and, by the way, much more simple and much more useful than oscillatory experiments. But now we do not perform them. As for noise tests, it is the same—we have been making them, but now we do not perform them. We do undertake periodic measurement of the power coefficient by operating at various flows, to see that the power coefficient for the fuel does not become positive. We also check to see if there is a drift in the structural and sodium component of the reactivity coefficient. At the beginning of reactor operation, we saw a small variation of, let us say, 20% in the structural component. Now it is stabilized.

Mr. Madell (ANL): Are you contemplating changes in your fuel-element or your fuel-pellet design to improve the thermal-expansion characteristics, if the stability is not satisfactory?

Mr. Denielou: No.

Mr. Blomberg (AB Atomenergi): Have you always had a linear change of reactivity when going up from zero power to full power?

Mr. Denielou: The sodium and structural component has always been linear and is linear. But it's easier to assess it now that the fuel component is null, because in the beginning we had some kind of non-linearity that we now attribute to the extension of the fuel. My personal idea, but you know there are many discussions about it, is that as the fuel coefficient is a sum of many effects, if you have some contact effect in the expansion of one pin, you should note it on the sum of it, some kind of bending of the curve. In fact, there appears to be such a bending in the beginning, especially at high power. The coefficient tends to be higher at high power than at low power.

Mr. Zebroski (GE): We have recently measured differential axial expansion of a column of fuel pellets and cladding. We find it is very linear with power up to 16 kW/ft. Secondly, the maximum value of the differential expansion for a 6-in. column drops from 80 mils at zero irradiation to 40 mils at 17,000 MWD/T. So this would at least be a partial explanation of the coefficient changes you see. In addition to the comment, I have two questions: What other observations do you have of the fission gas composition from the pin hole leaks and what effect does it have on the reactor operation?

Mr. Denielou: We sample the argon gas of the reactor. We follow some specific noble gases and we try also to identify some other products inside the argon. But we do not know the exact composition of the gas inside the pins except by laboratory examination after that. On that I have no precise answers, except that it fits with normal data and normal effects. The effect on operation of the reactor has been the following: it has helped us in improving the argon-system tightness because, well, you see this is no joke, you know this helium testing and so on, it's done on the reactor building, but after that, you have no longer any true control. It appeared that in some places pressure tests had been made on the argon system but the leaktightness was not enough. We were able to locate this defect after the first burst, which resulted in a slight increase in the contamination of the building.

Mr. Duffield (Chairman): Anyway, you have the license, as you said.

SESSIONS ON LARGE FAST REACTOR DESIGN

SESSION LFRD-II

Thursday Afternoon, November 7, 1968

Chairman: M. Benedict
Massachusetts Institute of Technology

Secretary: I. Charak
Argonne National Laboratory

Safety Consideration in Setting the Design Criteria on the JAERI Design Studies

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(PRESENTED BY T. IWAKI)

1. Introduction

For fast reactors still in the developing stage, the general design criteria for safety have not been established. One of the main reasons for this is that the mechanism of sodium voiding in the core has not been made clear. Therefore, safety consideration may be classified into two different approaches; one of them is the so-called European approach that associates nuclear characteristics with engineering safety devices, and the other is the American approach that emphasizes inherent nuclear safety and takes the engineering reliability as its backup.

In this work the former attitude has been chosen. So a positive sodium-void effect was allowed to some extent if the negative Doppler effect can prevent fuel melting before the reactor is scrammed.

However, as is shown in the following sections, somewhat different attitudes have been taken for the sodium-void effects in the first conceptual design in 1965 and in the second conceptual design in 1966.

In the first conceptual design, the reactivity increment was estimated when a sodium void propagated radially like a concentric cylinder from the center channel. The core safety was assessed by comparing this reactivity with the allowable upper limit of $\$1.5$ which had been obtained from the reactor transient analysis.

In the second conceptual design, the aim was that the reactivity due to sodium void should not be much larger than the above-mentioned $\$1.5$ even if the geometry of the sodium void gave the most pessimistic value.

Thus, the shape of the core in the first design was a flat cylindrical shape; in the second design a hollow cylindrical (annular) shape was chosen.

In the following sections, plant safety, core characteristics, system design, and safety analysis are described for both designs.

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2. The First Design

One of the main purposes of this work was to design a core for which the internal breeding ratio (IBR) was close to unity. Not only does the high IBR have an advantage for the fuel cycle, but also it reduces the excess reactivity for burnup and thus supplements the core safety.

The sodium-void effect is shown in Fig. 1. This shows the reactivity change when the sodium void propagates radially from the central channel.

The maximum reactivity change in Fig. 1 is $0.74\% \Delta k$, and it occurs when voiding extends to 115 cm from the center of the core. The channelwise void effects are given in Table 1. Also in Table 1 are given the reactivity changes in the cases of uniform sodium loss of 50% from the core and of complete loss from 38 channels along the inner zone boundary of the core. The sodium temperature coefficient was $+2.0 \times 10^{-6}/^{\circ}\text{C}$ at 500°C .

These values were obtained by assuming that a void is only in the core part of the channels. If the void region extends to the axial blanket part, it is expected that the sodium-void effect moves in the negative direction.

Moreover, if we assume a local distribution of sodium void, the maximum reactivity change obtainable was $1.14\% \Delta k$ ($\$3.1$) by a two-dimensional calculation.

It was assumed that the Doppler coefficient was inversely proportional to the fuel temperature. The obtained Doppler coefficient is shown in Table 2. The values are tabulated both for the normal operating state and the case in which void occupied the zone up to 50 cm from the core center.

The reactivity control requirement of this core was $7\% \Delta k$ in total; details are shown in Table 3. Details of the reactivity change from cold to full power are given in Table 4. Here the cold state corresponds to 200°C for the core and the blanket, and the zero power state to 400°C .

These reactivities were controlled by twelve absorbing rods. Six were shim rods, and the rest were safety rods. As neutron absorber B_4C was used, and absorbing capacity was adjusted by changing the content of ^{10}B .

FIG. 1. Spatial Dependence of Sodium-void Effect.

TABLE 1. THE REACTIVITY INCREMENT DUE TO SODIUM LOSS (THE FIRST DESIGN)

Patterns of Sodium Loss	Reactivity Increments	
	% Δk	\$
Loss from center channel	+0.02	+0.05
Loss from central 7 channels	+0.14	+0.38
Loss from central 19 channels	+0.34	+0.92
Uniform 50% loss from core	+0.32	+0.88
Loss from 38 channels along the zone boundary	+0.024	+0.07

TABLE 2. DOPPLER CHARACTERISTICS (THE FIRST DESIGN)

Doppler Effects	Normal	Complete Sodium Loss to 50 cm from Core Center
$-T(dk/dT)$	0.00966	0.00935
dk/dT at 1500°C	$-5.4 \times 10^{-6}/^{\circ}\text{C}$	$-5.2 \times 10^{-6}/^{\circ}\text{C}$
Δk up to melting point from 1500°C	-0.616% (\$1.67)	-0.597% (\$1.62)

TABLE 3. REACTIVITY CONTROL REQUIREMENT (THE FIRST DESIGN)

	Reactivity, % Δk
Burnup	-2.1
Cold to zero power	-0.6
Zero to full power	-1.0
Safety and shutdown margin	-3.3
Total	-7.0

From the safety consideration the maximum reactivity worth of a rod was limited below \$1 for the shim rod, and below \$1.5 for the startup safety rod.

In Table 5 the reactivity worth of control rods and ^{10}B content are shown.

The reactivity worth of fuel assemblies has been calculated in order to estimate the reactivity insertion in case of misloading or fuel drop. The results depend on the position in the core and the enrichment:

i) 9.5% enrichment,

$$\Delta k = 0.10\% (\$0.27), \text{ inner zone;}$$

ii) 13% enrichment,

$$\Delta k = 0.161\% (\$0.44), \text{ inner zone;}$$

$$\Delta k = 0.135\% (\$0.367), \text{ outer zone.}$$

Other general informations about the core design are described in accompanying papers presented at THIS CONFERENCE. The safety consideration of the system design will now be described briefly.

An arrangement in the reactor containment building for the first design is indicated in Figures 2 and 3. The primary-system components, the reactor, and the intermediate heat exchangers are installed in an airtight container. Any radioactive leak from the primary system would not leak out to the public. The core is not exposed even if the vessel broke down; sodium flows into limiting spaces around the reactor vessel. The coolant nozzles of inlet and outlet are situated at a higher position than the core,

TABLE 4. DETAILS OF COLD-TO-FULL POWER REACTIVITY (THE FIRST DESIGN)

	Reactivity in % Δk	
	Cold to Zero Power	Zero to Full Power
Fuel Doppler effect	-0.34	-0.73
Core radial expansion	-0.26	-0.13
Fuel axial expansion	-0.04	-0.15
Sodium thermal expansion	+0.04	+0.02
Clad axial expansion	~0	~0
Sodium displacement by fuel radial expansion	~0	~0
Total	-0.60	-0.99

TABLE 5. CONTROL-ROD WORTH AND ^{10}B ENRICHMENT (THE FIRST DESIGN)

	Enrichment of ^{10}B	Number of Rods	Purpose	Worth, % Δk	Worth per Rod, \$
Inner zone	0.22	3	Burnup	1.05	0.95
Inner zone	0.52	3	Cold to full power and safety	1.5	1.45
Outer zone	0.16	3	Burnup	1.05	0.95
Outer zone	0.86	3	Safety	3.3	3.0

that there is no fear of the core exposure even if the intermediate heat exchanger or piping fails.

The height of the center of heat-transfer tube bundles in the intermediate heat exchangers is higher than that of the core by as much as 3 m. Upon a failure of the primary main pump, the decay heat can be removed by natural circulation and also by an auxiliary motor equipped as a backup. The sodium pressure of the secondary system is kept higher than that of the primary system, so that even if there is a leakage in an intermediate heat exchanger, radioactive sodium in the primary system cannot leak from the system.

For the accident analysis, survey calculations were carried out by using the EXCURS code in order to find the limit of the reactivity and its insertion speed that can be alleviated by the Doppler effect. The following assumptions were made in the survey calculation:

(1) The reactivity is inserted as a ramp, its value is from \$1.5 to \$5, and the insertion speed ranges from \$5/sec to \$100/sec.

(2) The sodium flow at the transient is the same as that at stationary operation. Here sodium boiling and fuel melting are not considered. (Also, flow blockage by voiding is not considered.)

(3) The scram delay time is 30 msec, the falling acceleration is 1 g, and the reactivity worth of the safety rod is 6% Δk .

(4) For feedback of reactivity only that due to the Doppler effect and the sodium temperature coefficient are taken into account. They are

$$-T \frac{dk}{dT} = 0.0095 \text{ and } \frac{dk}{dT} = 2.8 \times 10^{-6}/^{\circ}\text{C},$$

respectively.

Some of the results from the survey calculation are shown in Figures 4 and 5.

Figure 4 shows the relation between the maximum fuel temperature and the reactivity-inserting velocity at the hottest channel. Figure 5 shows the relation between the sodium outlet temperature and the reactivity-insertion velocity at the same channel.

As the temperature limit of fuel damage, 3600°C was used.

From these surveys the following conclusions were obtained:

1) The reactivity up to \$1.5 is suppressed by the Doppler effect; for a larger reactivity insertion the fuel will be damaged before scram if the inserting velocity is not below \$10/sec.

2) If it is assumed that the sodium flow is normal and subcooled boiling of sodium does not occur, the fuel rods will break down before sodium boiling oc-

FIG. 2. Reactor System Schematic for the First Design (Vertical).

FIG. 3. Reactor System Schematic of the First Design (Horizontal).

1. Core
2. Reactor Vessel
3. Intermediate Heat Exchanger
4. Containment Building

FIG. 4. Maximum Fuel Temperature at Hot Channel.

curs in the case of a reactivity-insertion accident. The temperature distribution in the fuel rod is shown in Fig. 6 in the limiting case of $\Delta k = \$1.5$ and for an insertion velocity of $\$10/\text{sec}$. The investigation was done by assuming a loss-of-coolant flow accident. Reasons for flow reduction were a pump failure and piping rupture of the primary system. Time constants of 5 and 10 sec were used.

In the case of 5 sec, the transient behaviour of power, fuel temperature, and cladding temperature are shown in Fig. 7. Conclusions were as follows:

1) When time constants for the flow reduction are 5 and 10 sec, the time from the beginning of the flow reduction to boiling are 6 and 11.5 sec, respectively.

2) The center temperature of the fuel does not change.

The average fuel temperature rises, and the power decreases by the Doppler effect. If scram is inoperable, the cladding will be ruptured either by the rise of internal gas pressure or burnout.

As to loss-of-flow accident, the investigation was carried out for cases for which the rate is reduced in steps of 100%, 75%, 50%, and 25%, respectively.

The temperature change for the case of 100% re-

FIG. 5. Coolant Outlet Temperature at Hot Channel.

$> 3600^{\circ}\text{C}$
$> 2700^{\circ}\text{C}$

FIG. 6. Temperature Distribution in the Fuel Rod. Reactivity addition: $\Delta k = \$1.5$. Reactivity insertion speed: $\$10/\text{sec}$. Doppler effect: $-T(dk/dT) = 0.0095$.

action is shown in Fig. 8. As a result, the following statements can be made:

(i) Until the flow loss reaches about 70%, boiling does not occur.

(ii) In the case of 100% loss of the flow, the power increases at the beginning because the coolant reactivity coefficient is positive. Then the power decreases by the Doppler effect due to the fuel temperature rise.

(iii) At 100% loss of flow, boiling begins from the core center. Since the sodium-void coefficient is positive in this case, the study of this phenomenon is most important for fast reactor safety.

The explosive energy of the core-meltdown accident has been calculated by the EXPLO code, which is the modified version of the AX-1 code. The maximum destructive energy was found to be equivalent to 262 kg of TNT. In the accident it was assumed that the coolant flashes out from the core and the whole core begins to melt in a moment. Also, assumptions were made that the shutdown system fails to work and the only negative-reactivity feedbacks were obtained by the Doppler effect and the core disassembly.

In the calculations, the threshold model was ap-

FIG. 7. Power, Flow Rate, and Temperature in the Loss-of-coolant Flow Accident.

FIG. 8. Power and Temperature during the Complete Loss of Flow.

plied for the fuel and $T(dk/dT)$ taken to be -0.0045 for the Doppler effect.

Other analytical constants were taken to be the same as for SEFOR.

3. The Second Design

The mechanism of sodium boiling in fuel-assembly channels and the core is not yet clear. Therefore in the second design the aim was to make the sodium coefficient as small as possible under the worst conditions.

As described in the accompanying papers, an annular core has been selected. Its outline is indicated in Fig. 9.

The dimensions of the fuel assembly are identical with those of the first design. The BeO reflectors of 40-cm thickness in the upper and the lower sides of the central region of the reactor are of the tube-in-shell type.

Contrary to the first design, the following design philosophy was taken as the basis for design criteria: even if the maximum value of the local sodium-void effect is inserted, a large-scale accident

FIG. 9. Reactor Core Arrangement of the Second Design.

1. Central Na Region
2. BeO Reflector
3. Core
4. Axial Blanket
5. Radial Blanket
6. Control Rod
7. Safety Rod

TABLE 6. SODIUM-VOID EFFECTS (THE SECOND DESIGN)

Patterns of Sodium Loss	Reactivity Increments	
	% $\Delta k/k$	\$
Total loss from core	0.219	0.59
Uniform 50% loss from core	0.124	0.34
Total loss from central hollow region	-0.109	-0.29
Maximum reactivity resulting from optimum void	0.67	1.82

does not occur in the core as long as the scram works.

Various sodium-void effects are given in Table 6. Here the maximum sodium-void effect is \$1.82. As shown in safety analysis below, there is no core damage until the reactivity-insertion velocity reaches \$18/sec.

The sodium-temperature coefficient for operation is $0.72 \times 10^{-6}/^{\circ}\text{C}$, which is about one-third of the magnitude in the first design.

The Doppler coefficients at full power and at startup are shown in Table 7.

Adding the contribution $-4.29 \times 10^{-6} \Delta k/^{\circ}\text{C}$ from the thermal expansion of structure material, the temperature coefficient has a negative value as a whole, that is, $-8.62 \times 10^{-6} \Delta k/^{\circ}\text{C}$.

The excess reactivity to be suppressed by control rods and safety rods is shown in Table 8. There are fifteen rods in all; nine are control rods and six are safety rods. The reactivity worth per control rod was so chosen that it was about \$1. The number of con-

trol rods is larger than that of the first design. However, sufficient space for control rods is provided by the hollow part of the core. The excess reactivity for burnup is larger by 1% Δk than for the first design. This is due to the smaller internal breeding ratio and the larger burnup.

A layout of main components in the containment building is given in Figures 10 and 11. Because the primary sodium circulates in the new type of steam generator and the reheater, they are put in the containment building. Since an accident by the sodium-water reaction would be much more probable than one caused by a nuclear accident, the container is a double-compartment structure. The steam generator and the reheater are installed together at the outer region of container. Moreover, the compartment is subdivided into loops, so that an accident at the outside of the reactor will not propagate into the reactor vessel.

Safety devices similar to the first design are provided for protecting the core from exposure in the various accidents.

In the core-accident analysis, the EXCURS code was used in the same way as for the first design. Therefore, the dynamical behaviour of sodium void cannot be taken into account. If we assume that the reactivity change by sodium void is added in the core while in operation, the transient characteristics can be calculated. As for core accidents, the reactivity-insertion accident and the loss-of-coolant flow ac-

TABLE 7. DOPPLER COEFFICIENTS (THE SECOND DESIGN)

dk/dT during the operation	-5.05×10^{-6}
$T(dk/dT)$ during the operation	-0.00916
Δk at the startup ^(a)	-0.0127
Δk up to the fuel melting ^(b)	-0.00677

^(a) Fuel temperature rise: $200^{\circ}\text{C} \rightarrow 1,300^{\circ}\text{C}$.

Blanket temperature rise: $200^{\circ}\text{C} \rightarrow 750^{\circ}\text{C}$.

^(b) Fuel temperature rise: $1,300^{\circ}\text{C} \rightarrow 2,700^{\circ}\text{C}$.

TABLE 8. REACTIVITY CONTROL REQUIREMENTS (THE SECOND DESIGN)

Necessities	% Δk	\$		Types	Number	% Δk	\$
Decrease due to fuel depletion	3.4	9.2	←	Control rod	9	3.5	9.5
Load variation	—	—	←				
Startup	1.7	4.6	←	Safety rod	3	3.0	8.1
Reactivity-insertion accident ^(a)	0.67	1.82	←	Safety	3	3.0	8.1

^(a) Local sodium-void effect.

Fig. 10. Primary-component Layout of the Second Design (Vertical).

1. Reactor Vessel
2. Main Coolant Pump
3. Steam Generator
4. Reheater
5. Ringheader of Steam Generator

Fig. 11. Primary-component Layout of the Second Design (Horizontal).

1. Reactor Vessel
2. Main Coolant Pump
3. Steam Generator
4. Reheater

cident were considered. The causes of the core melt-down were taken to be:

- 1) a fuel center temperature of 4000°C , and
- 2) a coolant temperature at 890°C (boiling temperature).

Various reactivity-insertion accidents are shown in Table 9. The maximum reactivity appears in the safety-rod-ejection accident, but it occurs in a cold shutdown state. As the contribution from the Doppler effect due to the fuel temperature rise is $-\$3.0$, the reaction is suppressed before the fuel is ruptured. If the coolant flow is over 50% of nominal rating, there is no sodium boiling even if scram is not initiated.

The most important accident is the sodium-void accident. If the maximum $\$1.8$ is added in a step, the transient characteristics are as given in Fig. 12. The maximum temperature distribution of sodium in the core during the transient is shown in Fig. 13. It was assumed that the scram initiates at 125% power level of rating or when the period is less than 20 sec. The fuel center temperature and the coolant outlet temperature exceed the aforementioned restrictions, as shown in Fig. 12. Boiling begins at a position one-third from the top of the core. The maximum temperature at the transient is shown in Fig. 14 for the case that the reactivity $\$1.8$ is added as a ramp.

The reactivity-insertion speed should be limited below $\$47/\text{sec}$ and $\$18/\text{sec}$ for the fuel center temperature and the coolant outlet temperature, respectively. In any other accidents, the core will not break down if the scram works.

As for loss of coolant flow the following three reasons were considered;

- (i) flow decrease by loss of the pumping power source;
- (ii) flow stoppage by piping rupture;
- (iii) flow stoppage by channel plugging.

The results of transient analysis for (i) are indicated in Fig. 15. Here it was assumed that the time constant of flow decrease is 10 sec, by taking account of pump inertia and flow inertia, and that scram starts at 90% of the rated flow.

As is seen from Fig. 15, the fuel center tempera-

TABLE 9. REACTIVITY-INSERTION ACCIDENTS
(THE SECOND DESIGN)

Identification	Reactivity Inserted, \$
Sodium-voiding accident	1.8
Control-rod-withdrawal accident	1.05
Safety-rod-withdrawal accident	2.7
Fuel-assembly-drop accident	0.4
Coolant-inlet temperature rise accident	0.5

FIG. 12. The Transient Behavior of Power and Temperature of Fuel and Sodium. The \$1.8 reactivity is added stepwise.

FIG. 13. The Axial Temperature Distribution of Sodium at the Transient Peak. The \$1.8 reactivity is added stepwise.

ture and the sodium temperature do not increase to the core-breakdown point in case of power failure.

Since four loops exist, four cases involving pipe rup-

ture—one loop failure, two loop failures, and so on—have been examined. The coolant temperature is shown in Fig. 16 for one and two loop failures. There is no problem about the fuel center temperature at the transient, and also there is no boiling until flow loss is 75%. The result when the flow stops stepwisely at the plugging channel is shown in Fig. 17. Even if scram initiates immediately after flow stoppage is detected,

FIG. 14. The Temperatures of Fuel and Sodium at the Transient Peak vs the Reactivity-insertion Speed. The \$1.8 reactivity is added rampwise.

sodium boiling begins from the central part of its channel after 0.6 sec. Since very few experimental data on channel plugging are available, there is no

FIG. 15. Transient Characteristics of Flow, Power, and Temperature in the Pump-power-loss Accident (Hot Channel).

FIG. 16. The Temperature Variation of Sodium at the Hot-channel Outlet during the Loss-of-coolant-flow Accident.

FIG. 17. The Transient Behavior of Power and Temperature of Fuel and Sodium for Hot-channel Plugging.

way to predict the extent of failure of plugged channels. However, even if plugged channels failed, the possibility of spread to the other channels is small.

The accident analysis of core meltdown was also carried out for the second design. An explosive energy of 510 kg TNT equivalent was obtained in this case. The same assumptions as for the first design were adopted in this analysis, except $T'(dk/dT) = -0.004$ for the Doppler effect.

The large energy release results because of the following reasons:

i) In an annular core, the reactivity feedback arising from the core meltdown has a positive value at the initial period.

ii) As it was assumed that the whole core was melted in a moment, the reactivity-inserting velocity became as large as $35\% \Delta k/k/\text{sec}$.

For these points, further detailed investigation would be necessary.

4. Conclusion

Different design concepts have been employed in two designs. In the first, the achievement of higher internal breeding ratio was emphasized. In the safety

analysis, sodium voiding was assumed to occur in channels. This led to a consideration in the analysis of reactivity-insertion accidents due to control-rod withdrawal or fuel drop. The maximum reactivity worth of control rods has been fixed at \$1.5. It was found, however, a reactivity addition of \$3.1 would be obtained by the local distribution of sodium void.

In the second design the design effort was concentrated upon lowering the sodium-void effect down to \$1.5. An annular core geometry was selected. Un-

fortunately, a detailed calculation gave \$1.82 for the maximum sodium-void effect, and the accident analysis showed fuel damage by the reactivity insertion.

A more elaborate method of accident analysis is being developed to advance the fast reactor safety in the JAERI.

Discussion

See Discussion following article by Yoshimi *et al.*, THIS SESSION.

Reference Concepts of the JAERI Large Fast Reactor Design Studies

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1. Introduction

Japan Atomic Energy Research Institute has carried out the design studies of two kinds of oxide-fuel, sodium-cooled, 1000-MWe fast breeder reactors from 1965 to 1967. The design studies are the results of cooperation between JAERI's staffs and engineers from Japanese atomic industries making use of calculation techniques developed by JAERI for JAERI's computer.

The principle used for the first conceptual design,⁽¹⁾ which was finished in April 1966, was to characterize the reactor as a fast breeder reactor with flexibility to adapt to the expected development of technologies of fast reactors in the future. In other words, insofar as nuclear characteristics are concerned, the core breeding ratio was to be as close to 1.0 as possible, and the positive sodium coefficient should be covered by the overall negative temperature and power coefficient of the reactor. The core composition was determined by application of the above principles. A new concept of fuel-exchange mechanism, different from conventional ones, was employed after survey and examination. The reactor mixed outlet temperature, which is most important for reactor design, is kept low enough to permit usage of AISI Type 316 stainless steel fuel-cladding tube. Nuclear safety was accounted for by requiring that even the entire loss of sodium coolant from the central 19 fuel assemblies in the core was not to make the positive reactivity more than \$1.

For the second design⁽²⁾ nuclear safety was to be warranted from the nuclear characteristics such that the accident caused by the worst sodium voidage in the core would be localized by scram. Therefore, an annular core was chosen to suppress the positive sodium-void coefficient. The selection of a special type of steam generator made it possible to lower capital investment. Not only have evaluations about the characteristics for breeding and safety been con-

sidered, but also an economical evaluation has been undertaken for the two designs.

These design studies have directly contributed to the design of the 100-MWt fast experimental reactor and the prototype fast reactor which Japan is now promoting for construction. The methods of use of the fast experimental reactor for research and development of a fast breeder reactor such as for fuel irradiation will be determined from the results of these design studies.

2. Core and Reactor System

The main geometrical size of the hexagonal fuel assembly, which is obviously most important for designing the core system, is the same for both of the first and the second designs. A fuel pin has an 80-cm-long core part with 40-cm-long upper and lower axial blanket parts. At the top it has a 60-cm-long fission gas reservoir. The fuel pins in a wrapper tube form a hexagonal fuel assembly. Their lattice pitch is 135 mm. The main difference between the first and the second designs is the overall core configuration formed by the alignment of the core assemblies. The former core is cylindrical and the latter is annular.

2.1. Core and Fuel Assembly

2.1.1. The First Design

The fuel pin has been designed so that the composition of structural material is as small as possible. Therefore, the thickness of cladding material (AISI-316) of fuel pins has been kept thin, at a 0.275-mm thickness. This thickness is very close to the limit for use as sealed fuel pins. The outer diameter of 6.4 mm is common for both of the first and the second designs, as is the fuel-pin pitch of 7.7 mm. A fuel assembly is formed by 265 fuel pins which contain mixed-oxide pellets. To reduce the thickness of the wrapper tube wall which protects the fuel assembly, it is tried to obtain stepwise pressure distribution in a wrapper tube by introducing side flow.

The compositions of a fuel pin are, from the bottom, the UO₂ lower axial blanket of 40-cm length, the PuO₂-UO₂ core portion of 80-cm, and the UO₂ upper axial blanket of 40-cm length. At the top the

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tube has a 60-cm-long vacant space serving as a gas reservoir. The volume of the reservoir was determined so that the gas pressure of fission products would not exceed 80 kg/cm² after a burnup of 100,000 MWD/T. The gas pressure was estimated on the basis of a model⁽³⁾ that 75% of gas will be released at a temperature higher than 870°C and 60% at a lower temperature. These values have been commonly used for both of the two designs.

The fuel assemblies are held by a supporting plate made of two stainless steel plates and form a core configuration of 80-cm height and 264.8 cm in diameter. To protect against floating up due to upward flow, a mechanical holddown was employed. A holding point exists at a shield plug.

Power flattening was achieved by dividing the core into two zones on the basis of the core inner breeding ratio. A high inner breeding ratio leads to a higher power density in the central part of the core at the later half of expected burnup. The power flattening was determined on the basis of such a tendency. The enrichments of PuO₂ fuel of the inner and the outer core zones are 9.5% and 13.0%, respectively.

The twelve control rods of B₄C poison are distributed in the core. Their geometry is the same as

that of the fuel assembly. Pins of the same dimensions as for the fuel assembly are used for the blanket assembly. The difference is that their meat is UO₂ instead of PuO₂-UO₂. These 235 blanket assemblies are arrayed to form a radial blanket of 40-cm thickness. Since both fuel and blanket assemblies are fixed at their top by the holddown mechanism, the inlet nozzles at the bottom have springs to accommodate their thermal expansion.

2.1.2. The Second Design

Although special attention was paid to the reduction of thickness of the cladding tube in the first design, tubes of 0.35-mm thickness were employed in the second design. Their outer diameter of 6.4 mm was kept the same as in the first design. The core configuration was determined from considerations that the effect of the sodium-void coefficient should be as small as possible under this fixed core composition.

The core of the second design is annular. Its nuclear characteristics will be described later. The core of 160-cm inner diameter and 324.4-cm outer diameter contains 390 fuel assemblies whose core length and distance across flats of a hexagonal section are the same as those of the first design. Inside the core annulus, there is no inner blanket. The corresponding region to the core height of the inside space of the core annulus is filled with sodium and some stainless steel, and the regions corresponding to the axial blankets are BeO reflectors. Since this region should be also cooled, it is filled with 112 deflector assemblies of hexagonal tube-in-shell type.

Around the outside of the core, 312 radial blanket assemblies are arranged to form a 40-cm-thick radial blanket. The blanket pins filled with UO₂ pellets containing natural uranium have a cladding thickness of 0.5 mm and an outer diameter of 10.0 mm. These Type 316 stainless steel tubes are arrayed hexagonally in a blanket assembly with a rod pitch of 11.3 mm. They are covered with a hexagonal wrapper tube.

All assemblies are held by hydraulic holddown system. Perspective views of core configurations of the first and the second designs are shown in Fig. 1.

2.2. Nuclear and Thermal Characteristics of the Core

2.2.1. The First Design

With the purpose of raising the internal breeding ratio to as large a value as possible, a nuclear parametric survey was undertaken and the design conditions chosen so as to stress this point. Although the positive sodium-void coefficient is not small enough, it

FIG. 1. Reactor Core Arrangement. (Dimensions in mm.)

was to be overcome by the total temperature and power coefficient. Thus, safety at normal operation would be sufficiently secured.

The fuel plutonium is supposed to be provided from Tokai Reactor, which is a British type of gas-cooled graphite reactor. Densities of the mixed oxide $\text{PuO}_2\text{-UO}_2$ and of UO_2 containing 0.3% depleted uranium were assumed to be 10.0 g/cm^3 . The coolant temperatures for the nuclear design were chosen to be 500°C in the core and 450°C in the blanket; the average temperature of the fuel in the core was 1300°C and that of the blanket material 750°C .

The nuclear data used were from the Russian ABN set. For one-dimensional calculation, the original 25-group set excluding the last group was used. Two-dimensional calculations were carried out with a 6-group set contracted from the ABN set.

Since the height of the core had been fixed at 80 cm, the variables in design calculations were the core composition and the volume ratio of the inner zone to the outer of the two-zone core.

Inner breeding ratio, radial peaking factor, core volume, and sodium coefficient as functions of the volume ratio of the zones were examined. After synthetic investigation of the results,⁽⁴⁾ the core whose volume ratio is nearly 1.0 was chosen as the design. The final design values are shown in Table 1.

At 500°C the sodium-temperature coefficient is $2 \times 10^{-6} \Delta k/^\circ\text{C}$ and the fuel-expansion coefficient is $-2 \times 10^{-6} \Delta k/^\circ\text{C}$. Even if the reactivity coefficient from fuel expansion is not so reliable, the Doppler coefficient of $-5.4 \times 10^{-6} \Delta k/^\circ\text{C}$ at the average fuel temperature gives the reactor a large-enough negative temperature coefficient. The reactor is controlled by 12 control and safety rods of B_4C poison enriched with ^{10}B . Half are in the inner zone of the core and half in the outer. They have a total reactivity worth of 7% to compensate burnup and power coefficient, and to keep the necessary reactivity worth for safety and control. The pressure drop across the core is 8 kg/cm^2 .

The highest hot-spot temperatures of fuel, cladding material, and channel coolant outlet are 2660°C , 650°C , and 630°C at the core initial condition, and 2670°C , 660°C , and 640°C just before the refueling, respectively.

The radial distribution of power in the core at initial state and just before refueling after it was burnt up are shown in Fig. 2.

2.2.2. The Second Design

The core height of 80 cm is the same as in the first design. The difference between fuel assemblies is the

thickness of a cladding tube. The core composition was varied by the amount that depended upon that difference. In order to have a reactor with a smaller sodium-void coefficient, an annular core without an inner blanket inside was selected after a parametric survey of nuclear characteristics. Other conditions for the nuclear calculations were the same as for the first design.

In the first design, when the sodium void is formed and spreads to the worst conceivable pattern instantaneously due to such a phenomenon as boiling, the positive reactivity of \$3 is added. On the other hand, the similar worst pattern for the core of the second design adds to the positive sodium-void coefficient only about \$1.8. However, the inner breeding ratio will be reduced from the value of 0.96 in the first design to 0.81, and the total breeding ratio from 1.44 to 1.29.

The schematic of the employed annular core is shown in Fig. 1. Six safety rods in the core are usually extracted upward. In the central sodium region, nine rods of fine control, shim control, and power-coefficient compensation are distributed. The collection of these rods has sufficient rod worth, and it prevents a large variation of power distribution due to burnup.

Since safety rods are extracted during normal operation, sodium will fill the space of the rods if there are no substitutes. The stainless steel dummy rods were used as followers for the safety rods to prevent sodium from occupying the rod spaces; they reduced the maximum sodium-void coefficient by \$0.4.

The reason why a small amount of sodium flow was introduced through the central region is not only to cool that region, but to prevent the inflow of sodium into the region at the accident if it is vacant. Otherwise, it will bring about a positive reactivity effect.

Although in the first design radial blanket pins have the same dimension as those of the core region, pins of a little thicker diameter were employed in the second design.

The annular core enables the enrichment of the fuel to be a uniform 13.0% overall. However, the fission distribution is comparatively flat and its peaking factor is 1.55; it does not become worse even after the burnup is achieved.

The design of thermal characteristics aimed at a core inlet temperature of 400°C and an outlet temperature of 600°C . Another different point from the first design is an increase of the core volume to 5,000 l, reducing the flow speed of the coolant through the core

TABLE 1. DESIGN DATA FOR THE 1000-MWe FAST BREEDER REACTOR DESIGN STUDIES

	First Design	Second Design		First Design	Second Design
1. Reactor Power			External breeding ratio	0.48	0.48
Total thermal output (MWt)	2500	2500	Simple doubling time (yr)	7.2	12.6
Core output (MWt)	2200	2250	Reactivity coefficient		
Axial blanket output (MWt)	{ 300 }	150	Doppler coefficient ($T(\partial k/\partial T)$)	-9.66×10^{-3}	-9.16×10^{-3}
Radial blanket output (MWt)		100	Sodium-void reactivity worth (%)		
2. Plant Parameters			100% core voided	—	0.219
Mixed reactor outlet temperature (°C)	550	600	Max void worth	1.114	0.672
Reactor inlet temperature (°C)	400	400	Effective delayed-neutron fraction (β_{eff}) (%)	0.37	0.37
Steam-generator outlet (Temp/Press) (°C/kg/cm ²)	500/246	541/255	Prompt-neutron lifetime (l_p) (sec)	3.5×10^{-7}	3.5×10^{-7}
3. Sodium and Steam Circuit			Peak to average power of the core		
Number of sodium loops	4	4	Axial direction	1.24	1.24
Main sodium-circulating pump	1 each	1 each	Radial direction	1.29	1.25
Number of steam generators	4	4	Total	1.60	1.55
Number of reheaters	2	4	Specific power (MW/kg-fissile)	1.14	0.96
Number of turbines	—	2	Power density (MW/l (core))	0.5	0.45
Net thermal efficiency (%)	40	43.6	7. Number of Assemblies		
4. Reactor Core			Number of core fuel assemblies	337	390
Dimensions			Number of radial blanket assemblies	224	312
Core height (cm)	80	80	Number of control rods	12	9
Core outer diameter (cm)	264.8	324.4	Number of safety rods		6
Core inner diameter (cm)	—	160	8. Fuel element (pin)		
Core volume (liter)	4400	5000	Clad outer diameter (mm)	6.4	6.4
Blanket thickness, radial and axial (cm)	40	40	Clad thickness (mm)	0.275	0.35
Reflector thickness (cm)		40	Clad inner diameter (mm)	5.85	5.7
Core Material composition			Fuel-pellet outer diameter (mm)	5.8	5.6
Fuel (PuO ₂ -UO ₂) (%)	44	41.4	Pin length		
Coolant (sodium) (%)	39	36.2	Core fuel length (cm)	80	80
Structure (SS) (%)	16	21.6	Axial blanket length (cm)	40 × 2	40 × 2
Radial blanket material composition			Gas plenum length (cm)	60	60
Fuel (UO ₂) (%)	44	55	Total pin length (cm)	220	220
Coolant (sodium) (%)	39	25	9. Core fuel assembly		
Structure (SS) (%)	16	20	Number of pins/assembly	265	265
Material composition of central sodium region			Number of tie rods	6	6
Coolant (sodium) (%)	—	92	Pin pitch (mm)	7.7	7.7
Structure (SS) (%)	—	8	Wrapper tube thickness (mm)	2.5	2.5
Reflector material composition			Spacer pad thickness (mm)	1.0	1.0
Reflector material (BeO) (%)	—	60	Assembly pitch (mm)	135	135
Coolant (sodium) (%)	—	25	Spacer pitch (mm)	100	100
Structure (SS) (%)	—	15	10. Primary coolant		
5. Fuel (PuO ₂ -UO ₂)			Coolant velocity (averaged in the core) (m/sec)	8.0	4.8
Enrichment (²³⁹ Pu + ²⁴¹ Pu/Pu + U) (%)			Coolant flow rate (ton/sec)	12.3	9.86
inner zone	9.5		Reactor pressure drop (kg/cm ²)	10	5.0
outer zone	13	13	11. Maximum temperatures		
Isotopic composition of plutonium			Fuel (°C)	2660	2556
supplied { Pu-239 (%)	77.4	77.4	Clad (°C)	650	723
{ Pu-240 (%)	17.7	17.7	Coolant core outlet (°C)	630	705
{ Pu-241 (%)	4.3	4.3	12. Power		
{ Pu-242 (%)	0.5	0.5	Fuel-pin linear heat rate		
discharged { Pu-239 (%)	74.7	70.8	Average linear heat rate (W/cm)	312	272
{ Pu-240 (%)	20.2	23.3	Maximum linear heat rate (W/cm)	500	420
{ Pu-241 (%)	4.4	4.8	Power per fuel assembly		
{ Pu-242 (%)	0.7	1.1	Average power (MW)	6.528	6.153
Critical mass ²³⁹ Pu + ²⁴¹ Pu (kg)	1930	2335	Maximum power (MW)	8.160	7.568
Pu (kg)	2350	2858	13. Burnup design basis		
6. Nuclear characteristics			Average burnup (MWD/T)	86,300	83,000
Total breeding ratio	1.44	1.29	Maximum burnup (MWD/T)	100,000	100,000
Internal breeding ratio	0.96	0.81			

to 4.8 m/sec. It reduced the pressure drop to 5.0 kg/cm², so that it became easy to use the hydraulic hold-down to fix fuel assemblies. As in the first design, a coolant flow rate through each fuel assembly was adjusted by orificing according to the power generation of each assembly to obtain effectively the high mixed outlet temperature. The flow control was designed so as to be suitable for the equilibrium core.

The average rod power per unit length of the fuel pin is 272 W/cm, and its maximum power per unit length 420 W/cm. The highest hot-spot temperatures of fuel, cladding material, and channel coolant outlet were 2556°C, 723°C, and 705°C, respectively.

2.3. Reactor Vessel and Internals

2.3.1. The First Design

The reactor vessel is made of 60-mm-thick Type 316 stainless steel, which withstands the design temperature of 550°C and design pressure of 12.5 kg/cm². To reduce the vessel thermal stress, many folds of thin stainless steel plates are lined on the inside of the reactor vessel.

The support plate of the fuel assemblies has a sandwich structure of two holed-plates that are separated by 600 mm. The diameter of the holes is 50 mm. The buoyancy due to the pressure loss along the fuels is suppressed by mechanical holddown at the upper shield plug. The positive reactivity due to bowing of fuel pins is reduced by spacer pads attached to the fuel assemblies at a little higher position than the vertical midpoint of the core. The core and blanket region is enclosed by a thick stainless steel cylinder, around which there is a fuel-storage rack for decay of spent fuels. The support plate unifies the core, blanket, and the fuel-storage rack. The reactor has a rigid structure that can rotate as one body. The rotation of the whole body of the core and the fuel-storage rack is driven by the driving axis through the upper shielding lid with the help of bearings and its supporting system.

The possibility of rotation in cooperation with the use of a fuel exchanger of the pantograph type which is attached on the upper shield lid makes it unnecessary to rotate the upper shield lid. Therefore, the rotating seal of the upper lid is, naturally, unnecessary. Attached to the upper shield lid are the drive axes of control rods, the supporting rods for hold-down, the fuel-exchange mechanism, the refueling port holes, a periscope, the drive axis for core rotation, and the like.

The primary coolant enters the fuel assemblies from the bottom of the core. The whole of the core is suspended from the top of the reactor vessel

FIG. 2. Power Distribution (First Design).

by another steel cylinder inside of the reactor vessel. The core-fixing frame is supported by the cylinder at the outer side of the bottom rim. The primary coolant from the outer circuit goes down to the bottom of the core through the space between this support cylinder and the reactor vessel. In order to inspect the inside of the reactor vessel by a periscope from the top, it is possible to lower the sodium level down to the level of the handling heads of fuel assemblies. The entrance of the primary coolant to the core vessel and the exit from it has a double coaxial structure with a double nozzle. The coolant channels extend in four directions.

2.3.2. The Second Design

The second design was based on the same principles as was the first. The second core is larger than the first by 50 cm in outer diameter because it is annular.

The fuel assemblies are supported by inserting into the holed support structure, which consists of the two plates, an upper and lower, whose thicknesses are 60 and 160 mm, respectively. The fuel assemblies are fixed in this core support structure by hydraulic holddown. The primary sodium coolant flows into the fuel assemblies from the bottom.

Since there is no fuel in the central part of the core, only 2% of the whole sodium in the core goes through there to remove heat from the upper and the lower BeO reflectors.

2.4. Fuel-handling System

2.4.1. The First Design

A reactor concept largely depends on how to remove the decay heat of spent fuels. The way employed here is through the use of a fuel-cooling rack around the core. The spent fuel in the core and the fresh fuel in the storage rack are exchanged by a fuel exchanger of the pantograph type. The access of the exchanger to the fuel at any position in the core or the storage rack is accomplished by a combination of rotation of the exchanger, extension of its arm, and rotation of the core and storage-rack assembly. The amount of fuels to be exchanged is 120 fuel assemblies over an interval of six months. The time required has been estimated as about 8 to 10 days. Spent or fresh fuel is interchanged between the storage rack and a cask car mounted upon the refueling hole through that hole.

2.4.2. The Second Design

Following the same idea as in the first design, fuel exchange is performed by utilization of the rotating core assembly and an exchanger of the pantograph type mounted on the fixed, upper shield lid. The reactor has such a structure that the central reflector assemblies are extracted directly from the core through holes in the upper shield lid. Other processes are almost the same as the first design.

3. Coolant System and Power-generating System

3.1. The First Design

The first design was confined within a preliminary investigation of the intermediate heat exchanger and the pumps of the primary coolant system to satisfy the calculated necessary characteristics of the coolant system. Therefore, the design of the power-generating system remained the step to be figured out.

The sodium of the primary system, whose total flow rate is 46.1×10^6 kg/hr, is forced upwards through the four loops to the core and blanket at 400°C. Sodium flows into the reactor vessel and flows out from it through coaxial double tubes which lead to intermediate heat exchangers. Their outer pipes are channels for inflow into the vessel and inner ones for outflow into the shell sides of the intermediate heat exchangers.

The intermediate heat exchanger has an impeller of a pump of the primary system at its bottom. A drive shaft of the pump has been connected through a central cavity to a drive motor at the top. One intermediate heat exchanger is furnished to each of four

loops. Its structure is very compact because of its connection to the reactor vessel with a coaxial double tube and employment of the pump contained inside of the heat exchanger. The thermal expansion of the primary piping will be relieved by mounting the integral type of intermediate heat exchanger, which has an internal drive motor, on suspending roller bearings.

The secondary sodium which passes the tube side of the intermediate heat exchanger goes out at 528°C and flows through four parallel steam generators. A small portion passes through two reheaters in parallel.

The steam conditions obtained by this steam-generation system have been assumed to be 245 kg/cm² and 500°C. A thermal efficiency of 40% is expected.

3.2. The Second Design

The special steam generator was employed in the second design. This simplified the heat-removal system of the plant, which consists of the systems of the primary sodium, steam generation, and electric generation.

The primary sodium from the reactor vessel at a temperature of 600°C flows out through four entrance-exit nozzles into four special steam generators. The steam generator is a tube-and-tube type, between whose channels for primary sodium and those for water or steam there is a slowly circulating sodium flow serving as the medium for thermal transmission between them. Between the vertical primary tubes for sodium and the tubes for steam system laid horizontally in zigzag lines, thin steel barriers are inserted to prevent the occasional leakage of highly pressurized steam from propagating to the adjacent region. It is a precaution to minimize the damage owing to the Na-H₂O reaction. Besides that, a cover-gas system and a purification system with radiation detectors are provided for the nearly stagnant sodium to detect the leakage from any one of both tube assemblies.

The four loops of the primary coolant are arranged axial-symmetrically around the reactor vessel. After the coolant from the reactor passes the steam generators and partially the reheaters, it flows together again and goes back to the reactor vessel by main circulating pumps. The nominal flow rate of each loop is 8.9×10^3 t/hr. About 25% flows in a branch through the reheaters. The pressure of gas which covers the sodium surface in the reactor is as high as 1.5 kg/cm², enough to send sodium to the steam generator and to keep a sufficient NPSH on the circulation pump. Employment of a hot-leg pump would make

possible to avoid this high pressure in the cover-gas system.

The steam conditions in the coolant system are supercritical ones, 246 kg/cm² and 538°C, and it generates electric power of 1147 MW at the terminals of a turbine generator.

Moreover, auxiliary facilities such as a sodium-purification system and equipment for emergency cooling have been also taken into consideration in both the first and second designs.

4. Safety

4.1. The First Design

The transient behaviors of the neutron flux in the core caused by a reactivity insertion arising from accidents were analyzed by the kinetics code EXCURS.⁽⁵⁾ The kinetic parameters used are shown in Table 1. Several cases of ramp-reactivity insertion were surveyed for insertion speeds from \$5/sec to \$100/sec. The time delay from the time that the power exceeds a previously set scram level until the scram actions starts was assumed to be 30 msec. The safety rods will fall down with the acceleration of 1 g. As the reactivities to be fed back, the Doppler coefficient and the temperature coefficient of sodium were taken into account. The Doppler coefficient used in the analysis was $T(dk/dT) = -0.0095$. The results of the calculation showed that a total reactivity insertion of \$1.5 with a rate less than about \$50/sec does not raise the temperature of the core center above 3600°C and that the core will not suffer damage according to the criteria set by the literature.⁽³⁾ For an accident due to reactivity insertion, fuel pins will be damaged before sodium boils. However, for the case of sodium voiding, the worst pattern, an estimated reactivity insertion up to \$3 will bring about the destruction of the core.

To obtain the design basis of the container, the calculation has been performed for the meltdown-recritical accident. The hypothetical situations are that the control system does not work at all and that sodium goes away instantaneously from the core and the axial blankets. The case was analyzed by the code EXPLO.⁽⁶⁾ The reactivity-insertion rate at the prompt critical was \$43/sec. The Doppler feedback coefficient was $T(dk/dT) = -0.0045$ at that time. The results of the calculation predict that the destructive energy corresponds to that of 260 kg of TNT.

2. The Second Design

The use of an annular core made it possible to lower the reactivity addition due to sodium voiding

down to about \$1.8. However, it was not possible to reach the target figure of less than \$1.5.

An analysis similar to the first design was made to investigate the transient behavior of the core for a reactivity insertion of \$1.8. It was found that step insertion brought the temperature rise to more than 4,000°C at the fuel center and that the rise of the sodium temperature above its boiling point led to core destruction. Ramp insertion of \$1.8 at a rate of \$18/sec brought the coolant to boiling, partially because of the higher outlet temperature than in the first design.

The explosive energy estimated corresponded to an equivalent of 510 kg of TNT, larger than for the first design.

An annular core has a special character in meltdown-recritical phenomenon, due to the inward dispersion of the core fragment at the very beginning of the destruction of melted assembly. This inward motion of the fragment will cause some positive reactivity additions.

5. Economics

The economics of both conceptual designs were evaluated in terms of their plant capital costs and fuel-cycle costs. The first design, which has the better breeding ratio, is naturally more advantageous as regards the fuel-cycle cost. In the second design, the simplification of the heat exchanger and the secondary circuit and the improvement of thermal efficiency are favorable factors as regards plant capital cost. It is rather difficult to convert the effect of considerations about the safety directly into the cost. In the second design, the effect of the design from the standpoint of nuclear safety would not explicitly appear in the economy.

The scale of the incredible accident as a design basis is a decisive factor for costs of buildings such as the containment or plant buildings.

To fix the capital cost, the cost of borrowed money was estimated assuming an annual interest of 8%; an annual rate of 6% was taken for depreciation and taxes in the second design. For the land and indirect construction expenses an 8% annual interest was assumed. As regards operation and maintenance cost, 0.11 Yen/kWh was added for the personnel expenses for an estimated 130 employees and for the general management expenses, following the way of Japanese electric power companies.⁽⁷⁾

To estimate direct construction costs, stationary production of reactor equipment was assumed. To calculate the cost of each component, the raw-material costs were multiplied by factors which roughly indicated the fabrication cost, the quality-control cost, inspection charge, the freight charge, and the as-

TABLE 2. COST ESTIMATE FOR REACTOR EQUIPMENT^(a)

	Material	10 ⁶ Yen/ton	First design		Second design	
			Weight (ton)	10 ⁶ Yen	Weight (ton)	10 ⁶ Yen
Control-rod system	Type 316 and others	60	10	600	13	780
Upper core mechanism	Type 316	4	50	200	50	200
Fuel exchanger	Type 316	8	20	160	20	160
Upper shield plug	Type 304	3	320	960	350	1,050
	Steel	0.25	320	80	380	96
	Borated graphite	1	160	160	190	190
Reactor vessel	Type 316	3.5	200	700	200	700
Core support plates	Type 316	4	65	260	80	320
Inner cylinder A	Type 316	2.5	85	213	110	275
Inner cylinder B	Type 304	2	20	40	24	48
Thermal baffles	Type 304	2.5	40	100	45	112
Shielding graphite	Reactor grade	0.5	2,000	1,000	1,700	850
Thermal shield	Asbestos	0.25	300	75	100	25
Sodium	Reactor grade	0.5	1,000	500	620	310
Others	—	—	—	952	—	884
Sum				6,000		6,000

^(a) Drawings of the reactor components are to be found in J. Takahashi *et al.*, THIS CONFERENCE.

TABLE 3. ESTIMATE OF TOTAL CONSTRUCTION COST (10⁶ YEN)

	First Design	Second Design
Site and buildings cost	50	15
Direct construction cost	470	436
Indirect construction cost	117	109
Total	637	560

TABLE 4. ESTIMATE OF CAPITAL COST (YEN/kWh) FOR THE SECOND DESIGN

$\frac{\text{Total construction cost} \times 0.14}{\text{Total power generated per year}}$	1.02
$\frac{\text{Site and indirect construction cost} \times 0.08}{\text{Total power generated per year}} + \frac{\text{Direct construction cost} \times 0.14}{\text{Total power generated per year}}$	0.93

TABLE 5. ESTIMATE OF FUEL-CYCLE COST (YEN/kWh)

	First Design	Second Design
Cost of fissile plutonium inventory	0.066	0.088
Cost of core-fuel fabrication	0.168	0.185
Reprocessing cost	0.084	0.087
Cost of UO ₂ inventory	0.003	0.004
Cost of blanket-fuel fabrication	0.062	0.063
Reprocessing cost	0.013	0.019
Plutonium credit	-0.147	-0.107
Total	0.249	0.339

sembling cost, taking into account accuracy, heat treatment, or the kind of the final inspection. The costs of conventional products or equipment were taken for fast reactor constituents other than reactor equipment.

Table 2 shows the costs for reactor equipment. Total construction costs for both designs are shown in Table 3. In the calculation of capital cost, a load factor of 80% per year is assumed. If 12% interest is used for the total construction cost of the first design, 1.11 yen per kWh for the capital cost is obtained. Using the abovementioned interest charges for the second design, the capital cost estimate is indicated in Table 4.

Fuel-cycle costs are shown in Table 5 together with their cost parameters. As can be seen, the fuel-cycle cost for the first design is better than for the second design.

Total power-generating costs are evaluated by summing all the costs mentioned above. From the first design, 1.47 Yen/kWh and from the second design 1.38 Yen/kWh are obtained, respectively.

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Discussion

See Discussion following article by Yoshimi *et al.*, THIS SESSION.

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Special Engineering Problems Involved in the JAERI Design Studies

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1. Introduction

Miscellaneous engineering problems were found in our studies of two conceptual designs of a 1000-MWe fast reactor plant. Since fast reactor technology is a very broad field, all of its problems are not always clear, but we may say at least that the essential points of the problems have been clarified. We would like to discuss the specific problems which are relevant directly to our works and the conceptions chosen.

2. Core Designs

In the first design, an attempt was made to obtain an internal breeding ratio near to 1. It is desirable to keep the volume fraction of the fuel as large as possible. It was chosen as 0.44. As a result, there appeared difficulties in thermal and hydraulic designs.

A powerful way to evade this difficulty is through the use of tube-in-shell type fuel, for which volume fraction can be made large enough. Since we have not exhaustively studied the thermal and structural properties of such fuel, this fuel concept was not adopted for this design. However, this type of fuel has many advantages as regards the nuclear aspects of the reactor, such as good breeding ratio and less positive sodium-void effect, and its development is highly desirable.

For the same reasons as mentioned above, relatively thin fuel cladding (0.275-mm-thick Type 316 stainless steel) was used in the first design. Although this fuel cladding is proved by calculation to be strong enough for the operating condition of the first design, it would hardly stand up under advanced operating conditions, such as 100,000 MWD/T burnup and 700°C for maximum fuel-cladding surface temperature. Information on corrosion effect, mechanical properties, and effect of fast-neutron irradiation on fuel cladding material under a high-temperature sodium atmosphere is limited at present. The thinner fuel cladding may be justified through experimental

investigations of these effects and developments of new materials for fuel cladding or for vented fuel in future.

In the second design, several types of reactor core were studied from the view point of safety. An annular core was chosen. It was shown that sodium-void effect, Doppler effect, the internal breeding ratio, and the power peaking factor are rather good for the annular core. It is worthy of note that the hollow part is used for controlling the reactor and that it is also very convenient to employ the rotating core concept. However, there are some problems concerning the annular core.

First of all, there is a question about the nuclear calculations. In a system of such a complex shape as the annular core, the calculation of reactivity coefficient is largely affected by the evaluation of the spectrum, which is to be studied in future. Furthermore, calculation of power distribution in the inner zone of the annular core is considerably troublesome because it is affected by the configuration of control rods. Calculation of power distribution taking into account the configuration of control rods seems to call for the three-dimensional calculations. These are future problems.

There is a question about thermal design. The gradient of the radial power at the inner side of the core is abrupt, which may impose differential thermal stresses on the both sides of the wrapper tube of that part. Furthermore, the power level at the inner side of the core varies strongly in the process of burnup, which makes the power control of the reactor difficult to a certain extent. It is desirable to study these effects.

3. Examination of Reactor Type (Tank Type vs Loop Type)

The so-called loop type is employed in both the first and second designs. One of the reasons for this in the second design is that there is no possibility to employ the tank type, because special-type steam generators are used to eliminate the intermediate heat exchangers. However, the tank type seems to have several advantages; a preliminary examination of these two types was performed. The arrangement of

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components in the containment building is shown in Fig. 1.

The tank which contains primary sodium is rather large compared with that in the loop type, and there are some uncertainties in its construction and support. Unfortunately, since the primary pump and the intermediate heat exchanger are set up near the core, compact shieldings for neutron radiation is necessary. On the other hand, piping for the primary system of the tank-type design is not outside of the sodium tank; hence, it is safer in case of breakdown of piping of the primary system than for the loop-type design.

It is also possible to evade the problem of stress concentration at the joints of the piping with the reactor vessel. This is one of the most critical problems in the loop-type design. Absence of piping for the primary system has another advantage, that is, although the piping in the loop-type design is sufficiently long so that thermal expansion of the piping of the primary system is no problem, there is no such problem with the tank type, so it is possible to make a compact arrangement.

Now, from the above discussions, it seems that the so-called tank-type design has several advantageous features from the view of safety. Therefore, it would be desirable to investigate such problems as manufacturing of the tank and the compact shielding to be used in the tank for this type of the reactor.

4. Fuel-handling System and Rotating Core

In both designs, the concept of a rotating core is used for the fuel-handling system. This concept was chosen because it does not require the removal or rotation of the upper shielding plug. In the case of a relatively small reactor, such as an experimental reactor, it is not so difficult to raise up or to rotate the upper shielding plug, because it has a small diameter and is not too heavy. However, for a large reactor, many problems are connected with rotation of the shielding plug. Moreover, in the case of such a tall steam generator as in the second design, the pressure of the cover gas in the reactor vessel becomes so high that the problem of sealing the gas from the external atmosphere may cause some engineering problems. These problems may be solved if the primary sodium pump is installed in the hot leg of the cooling system, which requires that the pump be operable under liquid sodium up to 600°C.

Of course, since the core itself rotates in sodium in the rotating-core model, a mechanical problem such as that in the rotating-plug method still remains. But it is advantageous that an external gas seal such as a liquid-metal dip seal is not required.

FIG. 1. Reactor Building Elevation for Tank-type Design.

1. Core
2. Shielding plug
3. Fuel-handling machine
4. Fuel-storage rack
5. Refueling machine
6. Intermediate heat exchanger
7. Reactor vessel
8. Containment

One of the most important engineering problems in connection with the rotating core is the mechanism of rotation in sodium. During operation of the reactor, the rotating part is in sodium at 400°C, so the corrosion of balls and races by sodium or the deposition of sodium compound may obstruct the rotation. Now, in JAERI an examination for testing the feasibility of ball bearings in high-temperature sodium is in progress. The basic data with respect to ball bearings in the high-temperature sodium are being accumulated. A tendency to galling or self-welding has been observed.

Another method of rotating the core may be applicable to the second design. The whole core assembly hangs from the upper shielding plug through a drive shaft that passes through the center of the hollow region of the annular core, being driven by a driving device located above the upper shielding plug, not in the liquid sodium.

The second problem is related to the fuel-handling mechanism. In contrast to the fuel-handling machine combined with double rotating plugs which permits movement only in the vertical direction, the rotating

FIG. 2. Steam Generator.

1. Safety valve	9. Vessel
2. Rupture disc	10. Primary sodium tubes
3. Primary sodium inlet	11. Intermediate sodium inlet
4. Intermediate sodium overflow	12. Feed water inlet
5. Steam outlet	13. Intermediate sodium drains
6. Intermediate sodium inlet	14. Primary sodium outlet
7. Steam inlet (temporary)	15. Argon inlet and outlet
8. Steam outlet (temporary)	

core in this design as well as the fuel-handling machine combined with the single rotating plug requires the method of offset or pantograph. The mechanism of the latter is more complicated, and it is necessary to develop a highly reliable one. A solution to the problem may be to combine the rotating core with a rotating plug of small diameter.

5. Steam Generator

In the second design, we have employed SHTT (Sodium Heated Tube and Tube Steam Generator) in which the intermediate heat exchanger is eliminated and heat exchange through the stagnant sodium layer in the steam generator is performed between the primary sodium and feed water. Although we expected cost savings of components by removing the intermediate heat exchanger, the secondary pump, and pipings of the secondary cooling system, the cost savings as a whole may not be as much as expected because the steam generator itself becomes larger and relatively complicated. This is due to the fact that safety provisions are especially considered in this design. However, this type of the steam generator has such advantages that power control is possible to a certain extent by changing sodium level. Problems with this type of the steam generator are as follows:

1) As shown in Fig. 2, it becomes larger than a conventional steam generator. For this reason, the sodium level in the steam generator is very high compared with that in the core, and the pressure of cover gas in the reactor vessel must be high enough so that sealing of cover gas becomes a problem.

2) The second problem is concerned with the housing of the steam generator. Since activated primary sodium flows through the steam generator, the four steam generators and four reheaters are put inside the containment. Further, to protect against a possible sodium-water reaction, we have placed a strong barrier between the reactor vessel and the steam generators and the reheaters.

For these reasons the cost rises as the size of the containment becomes large.

Generally, as the cost of the steam generator is a main part of the cost of the sodium-cooled fast reactor and also because the type of the steam generator has a large influence on the other parts of the plant, it is desirable to determine the type of the steam generator to be used by careful consideration of operation, reliability, and economy of a whole plant.

6. Control and Instrumentation

We have not introduced a new idea about control and the instrumentation in these designs.

It is enough only to take account of some special characteristics (fast neutrons, sodium atmosphere at the high temperature, etc.) of the fast reactor in addition to the customary practice and to construct a highly reliable system. Here careful consideration is necessary for the following points:

1) There are some problems about nuclear instrumentation. In these designs, spent fuel assemblies are cooled in a storage rack placed on the outer circumference of the core. In these cases, the out-of-core neutron detectors can be influenced by the presence of fuel assemblies, kinds of fuel assemblies, and the like. Therefore, the arrangement of neutron detectors must be carefully examined.

2) It is also necessary to develop a suitable in-core instrumentation so as to obtain useful information for effective operation of the reactor and to keep the reactor running safely. Suppose one or more fuel channels are blocked. Sodium boiling can occur in a blocked channel, resulting in failure of the fuel assembly. Even if the maximum sodium-void effect can be kept extremely small (even negative), the core can be damaged severely as the result of sodium boiling itself. Therefore, development of an instrumentation system that detects the channel blockage or the outbreak of sodium boiling in the fuel assembly at an early stage is desirable. The system should initiate reactor scram so as to prevent propagation of sodium boiling to surrounding fuel assemblies. Since

these detectors are put in the core, they should be able to work in high-temperature sodium with high reliability. The development of lead wire by which the signal of detectors is taken out of the core is very important.

3) As to process instrumentation, it is very important to develop highly reliable instrumentation that can be used in the high-temperature sodium environment. We are vigorously pursuing the development of sodium technology.

4) No new devices for the control system of a plant have been used in these designs since they are conceived as base-load plants. However, problems about the control system are considered to be very important for a large-scale fast reactor plant. These problems require further investigation.

7. Conclusion

Several problems involved in the JAERI's design studies are discussed. They are surely important problems at present, but some of them may be solved in future as experiences with fast breeder reactor plant are accumulated and the general technologies advance.

Discussion

See Discussion following article by Yoshimi *et al.*, THIS SESSION.

Reasons for Choice of Major Mechanical Designs in the JAERI Design Studies

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1. Introduction

In this paper are described the characteristics and the reasons for choice for mechanical designs of the reactor systems in two designs of the 1000-MWe sodium-cooled fast breeder reactor that have been studied in JAERI.

1.1. Type of Reactor

In our first design we employed a loop type in which a reactor vessel, heat exchangers, and pumps are placed separately. There is a reason why a loop type needs not so much sodium and cover gas (argon) compared with a tank type in which the intermediate heat exchangers and the primary pumps are submerged in the primary sodium. Moreover, the reactor proper, the intermediate heat exchanger, and the pump are easily designed and manufactured separately in the loop type.

In the second design an intermediate heat exchanger and a steam generator were unified.

The two designs of the reactor system are shown in Fig. 1 and Fig. 2.

1.2. Fuel-handling System

In order to change fuel, the whole reactor core and blanket with a storage rack are able to rotate, instead of having to lift or rotate a shield plug for refueling. By rotation of the refueling equipment, whole assemblies of the core and blanket can be exchanged for new assemblies in the storage rack. The spent fuel assemblies handled per shutdown are all cooled in this storage rack until the next exchange time; then they are taken out and replaced by new assemblies.

We have selected the rotating-core concept for refueling because we considered that it was better than the other methods of lifting or rotating of the shield plug if we took into account a seal for the cover gas.

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The reactor core diameter in the second design is larger than that in the first by as much as about 50 cm. But there is no difficulty with this method.

1.3. Heat Exchanger

1.3.1. The First Design

The primary sodium system and the secondary sodium system each have 4 circuits; there is an intermediate heat exchanger between the primary and secondary system in each circuit. The intermediate heat exchanger is a shell-and-tube type. The primary sodium flows through the shells and the secondary sodium through the tubes. The primary pump is contained in the intermediate heat exchanger, as shown in Fig. 1. Piping of the primary sodium system could be simplified as the intermediate heat exchangers and the reactor vessel are connected directly with double concentric tubes.

1.3.2. The Second Design

There are four circuits in the primary sodium system, and sodium flows into each steam generator and reheater from a reactor vessel. Sodium again is sent back to the reactor vessel by the primary pump placed on the outlet line of each steam generator.

The heat of the primary sodium is transferred to a steam system through the nearly stagnant intermediate sodium (the secondary sodium) in the steam generator and the reheater. Therefore the cooling system is more simple than that of a conventional sodium-cooled reactor.

2. Reactor Plant

2.1. Core and Blanket

2.1.1. The First Design

The horizontal cross section of the core and the blanket of the first design is shown in Fig. 3. The core has two zones (i.e., Zone I and Zone II) in order to decrease the radial power peaking. Each zone has $\text{PuO}_2\text{-UO}_2$ fuel of 9.5% and 13% enrichment, respectively. The number of assemblies is 180 for the former, and 180 for the latter. The radial blanket is composed of 234 assemblies around the

core to a thickness of about 40 cm. Here depleted UO_2 is used. The unit arrangement of assemblies in the core and the blanket is a triangle with a 135-mm pitch. The height of the core is 800 mm, and there are axial blankets of 400 mm in the upper and the lower sides of the core. Twelve control rods are arranged in the core; B_4C is used as an absorber.

2.1.2. The Second Design

In Fig. 4 the core and the blanket arrangements of the second design are shown. The inner diameter of the core is 1600 mm, the height is 800 mm, and the major part of this central region is filled with sodium. There are reflectors, each of 400-mm height, in the upper and the lower sides of this central region. This central region consists of 112 assemblies and is composed of wrapper tubes made from Type 316 stainless steel, the rest filled with sodium. In the central region there are 9 control rods. Control rods may move in guide tubes.

① CONTAINMENT BUILDING	⑤ CONTROL ROD DRIVE EQUIPMENT	⑨ PRIMARY PUMP
② ROTATING BUILDING CRANE	⑥ SHIELD PLUG	⑩ STEAM GENERATOR
③ CORE	⑦ REACTOR VESSEL	⑪ REHEATER
④ FUEL EXCHANGE EQUIPMENT	⑧ MOTOR OF PRIMARY PUMP	

FIG. 1. Reactor System (First Design).

① CONTAINMENT BUILDING	④ FUEL EXCHANGE EQUIPMENT	⑦ REACTOR VESSEL
② ROTATING CRANE	⑤ CONTROL ROD DRIVE EQUIPMENT	⑧ MOTOR OF PRIMARY PUMP
③ CORE	⑥ SHIELD PLUG	⑨ INTERMEDIATE HEAT EXCHANGER

FIG. 2. Reactor System (Second Design).

FIG. 3. Core and Blanket Arrangement (First Design).

Number of Subassemblies:

Core: 337 (Zone I, 157; Zone II, 180)

Blanket: 234

Control Rod: 12 (Pitch: 135 mm)

Fig. 4. Core and Blanket Arrangement (Second Design).

A reflector is of the tube-in-shell type, each filled with BeO in a wrapper tube. Stainless steel tubes serve as passages for the cooling sodium. Around the central sodium region there is an annular core of 800-mm radial thickness and of 800-mm height. There are axial blankets, each of 400-mm height, in the upper and the lower sides of the annular core. These blankets consist of 390 assemblies, and each assembly has a hexagonal wrapper tube. The spacer pads, 1 mm thick at the outside of a wrapper tube, are in contact with neighbouring assemblies.

There are six safety rods in the core, arranged in the same way as are the control rods. Also there are radial blanket assemblies of 400-mm thickness and of 1600-mm height around the core. They consist of 312 assemblies, and each has almost the same figure as that of a core assembly.

2.2. Core and Blanket Support Structure

In Fig. 5 the vertical cross section for the 2nd design is shown.

Assemblies in the core, the blanket, and the storage rack are supported by the lower support plate, which is made of Type 316 stainless steel and has many holes for insertion of assemblies. The support plate is composed of two stainless steel plates welded with many columns and is of 600-mm. height. In order to provide a stable support for the assemblies in the core and the blanket, the insert holes of a support plate must be dimensioned with high accuracy. A storage rack for the assemblies is provided around the radial blanket. A stainless steel cylinder, of 100-mm thickness, between the radial blanket and the storage rack is welded to the support plate. The cylinder forms the outer circumference frame; it also serves as a shield for spent assemblies in the storage rack from neutrons from the core and the blanket.

A cylinder around the outer circumference of the storage rack forms the outer frame; the inner part can rotate as one body. Moreover, there is a fixed cylinder around the outer circumference of the outer frame, forming the fixed frame. The lower part of

Fig. 5. Vertical Cross Section of Reactor (Second Design).

the fixed frame bends to the inside. On it there are balls, on which rests the support plate, so that the core, blanket, and the storage rack can rotate as one body. The balls are in the inlet region of the core-cooling sodium and attain a temperature of about 400°C during reactor operation. But the load on the balls is small because the assemblies and their support plate receive an upward-directed force by sodium flow. The temperature of sodium is about 200°C when balls are rolling with a load for refueling.

The gear to rotate assemblies and their support plate is attached on the upper end of the outer frame of the storage rack.

2.3. Fuel Handling

Fuel handling is divided roughly into two operations: one is the exchange of new and spent fuels between the core and the blanket and the storage rack around them; the other is the reloading of new and spent fuels between the storage rack and the shipping cask on the reactor. The fuel exchange and the fuel reloading are performed after the reactor is shutdown.

In the second design there are reflectors inside of the core; these reflector assemblies are taken in and out from the upper port, except for a part of the outer circumference. These three operations can be done parallel to some extent.

The exchange between assemblies in the core and the blanket and assemblies in the storage rack is performed by the following procedure: the one-body rotation of the core with the blanket and the storage rack and the rotation of the fuel-exchange equipment are performed; then the lifting up and the lifting down of an assembly are done (see Fig. 5). The core, the blanket, and the storage rack are locked tightly together during reactor operation. However, the lock is taken off when the fuel is exchanged, and they are rotated by a drive power unit on the shield plug. When the fuel is exchanged, the decay heat of the fuel in the core part is removed by an auxiliary cooling system.

The whole fuel-exchange equipment rotates around the main axis. In cooperation with the above rotation and the rotation of the core with the blanket and the storage rack, spent assemblies in the core and the blanket can be exchanged for new assemblies in the storage rack. Two sets of fuel-exchange equipment are available so that the efficiency of the fuel exchange is high. During refueling the level of the core-cooling sodium is lowered until it reaches the upper end of the core assemblies.

The reloading of new-spent assemblies between the storage rack and the fuel-shipping cask on the reactor may be done by the following procedure: the spent

TABLE 1. CHARACTERISTICS OF REFUELING METHOD
(SECOND DESIGN)

1. Refueling Interval	12 months (or 6 months)
2. Type of refueling method	Remote; automatic
3. Assumed plant-load factor	80%
4. Number of control rods and safety rods	9 and 6, respectively
5. Number of core assemblies handled per shutdown	119
6. Number of blanket assemblies handled per shutdown	78
7. Total assemblies handled per shutdown	197
8. Fuel, blanket, etc., storage capacity	220
9. Is operation of mechanisms in sodium required? What kind?	Yes Ball and slide bearings; latching device; screw; gears; chain
10. Shield plug	Removal; rotating is not required
11. Core, blanket, and storage rack	Rotating and indexing is required as a whole
12. Sodium level during refueling	Lowered and adjusted Sodium
13. Fuel coolant during transfer from core to storage rack	
14. Fuel coolant during interim transfer operations from storage rack to shipping cask	Argon gas
15. Core-assembly decay heat in storage	66 kW at 24 hr after shutdown; 7 kW at 6 months after shutdown
16. Fuel-reprocessing location	Off site
17. Fuel cleaning required	Yes; dry and wet steam, then air; on site
18. Method of fuel shipment for reprocessing	Air-filled steel cask
19. Replacement fuel preheat	200°C; out-of-core

assembly is carried just under the fuel port of the shield plug by rotating the storage rack; they are then reloaded between the storage rack and the cask on the port.

In the central region inside the core, there are assemblies that have the sodium region at the center, and the reflector region in the upper and the lower sides of that sodium. The reloading of these assemblies is performed by the following procedure: an assembly to be reloaded is carried just under the reflector reloading port of the shield plug by rotating the core; then the assembly is lifted to the cask on the port. When the assembly is lifted, sodium in the assembly flows downward.

The items relative to the fuel handling in the second design are shown in Table 1.

FIG. 6. Fuel Subassembly (Second Design).

TABLE 2. DESIGN DATA FOR CORE ASSEMBLIES

Items	First Design	Second Design
Core Fuel Pin		
Fuel-pin-clad OD, mm	6.4	6.4
Clad thickness, mm	0.275	0.35
Pellet OD, mm	5.8	5.6
Active core height, mm	800	800
Active blanket height, mm	400×2	400×2
Gas reservoir height, mm	600	600
Core Assemblies		
Distance across flats, mm	133	133
Triangular pitch of pins, mm	7.7	7.7
Pins per assembly	265	265
Assemblies in region	337	390
Total pins in region	89,305	103,350
Zone	I	II
Fuel enrichment, %	9.5	13
Fuel material	Oxide	Oxide
Fuel density, %	90	90
Fuel density, gm/cc	10.0	10.0

3. Fuel

3.1. Fuel Assembly

As regards the figures of core assemblies, only the outer diameters of fuel pellets, the clad thickness, and the figure of the inlet nozzle are different for the two

designs. The core assemblies of the second design are shown in Fig. 6. The design data of the core assemblies for both designs are given in Table 2.

The length of a fuel pin is about 220 cm; from the lower end, in order, it consists of the 40-cm lower-blanket fuel (depleted uranium), the 80-cm core fuel ($\text{PuO}_2\text{-UO}_2$), the 40-cm upper-blanket fuel (depleted uranium), and the 60-cm gas reservoir. The outer diameter of a fuel pin is 6.4 mm in both designs. The clad thickness was changed from 0.275 mm in the first design to 0.35 mm in the second design; as a result, the outer diameter of a fuel pellet in the second design was changed to 5.6 mm from the 5.8 mm in the first design. Cladding material is Type 316 stainless steel.

There are 265 fuel pins, placed on a triangular pitch of 7.7 mm, each inserted in a hexagonal wrapper tube. In each corner of a hexagonal wrapper tube there are tie rods, and tie rods hold the grid spacers. The grids fix the fuel pins horizontally, keeping the intervals; 22 grids are arranged with a pitch of 10 cm in an assembly.

To prevent a floating of core assemblies by the flow of sodium, a mechanical holddown method is used in the first design; a hydraulic holddown method is used in the second design.

The nozzle, wrapper tube, and handling head of core assemblies is made of Type 316 stainless steel.

The radial blanket assemblies are almost the same

the core assemblies, except that fuel pellets are made of an oxide of depleted uranium. The outer diameter of a fuel pin is 10 mm, and the pins are arranged with a triangular pitch of 11.3 mm.

3.2. Cladding Strength

In the first design, the sum of the thermal stress S_t and the internal pressure stress S_p produced by fission product gas is limited within the allowable stress. The swelling of a fuel pellet may close a gap between a pellet and a cladding tube. We employed

FIG. 7. Radial Distribution of Power Density (Second Design).

FIG. 8. Longitudinal Temperature Distribution in Hottest Channel (Second Design).

TABLE 3. THERMAL CHARACTERISTICS OF CORE (SECOND DESIGN)

Average power of core pin per unit length, W/cm	272
Maximum power of core pin per unit length, W/cm	420
Average power of core pin, kW	23.2
Maximum power of core pin, kW	29.0
Coolant temperature	
at reactor inlet, °C	400
at reactor outlet (average), °C	600
at core outlet (maximum), °C	705*
Axial peaking factor	1.24
Radial peaking factor	1.25
Total peaking factor	1.55
Power ratio for hottest to average channel	1.23
Average channel power, kW	6.153
Hottest channel power, kW	7.568
Maximum fuel temperature, °C	2,556*
Maximum cladding temperature, °C	723*
Coolant velocity (average in core), m/sec	4.8
Total coolant flow, ton/sec	9.86
Coolant pressure drop through core, kg/cm ²	5.0

* Considering hot-spot factor

TABLE 4. POWER RATIO AND FLOW RATIO (SECOND DESIGN)

Region	Power Ratio		Flow Ratio
	Initial	End of Run	
Core and axial blanket	0.96	0.95	0.88
Radial blanket	0.04	0.05	0.06
Shim and safety rod	0	0	0.02
Center and reflector	0	0	0.02
Storage rack, etc.	0	0	0.02
Total	1.00	1.00	1.00

$\frac{3}{4}$ of the rupture stress as an allowable stress, the rupture stress value employed being obtained by reference to Notification No. 272 of the Ministry of International Trade and Industry (similar to ASME Boiler and Pressure Vessel Code, Section VIII).

After a strength analysis of a cladding tube, we employed the following design criteria for the second design: (a) a thermal stress ratchet must not be produced; (b) creep rate must be limited to within 1.5%.

Condition (b) was chosen because creep strength is more doubtful than the yield strength at the temperature at which a cladding is used. The following two equations are used as conditional equations:

$$S_p + 1/4 S_t < S_y', \quad (3.1)$$

$$S_t < 2S_y', \quad (3.2)$$

where S_y' is the allowable stress, 790 kg/cm², when the

creep rate is 1.5% per 16,500 hr of use. The thermal stress of this cladding tube at the conditions of use is 710 kg/cm², and the internal pressure stress is 588 kg/cm², for a burnup of 80,000 MWD/t. Thus both satisfy the above equations.

3.3. Core Thermal Characteristics

In this section and the next section we shall treat the thermal characteristics and the flow characteris-

TABLE 5. COOLANT PRESSURE DROP THROUGH CORE (HOTTEST CHANNEL), kg/cm² (SECOND DESIGN)

Nozzle inlet	0.06
Nozzle surface (lower)	0.05
Nozzle surface (upper)	0.19
Pin-bundle inlet	0.10
Pin surface (no grid)	1.64
Pin surface (with grid)	1.91
Grid inlet and outlet	0.89
Pin-bundle outlet	0.15
Handling head	0.03
Total	5.00

tics of the core in the second design. In the process of burnup, plutonium is accumulated in the core, so that the power ratios in the regions are 90% for the core, 6% for the axial blanket, and 4% for the radial blanket at the initial operation; however, the power ratio in the blanket increases as much as 2% during one refueling cycle.

The radial power density distribution at the core center, at which the power is highest, is shown in Fig. 7. As the excess reactivity is suppressed by inserting control rods inside the core at the initial operation, the power in neighbouring regions is lowered. But during burnup control rods are pulled up; the power is thereby raised. At the same time accumulation of plutonium in the radial blanket is increased, and the power in the blanket and in the core near there increases relatively. Based on these facts the radial power peaking factor is 1.25 at the initial operation; however, it becomes smaller during burnup.

The axial power distribution of the core can be approximated roughly by a cosine curve, taking into account a reflector saving except the circumference. Although the axial peaking factor is 1.24 at first, there

FIG. 9. Flow Diagram of Heat-transport System (Second Design).

is a tendency for decrease during burnup. The axial temperature distribution of the fuel assembly at the hottest channel is shown in Fig. 8. The temperature shown in this figure is a nominal temperature, and a hot-spot factor is not considered. If account is taken of a hot-spot factor, the maximum fuel temperature is 2556°C, the maximum cladding temperature is 723°C, and the sodium outlet temperature is 705°C. The thermal characteristics of this annular core are shown in Table 3.

The power-distribution characteristic of this core shows that the power peaking factor remains at 1.55 in spite of the use of a one-zone core. This core is superior in many respects as a thermal design, that is, the power peaking factor has a tendency to decrease as a result of burnup, and the position of the hottest channel does not change; the flow orificing is assumed to be optimized. On the other hand, there are some questions; for instance, the radial power gradient of an inner part of the core is steep and the power variation of that part is large.

3.4. Characteristics of Sodium Flow in Core

The power ratio and the flow ratio in each region in the reactor are shown in Table 4. The reactor thermal power is 2500 MW, the coolant temperature of the reactor inlet is 400°C, the coolant temperature of the reactor outlet (average) is 600°C, and the total flow rate is 9.86 tons/sec. As shown in this table the flow ratio is small in the core (it contains the axial blanket), so that the coolant temperature flowing out from this region is a little higher than the average coolant temperature at the reactor outlet. The flow through each assembly in the core is adjusted by an orifice according to the power in order that the temperature of the coolant flowing out from assemblies is uniform. In fact, fluctuations of power distribution accompany burnup, as well as when new and old fuel is in the core after refueling. There is about a 10% fluctuation of the temperature rise due to burnup, and another 10% fluctuation for other reasons.

In Table 5 the pressure drop in each part of the hottest channel is indicated.

4. Heat-transport System

The flow sheet for the heat-transport system of the second design is shown in Fig. 9, and various items are given in Table 6.

The primary sodium flows directly into steam generators and reheaters from the reactor vessel. Sodium in is sent back to the reactor vessel by main circulating pumps equipped on each outlet line of the steam generators. The heat of the primary sodium is transferred to the steam system, passing the nearly

TABLE 6. DESIGN DATA FOR COOLING SYSTEM
(SECOND DESIGN)

Primary sodium system	4
Main loops, number	4
Auxiliary cooling systems, number	1
Sodium temperature	
Reactor outlet, °C	600
Reactor inlet, °C	400
Total flow rate, tons/hr	3.56×10^4
Flow rate per loop, tons/hr	8.90×10^3
Main circulating pump	
Number of pumps	4
Flow rate per unit, m ³ /min	173
Sodium temperature, °C	400
Head, m	82
Motor power, kW/unit	2,250
Steam generator	
Number of units	4
Type	Vertical, once-through, tube and tube
Heat capacity, MW/unit	500
Sodium data	
Flow rate per unit, tons/hr	7,120
Inlet temperature, °C	600
Outlet temperature, °C	400
Steam data	
Flow rate per unit, tons/hr	836.5
Inlet temperature, °C	268.5
Outlet temperature, °C	541
Pressure (outlet), kg/cm ²	255
Reheater	
Number of units	4
Type	Same as steam generator
Heat capacity, MW/unit	125
Sodium data	
Flow rate per unit, tons/hr	1,780
Inlet temperature, °C	600
Outlet temperature, °C	400
Steam data	
Flow rate per Unit, tons/hr	836.5
Inlet temperature, °C	335
Outlet temperature, °C	538
Pressure (inlet), kg/cm ²	60
Steam turbine	
Number of units	2
Type	Tandem-compound reheating, condensing
Output per unit, MW	500
Revolving speed, rpm	3,600
Steam pressure	
Inlet, kg/cm ²	246
Outlet, mm Hg	722
Steam temperature (inlet), °C	538
Steam extraction, stages	7

stagnant intermediate sodium layer in the steam generators and the reheaters.

There are four loops in the primary cooling system; each loop is arranged symmetrically with respect to the reactor vessel. Coolant from one loop issuing from the reactor divides so that part passes to a steam

generator and part to a reheater on the way; these flows are joined and return to the reactor by a main circulating pump. Each steam generator has a bypass piping with a flow-control valve; the primary sodium flows through at the partial load operation. The sodium flow rate in each loop is 8.9×10^3 tons/hr, about 25% flowing through a reheater in parallel with the steam generator.

Primary piping and components are all located in the reactor containment building. Also, the primary sodium flowing through the steam generators and reheaters is treated equally. However, the steam generators and the reheaters are separated by an anti-pressure wall from the reactor vessel, built to prevent damage in case of an accident arising from a sodium-water reaction.

The reactor vessel has overflow piping to hold a given level of sodium and dump piping. They are connected with a purification system and a supply system, respectively.

The reactor vessel, the main circulating pumps, the overflow tank, and the sodium-storage tank have individual free sodium levels. Argon is used as a cover gas over sodium to prevent any chemical reaction of sodium. Components and piping of the primary sodium system have a preheater and a double container so as to make it leakproof.

Discussion

See Discussion following article by Yoshimi *et al.*, THIS SESSION.

Parametric Studies Leading to the Nuclear Characteristics of the JAERI Design Studies

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(PRESENTED BY S. TERASAWA)

1. Purpose and Scope of Survey

For the first design,⁽¹⁾ a parametric survey was undertaken whose main object was to make the internal breeding ratio as high as possible near 1, even if the sodium-void coefficient is positive; it should be as close as possible to the negative side and covered by the overall negative power coefficient. Another aim was to design the power-flattened core with a peaking factor less than 1.6.

Parameters chosen were the atom ratio Pu-239/U-238, the fuel volume fraction, the coolant volume fraction, the structure volume fraction, and the volume ratio of inner to outer zones of the two-zoned core. In terms of these parameters changes of the following quantities were considered: critical mass, the breeding ratio, the internal breeding ratio, the Doppler characteristics, the sodium-void coefficient, and the power peaking factor.

In the second design,⁽²⁾ nuclear safety was emphasized to a greater extent. In case there was a local sodium void in the core and even if the positive reactivity based on the worst pattern is added, the design was to overcome these defects using the Doppler effect. The nuclear characteristics based on the core shape were compared for the flat cylinder, the module, and the annular core. The annular core was chosen.

The thermal and structural designs were surveyed in parallel. The resultant design was a compromise. The kinetics and the safety analysis were done together.

The first design is a reactor of an advanced type, and the second design is a reactor of a conservative type.

2. Method of Nuclear Calculation

The codes used for the nuclear calculations used in this survey are shown in Table 1. All were developed in JAERI.

The cross sections employed were from the so-called Russian sets.⁽³⁾ These were chosen because the Russian sets gave the most probable values for the characteristics of the large core among cross-section sets available at the time. Some comparison with other sets was made. Although the nuclear characteristics change quantitatively, there may not be significant change, especially near the optimum design points.

In the various parametric surveys, the one-dimensional multigroup diffusion code EXPANDA 2⁽⁴⁾ was extensively used. When dependence on core shape was to be considered, the transverse leakage was taken into account by using the figure for buckling as the first approximation. Then, a check calculation was done by the two-dimensional few-group diffusion code TWENTY FAST 2.⁽⁵⁾ In TWENTY FAST 2, the following procedure is used: at first the calculation for a spherical core is done by using the one-dimensional multigroup diffusion code, then the routine is used to calculate few-group constants (≤ 6) averaged over the gained spectrum, and the two-dimensional diffusion calculation is performed by using the few-group constants.

TORCH⁽⁶⁾ is a modification of the burnup code SIZZLE.⁽⁷⁾ All 25 groups can be carried. As it involves one-dimensional code, the vertical leakage is considered by using transverse buckling; buckling can be treated as a variable that depends on each group and burnup.

KPARAM 2⁽⁸⁾ was used for the calculation of the neutron flux and the adjoint neutron flux by using the same one-dimensional diffusion routine as in EXPANDA 2. From these results, parameters that are necessary for the kinetic characteristics calculation, the prompt-neutron lifetime l_p , the effective delayed-neutron fraction β_{eff} , etc., were calculated.

In EXCURS⁽⁹⁾ the kinetic characteristics parameters given in KPARAM 2 were used. This code gives the time variations of the power, the temperature distribution in the fuel at the hottest channel, the maximum cladding temperature, and the sodium outlet temperature when the reactivity is inserted at a ramp rate or stepwise. The nuclear system is simulated by a one-point model; the thermo-hydraulic

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system is represented by the hottest channel and the average channel. The feedback of reactivity due to Doppler effect and the sodium temperature effect is taken into account. However, since the reactivity change due to sodium boiling cannot be treated, the sodium-void effect is taken into account in the calculation in such a way that the positive reactivity is inserted at a ramp rate or stepwise.

EXPLO⁽¹⁰⁾ and modified AX-1⁽¹¹⁾ were used for the evaluation of explosion energies by core melting.

3. Result of Parametric Survey for Composition Change

To investigate the core characteristics with respect to composition, we chose a spherical one-region core surrounded by a depleted UO_2 (the enrichment of ^{235}U was 0.3%) blanket of 40-cm thickness. The fuel was a mixture of PuO_2 and UO_2 . The atom ratio of ^{239}Pu to ^{238}U , instead of the enrichment of ^{239}Pu , was employed as the parameter.

The isotopic composition of the plutonium in the fuel was assumed to be the same as that provided from the JAPCO TOKAI Plant spent fuel, i.e., $^{239}\text{Pu}/^{240}\text{Pu}/^{241}\text{Pu}/^{242}\text{Pu} = 0.7743/0.1772/0.0434/0.0050$. The density of the $\text{PuO}_2\text{-UO}_2$ mixture was taken as 10.0 g/cm^3 , and the density of the UO_2 blanket was also chosen as 10.0 g/cm^3 .

The cladding material was Type 316 stainless steel (Cr, 16–18%; Ni, 10–14%; Mo, 2.0–3.0%; remainder iron). The other structural material was Type 304 stainless steel (Cr, 18–20%; Ni, 8–11%; remainder iron). The mean composition of these structural materials was set at $\text{Fe}/\text{Cr}/\text{Ni}/\text{Mo} = 0.70/0.18/0.11/0.01$, and the density at 7.95 g/cm^3 for the nuclear calculation. The mean temperature of the coolant was assumed to be 500°C in the core and 450°C in the blanket. The mean temperature of the fuel was taken as 1300°C in the core and 750°C in the blanket.

TABLE 1. NUCLEAR CODES

Name	Content	Reference
EXPANDA-2	One-dimensional multi-group diffusion (≤ 25 groups)	JAERI-1118
TWENTY FAST 2	Two-dimensional few-group diffusion (≤ 6 groups)	JAERI memo 1966
TORCH	One-dimensional multi-group burnup	JAERI memo 2312
KPARAM 2	One-dimensional diffusion and kinetic parameters calculation	JAERI-1082
EXCURS	Safety analysis	JAERI memo 2515
EXPLO	Explosion energy in hypothetical accident	JAERI memo 3100

TABLE 2. PARAMETER AND VARIABLE RANGE

Parameter	Variable Range
Atom (number density) ratio of $^{239}\text{Pu}/^{238}\text{U}$ ϵ	0.10–0.20
Fuel volume fraction V_F	0.15–0.75
Coolant volume fraction V_{Na}	0.20–0.80
Structure volume fraction V_S	0.05–0.25

FIG. 1. Core Volume.

FIG. 2. Critical Mass.

FIG. 3. Internal Breeding Ratio.

The volume fractions of fuel, coolant, and structural material in the blanket were chosen to be the same as in the core.

The range of variation of the parameters are given in Table 2. The changing behaviors of the critical volume, the critical mass, the internal breeding ratio, the total breeding ratio, the sodium-void coefficient, and the Doppler characteristics are shown in Fig. 1 through Fig. 6. The graph for the sodium-void coefficient shows the variation of k_{eff} when the sodium density in the critical core is decreased uniformly as much as 50%. The plot of Doppler characteristics shows the variation of k_{eff} when the fuel temperature changes from 1300 to 1800°C.

The following results were obtained:

- (1) If the fuel volume fraction V_F is constant, the effect of the structure volume fraction V_s upon the nuclear characteristics is generally small. If V_s increases to the neighbourhood of 0.1, the internal breeding ratio and Doppler characteristics improve, and the sodium-void coefficient increases.
- (2) The internal breeding ratio increases rapidly with decrease of the enrichment. It increases a little with decreasing fuel volume fraction V_F .
- (3) The sodium-void coefficient increases with decrease of enrichment. The relation between the fuel volume fraction and the sodium-void coefficient varies with enrichment. In the region where ϵ is 0.1–0.13,

FIG. 4. Total Breeding Ratio.

FIG. 5. Sodium-void Coefficient (50% Sodium Loss).

the sodium-void coefficient decreases as the fuel volume fraction increases; if ϵ is greater than 0.15, the sodium-void coefficient increases as the fuel volume fraction increases.

(4) In order to get an internal breeding ratio near 1, the enrichment ϵ should be in the range from 0.1 to 0.13, and it is better to take the fuel volume fraction V_F as high as possible. The reason is twofold. In the range for ϵ from 0.1 to 0.13, the sodium-void coefficient decreases as V_F increases. In addition, the critical volume of the spherical core decreases rapidly as V_F increases, so we may convert it to a very flat cylindrical core to improve the sodium-void coefficient further.

The range of compositions of interest for the first design is given in Fig. 7.

4. Parametric Survey of Cylindrical Two-region Core

Since the internal breeding ratio and Doppler characteristics for the critical core barely depend on the core shape, the result calculated by one-dimensional diffusion for the spherical core is satisfactory for the cylindrical core. Also, the reflector saving depends mainly on the composition and not particularly on the shape of the core. However, it is necessary to calculate the result for the power peaking factor of a cylindrical core by using the cylindrical shape.

FIG. 6. Doppler Characteristics.

In the first design, a two-zoned core was taken in order to attain the power flattening. In this case we compared the result for change of enrichment while keeping the same volume fractions with the result for change of volume fraction while keeping the same enrichment in the two zones. The height of the cylindrical core was 80 cm.

The conclusions obtained are as follows:

(1) To change the enrichment is better than to change the volume fraction in the two zones.

(2) The variation of nuclear characteristics is shown in Fig. 8.

(3) The nuclear characteristics when the enrichments of the inner and the outer zones are changed a little, for fixed geometry of the zone, are given in Fig. 9.

FIG. 7. Range of Interesting Composition.

FIG. 8. Nuclear Characteristics of Two-zone Core.

FIG. 9. k_{eff} and IBR vs Enrichment of Two-zone Core.

From these results the design of the core for the 1st design was selected.

Another interesting thing found in this survey was the design of a reactor which has good nuclear characteristics, as shown in Fig. 10, if the fuel volume fraction is made high such as in a tube-in-shell fuel.

5. Comparative Survey of Core Configuration

The nuclear characteristics for the second design were surveyed with emphasis on the protection against the occurrence of a large positive reactivity caused by local void of sodium. The following three core shapes were chosen;

- (1) flat cylindrical core;
- (2) modular core;
- (3) annular core.

The core volume for a 1000-MWe power reactor should be several thousand litres. The survey calculation

was carried out for a fuel volume in the core 2000 l. The volume fraction of stainless steel (cladding and structure material) in the core was assumed to be 15%.

In the calculation, the same as for the first design, the reactivity change was calculated when the sodium density in the core was decreased uniformly to 50%. At the same time, the maximum value of the positive reactivity caused by the worst pattern of local sodium voids in the core, $\text{Max } (\Delta k)_{Na}$, was calculated as an important feature for comparison of core configuration.

The calculation was mainly carried out by a one-dimensional diffusion code, but occasionally a two-dimensional diffusion code was used. The following results were obtained.

(1) Flat Cylindrical Core

In order to keep $\text{Max } (\Delta k)_{Na}$ small, the fuel volume fraction V_F and the enrichment should be high, and the core height H should be small.

Figure 11 shows an example of an approximate

FIG. 10. Characteristics of Two-zone Core (Tube-in-shell Fuel).

value of maximum positive reactivity due to local sodium void for the infinite slab core.

To get better Doppler characteristics and breeding ratio, H and V_F should be large.

If the pressure drop is considered from the thermal and hydraulic design, V_F has an upper limit. The values $H = 30$ cm, $D_c = 412$ cm, and $V_F = 0.50$ were selected for the representative flat cylinder.

(2) Modular Core

The relation between the number n of modules, the critical mass M_c , sodium-void coefficient, Doppler characteristic $T(dk/dT)$, and $\epsilon = {}^{239}\text{Pu}/{}^{238}\text{U}$ was examined. An example is shown in Fig. 12. The value of $\text{Max}(\Delta k)_{Na}$ was guessed from the spherical core.

For a representative unit module, a cylinder with $D = H = 108$ cm and $V_F = 0.40$ was selected.

(3) Annular Core

For the annular core, the core height H , the core outer diameter D_c , and the core inner diameter D_i can be chosen as parameters. No inner blanket was used with the annular core in order to obtain better control and improve the sodium-void effect.

In order to prevent useless leakage of the neutrons from the reactor, reflectors were placed in the upper and the lower sides of the central sodium region, as shown in Fig. 13. The composition of the reflector was $\text{BeO}/\text{Na}/\text{SS} = 60/25/15$. The calculated sodium-void coefficient of the annular core was found to be between the values for the spherical core and for the infinite-slab core.

For the representative annular core the following values were chosen: $H = 70$ cm; $D_c = 150$ cm; $D_i = 50$ cm; $V_F = 0.45$.

A comparison of the nuclear characteristics of the typical cores for the three different configurations is given in Table 3.

Although both the modular and annular cores were good, the annular core was chosen to take account of advantages for control and features of the thermal and the structural designs.

6. Parametric Survey of Annular Core

It is difficult to evaluate the nuclear characteristics quantitatively for an annular core by using only a one-dimensional diffusion calculation. Especially when the blockage or boiling of sodium coolant is generated locally in the core, a two-dimensional diffusion calculation is necessary for estimating the maximum positive reactivity due to this phenomenon. In the second design, for the annular core of complex geometry with BeO reflectors in the upper and the

FIG. 11. Maximum Sodium-void Coefficient of Infinite Slab Core.

lower sides of the central sodium region, it was necessary to do many one-dimensional and two-dimensional calculations.

The typical results obtained are as follows:

(1) When the core inner diameter is changed, keeping the core volume at 5,000 l and changing the core height H to 40, 80, and 120 cm, respectively, the critical enrichments (for convenience of treatment, the ${}^{239}\text{Pu}/{}^{238}\text{U}$ atom ratio ϵ) are calculated and given in Fig. 14. As shown by the figure, since ϵ can be chosen arbitrarily in the range from 0.1 to 0.2 as the inner diameter is changed, the nuclear characteristics in the core can be changed drastically by adjusting the enrichment. Thus, the nuclear design can be chosen much more freely and independent of the structural and thermal designs of the annular core.

(2) When the core inner diameter is changed, leaving the enrichment constant, the change of the core volume and the sodium-reactivity effect are as given in Fig. 15.

(3) When the core shape, dimensions, and the volume fractions are constant, the relations between the enrichment and the breeding ratio, the internal breeding ratio, and the excess reactivity available for burnup are as shown in Fig. 16.

(4) If a control-rod follower made of stainless steel

FIG. 12. Nuclear Characteristics of Modular Core.

FIG. 13. Reactor Core Arrangement (Second Design).

is attached to the safety rod, the sodium-void effect decreases. In the reference core, $\text{Max } (\Delta k)_{Na}$ decreased from \$2.18 to \$1.81.

(5) If the BeO reflector in the upper and the

TABLE 3. CORE SHAPE AND NUCLEAR CHARACTERISTICS

Nuclear Characteristics Core Shape	Flat Cylinder	Module	Annular
Core radius (cm)	206	54	150 (Outer)
Core height (cm)	30	108	704
Core volume (l)	4000	5000	4400
Fuel volume fraction	0.50	0.40	0.45
$^{239}\text{Pu}/^{238}\text{U}$ atom ratio	0.17	0.15	0.13
$\text{Max}(\Delta k)_{\text{local Na void}} (\%)$	1.5	$1.2 + \alpha$	$1.5 + \alpha$
$(\Delta k)_{Na \text{ 50\%}} (\%)$	0.5	-1.3	-0.0
Doppler characteristics $T(dk/dT) (\%)$	-0.49	-0.74	-0.82
IBR	0.68	0.79	0.89
BR	1.56	1.46	$1.52 - \alpha$
Peaking factor	No good	Good	Better

lower sides of the central sodium region is replaced with blanket fuel, the breeding ratio increases. In the reference core it increased from 1.33 to 1.44.

The effect that each parameter in (3), (4), and (5) exerts on the main nuclear characteristics is indicated in Table 4.

(6) For the annular core the peaking factor for power distribution is as small as 1.55, even for a one-

Core Volume 5,000ℓ
 $V_F = 0.41$
 $V_S = 0.18$
 Radial Blanket
 Thickness 40cm
 Central Region $V_S = 0.05$

FIG. 14. Critical Enrichment vs Inner Radius of Annular Core.

Core Height 80cm
 $V_F = 0.40$, $V_S = 0.15$
 $\epsilon = 0.14$
 Radial Blanket Thickness 40cm
 Central Region $V_{Na} = 0.92$

FIG. 15. Critical Volume and Sodium-void Coefficient vs Inner Radius of Annular Core.

FIG. 16. Breeding Ratio and Excess Reactivity vs Enrichment.

FIG. 17. Power Distribution in Radial Direction.

TABLE 4. EFFECT OF CHANGES OF DESIGN PARAMETERS ON REFERENCE CORE

	Δk_{ex}	BR	IBR	$\text{Max}(\Delta k)_{Na}$	Effect
Reduction of ϵ (0.146 $\rightarrow$ 0.140)	Decrease	Increase	Increase	No change	Excess reactivity decrease
Safety-rod followers (SS $\rightarrow$ Na)	Increase a little	Little decrease	Little decrease	Increase	Max Na-void-effect increase
Central reflector (BeO $\rightarrow$ blanket)	Decrease	Increase	Increase a little	—	BR increase Frequent replacement of reflector is required Better power distribution

TABLE 5. NUCLEAR CHARACTERISTICS OF THE JAERI 1000-MWe FBR DESIGN

Quantity	First Design	Second Design	Quantity	First Design	Second Design
1. Total power, MWt	2500	2500	3. Fuel—Continued		
Core power, MWt	2200	2250	^{241}Pu	4.0 4.4	4.8
Axial blanket power, MWt	300	150	^{242}Pu	0.8 0.7	1.1
Radial blanket power, MWt		100	(discharged blanket fuel)		Axial Radial
2. Core			^{239}Pu	98.4	96.7 94.5
Core shape	Flat Cylinder	Annular	^{240}Pu	1.6	3.3 5.5
Active core height, cm	80	80	Breeding ratio	1.44	1.29
Core diameter, cm	268.4	324.4	Internal breeding ratio	0.96	0.81
Inner zone, cm	180.0	160.0	External breeding ratio	0.48	0.48
Outer zone, cm	264.4	324.4	Simple doubling time, yr	7.2	12.6
Core volume, l	4400	5000	4. Number of assemblies		
Inner zone/outer zone	0.859	0	Core		
Axial blanket thickness, cm	40.0	40.0	Inner zone	157	
Radial blanket thickness, cm	40.0	40.0	Outer zone	180	390
Axial reflector thickness, cm	0	40.0	Radial blanket	234	312
Core and axial blanket volume composition, %			Control rod		
Fuel ($\text{PuO}_2\text{-UO}_2$)	44.4	41.4	Inner	6	
Coolant (Na)	39.0	38.2	Outer	3	9
Structure (SS)	15.8	21.6	Safety rod	3	6
Gas bond (He)	0.8	0.8	5. Reactivity		
Radial blanket volume composition, %			Doppler characteristics	-9.66×10^{-3}	-9.16×10^{-3}
Fuel (UO_2)	44	55	$T(dk/dT)$		
Coolant (Na)	39	25	Sodium in the core inner zone voided	-9.35×10^{-3}	
Structure (SS)	16	20	Sodium-void effect $(\Delta k)_{\text{Na}}$		
Inner space composition, %			100% core voided		2.19×10^{-3}
Coolant (Na)		92	50% core voided	3.24×10^{-3}	1.24×10^{-3}
Structure (SS)		8		(\$0.87)	(\$0.34)
Reflector composition, %			Max $(\Delta k)_{\text{Na}}$ (local sodium void)	11.41×10^{-3}	6.74×10^{-3}
Reflector (BeO)		60		(\$3.1)	(\$1.82)
Coolant (Na)		25	Central zone 100% voided		-1.09×10^{-3}
Structure (SS)		15			(\$-0.29)
3. Fuel			Control-rod Worth Δk , %	7.0	3.5
Initial enrichment ($^{239}\text{Pu} + ^{241}\text{Pu}$), %			Burnup compensation	2.1	3.4
Inner core	9.5			(6 outer rods)	
Outer core	13.0	13.0	Temperature compensation	1.5	1.7
Blanket	0	0	(startup and shutdown)	(3 inner rods)	
Final enrichment ($^{239}\text{Pu} + ^{241}\text{Pu}$), %			Safety-rod worth Δk , %	3.5	6.0
Inner core	10.7				(3.0% by 3 rods used for temperature compensation)
Outer core	12.9	11.6	6. Kinetic parameter		
Radial blanket		2.19	Effective delayed-neutron fraction β_{eff} , %	0.37	0.37
Axial blanket		2.37	Prompt-neutron life time l_p , sec	3.5×10^{-7}	3.5×10^{-7}
Core fissile metal ($^{239}\text{Pu} + ^{241}\text{Pu}$), kg	1930	2335	7. Power peaking factor		
Specific power, MW/kg (fissile)	1.14	0.96	Total	1.60	1.55
Initial Pu composition, %			Radial	1.24	1.25
^{239}Pu	77.4	77.4	Axial	1.29	1.24
^{240}Pu	17.7	17.7	8. Burnup, MWd/t		
^{241}Pu	4.3	4.3	One-batch	48,400	
^{242}Pu	0.5	0.5	Maximum	100,000	100,000
Final Pu composition (discharged core fuel), %			Average	83,600	83,000
^{239}Pu	75.4 74.7	70.8			
^{240}Pu	19.8 20.2	23.3			

region core. The power distribution in the radial direction is given in Fig. 17. Because control rods are inserted in the central sodium region of the inside of the annular core to suppress the excess reactivity in the initial fresh core, the power there is lower, but during burnup the control rod is pulled out and the power becomes higher. At the same time, accumulation of plutonium in the radial blanket increases and the power near the blanket increases. Thus, although the power peaking factor in the radial direction is 1.25 in the initial core, it decreases gradually during burnup. The position of the hottest channel is not changed by burnup. This is one of the features in the annular core.

7. Nuclear Characteristics of the First and Second Designs

The main characteristics of the reference design finally chosen are given in Table 5.

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Discussion

See Discussion following article by Yoshimi *et al.*, THIS SESSION.

Plant System Parametric Study on Sodium-cooled Prototype Fast Breeder Reactor

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ABSTRACT

A parametric study of a sodium-cooled prototype fast breeder reactor plant to yield 200–400 MWe has been conducted. The objective of the present study was to evaluate and select the preferable parameters of the prototype reactor plant. The evaluation criteria for the economic performance were considered to be practical, and conclusions were derived concerning safety and reliability.

The information needed to determine the main parameters of the prototype reactor plant was obtained. The following items have an important effect for determination of the main parameters: investigation of the safety design criteria based on study of the core-failure mechanism; investigation of maximum allowable temperature of a fuel cladding; investigation of hot-spot factors; estimation of construction costs of heat exchangers, especially of steam generators.

1. Introduction

For the evaluation of the optimum parameters of the prototype fast reactor plant, a preliminary design of the plant system was made and then the following items investigated;

(1) To find the attainable reactor outlet coolant temperature within the core design limits.

(2) To correlate the reactor coolant temperature rise and the reactor outlet coolant temperature with respect to the core design limits, cost of components of the heat-transport system, and plant efficiency.

(3) To correlate the plant thermal power with the core design limits.

(4) To determine if the above relations are affected by the design parameters of the core system and steam-electric system.

The calculations and evaluations are carried out by

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dividing the plant into two systems: the core system and the steam-electric system.

Main characteristics as evaluation measures are shown in Table 1, and the bases of evaluation are shown in Table 2. The parameters and their ranges are shown in Table 3.

2. Premise for Calculation

2.1. Core System

A cylindrical core was used as a model. The core had two zones of different enrichment, equal in volume. The fuel assembly was constructed in a hexagonal wrapper tube and was determined to have a maximum reactivity worth of \$2 based on preliminary safety analysis in the case of misoperation during refueling. The fuel pins were arranged in triangular array with spiral wire spacers. Core fuel was mixed $\text{PuO}_2\text{-UO}_2$ of density equal to 10 g/cm^3 (90.1% of theoretical). The isotopic composition of plutonium was taken as $\text{Pu-239/Pu-240/Pu-241/Pu-242} = 0.774/0.177/0.043/0.005$, which corresponds to that of plutonium discharged from the Tokai reactor.

Depleted uranium (0.3% ^{235}U) was used for core and blanket oxide. The volume fraction in the axial blanket was the same as in the core. The radial blanket had a greater fuel fraction in order to increase total breeding ratio. Type 316 stainless steel was used as the fuel-cladding material.

The ranges of core-structure parameters, such as fuel-pin diameter and pitch, which relate to core composition and volume, were set to cover the design of future power reactor cores. It was assumed that the core generated 90% of total power and the blankets 10%. Core-structure parameters and core volume compositions are listed in Table 4. Volume fraction of the radial blanket was fuel/structure/coolant = $0.55/0.20/0.25$.

Core volumes were calculated based on a maximum linear heat rate of 450 W/cm and a total power peaking factor of 1.6.

The calculated volumes are shown in Table 5, where M1-M9 represent material number and G1-G9 represent the geometry numbers of the cores. The core

R55-G5 occupied a central position in the survey and so was named the reference core R55 for convenience.

2.2. Steam-Electric System

In the survey of the steam-electric system, heat balances of heat exchangers and steam turbine were calculated. The former included calculation of the heat-transfer area of the intermediate heat exchanger (IHX), steam generator (SG), and primary and secondary sodium pumping power; the latter included

TABLE 1. CALCULATED MAIN CHARACTERISTICS
(EVALUATION MEASURES)

Core system
Sodium-void effect
Doppler effect
Critical enrichment (specific fissile inventory)
Total breeding ratio (specific fissile production rate; fissile doubling time)
Internal breeding ratio (reactivity change by burnup)
Primary-coolant pressure loss (primary-coolant pumping power required)
Fuel-cladding hot-spot maximum temperature
Steam-electric system
Plant net thermal efficiency (electric power)
Heat-transfer area of intermediate heat exchanger (equipment cost)
Heat-transfer area of steam generator (equipment cost)
Heat-transfer area of reheater (equipment cost)
Secondary-coolant pumping power required
Feedwater pumping power required

TABLE 2. EVALUATION CRITERIA

Limiting conditions on safety
Sodium-void effect: Reactivity change caused by complete coolant loss from the whole core should be negative or zero.
Doppler effect: Reactivity insertion caused by full coolant loss from the central 25% of the fuel channels of the core should be overcome by Doppler effect before any large-scale failure of the core occurs.
Fuel-cladding hot-spot maximum temperature: The temperature should be below 700°C at the inner surface of the cladding.
Primary-coolant pressure loss: It should be below 10 kg/cm ² .
Assumptions for economic evaluation
Price of fissile material: 3,600 Yen/g Pu
Annual charge rate to cost of fission-material inventory: 10%
Equipment cost of intermediate heat exchanger per heat-transfer area: 250,000 Yen/m ²
Equipment cost of steam generator: 500,000 Yen/m ² 1,000,000 Yen/m ²
Capital charge rate: 14%
Plant load factor: 80%
Power cost: ^[a] 3 Yen/KWh

^[a] This is used only for evaluation of cost of the primary-coolant pumping power.

TABLE 3. PARAMETERS SURVEYED AND THEIR RANGES

Main parameters
Reactor thermal power: Q_0 : 500–1,000 MWt
Reactor outlet coolant temperature, T_{RO} : 500–650°C
Reactor coolant temperature rise, ΔT_R : 150–300°C
Auxiliary parameters of core system
Core height, H_c : 60–100 cm
Fuel-rod diameter, D_f : 5.6–6.4 mm
Fuel-rod interval, I_s : 1.0–1.6 mm
Auxiliary parameters of steam-electric system
Temperature rise of secondary coolant in intermediate heat exchanger, ΔT_x : 150–300°C
Main steam pressure: 102–246 ata
Main/Reheat Steam Temperature: 454–538°C

calculations of steam-cycle efficiency, feedwater-pumping power, etc. The type of steam cycle was selected to be most typical and was optimized for each steam condition. Main items of steam cycle are shown in Table 6.

3. Results

3.1. Core System

Critical enrichments of one-zone cores are 14–17% for 500 MWt, 13–16% for 750 MWt, and 12–16% for 1,000 MWt. Radial peaking factors of one-zone cores are 1.5–1.8 for 500 MWt, 1.6–1.8 for 750 MWt, and 1.65–1.82 for 1,000 MWt.

The relation between the atomic density ratio of plutonium, $m = N_I^{Pu}/N_{II}^{Pu}$, in the inner and outer zones and the power peaking factor is examined by changing m from 1.25 to 1.50 for several cores (including R55).

The radial power peaking factor shows a minimum value at $m = 1.40$ – 1.45 . As the core becomes smaller, the optimum value of m becomes rather large. The optimum value of m depends on the dimensions of core, but as the variation is small, $m = 1.4$ was assumed.

The internal breeding ratio of two-zoned core was smaller than that of the one-zone core by 0.04–0.05. The total breeding ratios were 1.2–1.35.

The reactivity effects due to complete coolant loss are shown in Fig. 1. The value becomes more positive as the core height becomes higher and the fuel volume fraction larger. The relation between the sodium-void effect and fuel enrichment is shown in Fig. 2. The core having an enrichment below 13% has a positive sodium-void effect.

The Doppler effects ($-T(dk/dT)$) are shown in Fig. 3. The Doppler effect has the same tendencies as sodium-void effect. The relations between them are shown in Fig. 4.

TABLE 4. GEOMETRICAL PARAMETERS AND VOLUME FRACTIONS OF FUEL ASSEMBLY

Material Number	Pellet Diameter (mm)	Thermal Bond Thickness (mm)	Clad Thickness (mm)	Fuel-pin Diameter (mm)	Fuel-pin Pitch (mm)	Fuel Assembly ^(a)			Volume Fraction (%)		
						Inside Dimension (mm)	Wall Thickness (mm)	Pitch (mm)	Fuel	Structure	Coolant
M1	4.82	0.05	0.34	5.6	6.6	87.7	2.0	93.7	40.56	22.43	35.21
M2	5.18	0.05	0.36	6.0	7.0	92.9	2.0	98.9	42.05	22.08	34.23
M3	5.54	0.05	0.38	6.4	7.4	98.3	2.0	104.3	43.24	21.70	33.48
M4	4.82	0.05	0.34	5.6	6.9	91.9	2.0	97.9	37.15	22.02	39.27
M5	5.18	0.05	0.36	6.0	7.3	97.2	2.0	103.2	38.61	21.62	38.27
M6	5.54	0.05	0.38	6.4	7.7	102.4	2.0	108.4	40.03	21.29	37.22
M7	4.82	0.05	0.34	5.6	7.2	96.1	2.0	102.1	34.16	21.87	42.54
M8	5.18	0.05	0.36	6.0	7.6	101.4	2.0	107.4	35.65	21.42	41.53
M9	5.54	0.05	0.38	6.4	8.0	106.6	2.0	112.6	37.10	21.07	40.48

^(a) Number of fuel pins in an assembly is 169.

TABLE 5. CORE VOLUME (LITERS)

Power, MWt.....			500			750			1000		
Core Height (cm).....			60	80	100	60	80	100	60	80	100
Fuel-pin Diameter (mm)	Pitch (mm)	Material Number	G1	G2	G3	G4	G5	G6	G7	G8	G9
5.6	6.6	M1	720	723	722	1080	1080	1070	1440	1440	1440
6.0	7.0	M2	803	806	804	1200	1200	1200	1600	1600	1600
6.4	7.4	M3	893	896	895	1330	1340	1330	1780	1780	1790
5.6	6.9	M4	786	780	788	1180	1180	1170	1570	1570	1570
6.0	7.3	M5	874	878	876	1310	1310	1300	1740	1740	1750
6.4	7.7	M6	964	968	966	1440	1440	1440	1920	1920	1930
5.6	7.2	M7	855	859	857	1280	1280	1280	1710	1710	1710
6.0	7.6	M8	947	941	949	1420	1420	1410	1890	1890	1890
6.4	8.0	M9	1040	1045	1043	1560	1560	1550	2080	2080	2080

Burnup calculations were performed for the reference core R55 from the fresh condition to 100,000 MWd/t. It was found that an excess reactivity of about 5% $\Delta k/k$ would be required for 100,000-MWd/t burnup by one batch, and the peak power density at the outer zone would decrease by about 15%.

A simple relation between the reactivity change by burnup and the core internal breeding ratio was found by burnup calculations for some typical cores. The relation was used for evaluation of burnup characteristics of the cores.

The evaluation measures for combination of many parameters that provide the reactor thermal power and the core structure are as follows; sodium-void effect (Y_1), Doppler effect (Y_2), burnup characteristics (Y_3), fissile material doubling time (Y_4), and the sum of inventory cost and plutonium credit (Y_5). On the other hand, the core-coolant pressure loss (Y_6) and the primary-coolant pumping-power cost (Y_7) are

values that are determined by considering the reactor coolant temperature rise and the core structure. Also, the maximum temperature of the fuel cladding (Y_8) is a value which is determined by the reactor outlet coolant temperature and the reactor coolant temperature rise. The evaluation criteria for each evaluation measure was applied to the calculated results. A typical result is shown in Fig. 5.

In order to judge the results easily, large and small weightings were applied to the evaluation measures. For the prototype reactor, safety and technical limits are important. On the whole, Y_1 , Y_2 , Y_6 , and Y_8 are considered to be strong measures, whereas Y_3 , Y_4 , Y_5 , and Y_7 are considered to be weak measures. The most desirable cores selected by such operations are shown in Table 7.

The factor Y_8 serves as a strong evaluation measure for the primary-coolant temperature. In Fig. 6 allowable conditions for the primary-coolant tempera-

ure (reactor outlet coolant temperature and reactor coolant temperature rise) are shown as related to the fuel structures when the maximum allowable fuel-cladding temperatures are 650°C and 700°C. The most preferred conditions covering all core structures are shown in Table 8.

The sum of the factors Y_5 and Y_7 determines the economy of the core system and is shown in Table 9.

From the above tables, we may select the best kind of core, the characteristics being as follows: reactor thermal power is about 750 MW, the core height is

TABLE 6. MAIN ITEMS OF STEAM CYCLE

Type of cycle	Reheat (1 stage), regenerative cycle
Steam turbine	Tandem compound; unit power (100,000–300,000 kW); selected suitably for various main steam conditions
Condenser vacuum	722 mm Hg
Feed heaters	Surface type, 7 stages
Internal efficiency of steam turbine	86% at dry stage; Baumann effect is used at wet stage
Temperature drop from SG superheater outlet to steam-turbine inlet	3°C
Wetness at steam-turbine final stage	<8%
Generator combined efficiency	98.5%
Heat loss from steam system	0.5%

FIG. 2. Sodium-void Effect as a Function of Fuel Enrichment.

about 80 cm, the fuel-rod interval is 1.3–1.6 mm, and the fuel-rod diameter is 5.6–6.0 mm. However, there is little difference as regards economy between such a core and others.

FIG. 1. Reactivity Change for Loss of Coolant.

FIG. 3. Doppler Coefficient.

3.2. Steam-Electric System

3.2.1. Effects of Main Steam Pressure

The calculation results and their economic evaluation are shown in Table 10. A steam pressure of 169 atm indicates the best economy. The supercritical pressure is nearly as good economically as 169 atm, and in the fast reactor the merits of high pressurization might be reduced by status of steam-generator technology.

3.2.2. Effects of Main Steam Temperature

The calculated results and economic evaluation for effects of main steam temperature are shown in Table 11 and Fig. 7. The steam temperature depends on the reactor outlet coolant temperature. If a material such as Type 316 stainless steel is used as fuel cladding, the reactor outlet temperature will be lower than 600°C. Accordingly, main steam temperatures should be selected between 480 and 538°C. If a steam temperature of 510°C is taken as the base point, a steam temperature of 480°C is most economical at the present time, but the difference between 480 and 510°C is not large. Future commercial reactors will probably use a higher temperature. Thus, a steam temperature of 510°C is considered most preferable.

3.2.3. Effects of Reactor Coolant Temperature Rise

A part of the calculation result is shown in Table 12 and Fig. 8. The best economy occurs for a reactor coolant temperature rise of 150–200°C in spite of the steam-generator cost, especially when the cost is high (1,000,000 Yen/m²). When the cost is low (500,000 Yen/m²) the preferable point moves towards 150°C or lower. On the other hand, since the primary-coolant pumping power varies inversely to about the 2.5 power of the reactor coolant temperature rise, it increases rapidly below 175–200°C. Since the pump-equipment cost, the design pressure of the primary-coolant system, the reactor vessel, the holddown mechanism, and so on increase with a rise of pumping power, it seems that the reactor coolant temperature rise should not be smaller than 175–200°C.

3.2.4. Effects of Secondary Coolant Temperature Rise in IHX

The optimum value for the secondary-coolant temperature rise in IHX is affected by the steam-generator cost. As shown in Fig. 9, the optimum value is 150–200°C at 1,000,000 Yen/m². The secondary-coolant pumping power does not change much in the region from 170 to 250°C.

FIG. 4. Relationship between Sodium-void and Doppler Effects.

4. Conclusion

Based on this survey the following conclusions were obtained.

(1) A reactor thermal power of about 750 MWt is most suitable, as determined by examination of the characteristics of the core system. This conclusion was derived from the present safety criteria and nuclear data for cores.

(2) When account is taken of the plant thermal efficiency and the costs of heat-exchange equipment, the higher reactor outlet coolant temperature is the better. A small value for the reactor coolant temperature rise is good with respect to the plant thermal efficiency and the costs of heat-exchange equipment, but a large value is good with respect to the costs of primary-coolant pumping power when the reactor out-

let coolant temperature is constant. On the other hand, a definite relation between the reactor outlet coolant temperature and the reactor coolant temperature rise is required from the maximum temperature limit of the core-fuel cladding; so if the former is high, the latter should be low, and conversely. A reactor coolant temperature rise of about 150°C and a reactor outlet coolant temperature of about 580°C are suitable.

(3) With respect to the core hydraulic characteristics, the core height and the fuel-rod interval are related: when the former is high, the latter should be large; when the former is low, the latter should be small.

(4) The core for which nuclear characteristics as regards safety are good, and for which the sum of

Fig. 6. Primary-coolant Temperatures.

TABLE 8. MOST PREFERRED REGION OF CONDITIONS FOR PRIMARY-COOLANT TEMPERATURE

Reactor Outlet Coolant Temperature, °C	Reactor Coolant Temperature Rise, °C
550-560	200
565-575	175
580-590	150

TABLE 9. SUM OF Y_5 AND Y_7 (Yen/kWh)

Reactor Power (MWt)	Core Height (cm)	Fuel-rod Interval (mm)	Fuel-rod Diameter (mm)	ΔT_R		
				150°C	175°C	200°C
1500	60	1.3	5.6	1.9	1.2	0.6
750	100	1.6	5.6	2.0	0.3	-0.8
750	80	1.3	5.6	1.5	0.1	-1.1
750	80	1.6	5.6	1.7	-0.8	0.1
750	80	1.6	6.0	1.7	0.9	0.3
750	60	1.0	5.6	2.0	0.7	-0.3
750	60	1.3	5.6	2.3	1.6	1.0
500	60	1.3	5.6	2.4	0.8	-0.2
500	60	1.0	6.0	2.9	1.8	1.0

TABLE 10. EFFECTS OF MAIN STEAM PRESSURE
 $Q_o = 750$; $T_{RO} = 600$; $\Delta T_R = 150$; $\Delta T_x = 150$; $T_{sv}/T_{R2} = 538/538$

Evaluation Measure	Steam Pressure P_{sv} (atm)			
	246	169	127	102

(a) Calculation Results

Heat-transfer area of steam generator (m^2)	2280	2140	2140	1730
Heat-transfer area of reheater (m^2)	840	780	830	850
Heat-transfer area of intermediate heat exchanger (m^2)	3100	3100	3100	3100
Feedwater-pumping power (MW)	1220	790	580	450
Primary-sodium pumping power (MW)	8	8	8	8
Secondary-sodium pumping power (MW)	1.5	1.5	1.5	1.5
Net rate of plant heat consumption (kcal/kWh)	1970	2010	2130	2130
Net electrical output (MW)	328	320	311	303

(b) Economy Comparison

Turbine-generator set (10^8 Yen)	26.24	25.60	27.99	30.30
Condenser and condensing supply works	7.89	7.71	8.50	9.18
Piping	7.85	7.65	8.30	9.00
Feedwater systems	3.94	7.89	4.20	4.55
Intermediate heat exchanger	45.92	44.87	48.99	53.03
Steam generator ($500,000$ Yen/ m^2)	7.76	7.76	7.76	7.76
($1,000,000$ Yen/ m^2)	15.60	14.03	13.03	12.40
	31.22	28.06	26.06	24.80
Total cost (10^8 Yen)	69.29	66.66	69.78	73.10
	84.90	80.69	82.81	85.59
Cost per net electrical output (Yen/kWe)	21,100	20,800	22,400	24,100
	25,800	25,200	26,600	28,200
Relative cost evaluation	100	98.6	106.2	114.3
		97.4	102.8	109.1

and the plant thermal efficiency. A study of many effects, such as irradiation damage and the sodium environment of the fuel-cladding material, is important. There is need for improvement of stainless steel and development of other refractory alloys. The design study of fuels should take into account fuel-manufacturing cost and core characteristics.

(3) Given a definite maximum allowable temperature of fuel cladding, the hot-spot factor is a controlling factor for the reactor outlet coolant temperature. Detail analysis of the uncertainty factors

TABLE 11. EFFECTS OF MAIN STEAM TEMPERATURE

 $Q_o = 750$; $T_{RO} = 600$; $\Delta T_R = 200$; $\Delta T_x = 200$; $P_{sv} = 169$

Evaluation Measure	T_{sv}/T_{R2}		
	538/538	510/510	480/480
Heat-transfer area of steam-generator reheater (m ²)	3500	2710	2340
Heat-transfer area of intermediate heat exchanger (m ²)	2650	2650	2650
Sodium pumping power, primary/secondary (MW)	3.70/1.13	3.70/1.13	3.70/1.13
Feedwater-pump power (kW)	7860	8100	8380
Net rate of plant heat-consumption (kcal/kWh)	1900	2010	2050
Net thermal efficiency (%)	43.30	42.70	42.00
Net electrical output (MW)	325.0	320.2	314.7

FIG. 7. Main Steam Temperature.

composing the hot-spot factor and evaluation of each factor based on a mockup test are important. The study of statistical characteristics of the fluctuation of factors which compose the hot-spot factor and the

study of mathematical methods for superimposing factors groups are necessary.

(4) The costs of components for the heat-transport system (especially the manufacturing cost of the

TABLE 12. EFFECTS OF REACTOR COOLANT TEMPERATURE RISE

$Q_o = 750$; $T_{RO} = 650$; $\Delta T_x = 300$; $P_{sv} = 169$; $T_{sv}/T_{R2} = 480/480$

Evaluation Measure	ΔT_R (°C)			
	300	250	200	150
Heat-transfer area of steam-generator reheater (m ²)	2960	2960	2960	2960
Heat-transfer area of intermediate heat exchanger (m ²)	1290	940	940	730
Primary-sodium pumping power (MW)	1.5	2.0	3.7	8.0
Net rate of plant heat consumption (kcal/kWh)	2030	2040	2050	2080
Net electrical output (MW)	317.3	316.8	315.3	310.8

FIG. 8. Reactor Coolant Temperature Rise.

FIG. 9. Secondary-coolant Temperature Rise in IHX.

steam generator) have a large effect on the determination of system parameters. Lowering of the costs of these components is the most effective way to reduce plant capital cost.

Discussion

Mr. Charak (ANL): What is the Japanese program with respect to building a prototype reactor?

Mr. Miki (PNC-Japan): Construction of our prototype is scheduled to start in 1973, and criticality will be attained in 1976.

Mr. Wirtz (Karlsruhe): Did I understand that in the second design you have a beryllium reflector? Why do you like to have such a reflector?

Mr. Nozawa (JAERI): The primary reason for a beryllium reflector inside the annular core is to get a higher Doppler coefficient.

Mr. Benedict (MIT): Does that not cost you something in breeding ratio to degrade the spectrum?

Mr. Nozawa: Yes, we have a little penalty from the beryllium oxide; we have no blanket inside the annular core, however, so it's not too much. If we put a blanket inside the annular core, we might have a more positive sodium coefficient; we just avoid putting the blanket inside the annular core.

Mr. Pearson (ANL): In your fuel-handling scheme, you plan on rotating your entire core structure. What are you planning in your research and development program to test out this type of concept? What do you have as a background for the mechanisms that you have planned to employ?

Mr. Nozawa: We think that the most important component for rotating the core in sodium is good bearings. We have applied different loads on this roller bearing in sodium. The roller bearing was made of Stellite-B, but we have to do more comprehensive testing and research and development.

Mr. Wyckoff (ComEd): I'd like to know a little more about your prototype plant. What size will it be? What is the steam pressure? What is the steam temperature? Can you reference it to the 1000-MW plant; do you see it as being very similar in temperature and pressure, or is it something different?

Mr. Nozawa: In Japan the Power Reactor and Nuclear Fuel Development Corporation is in charge of

this business. They are now doing conceptual-design studies. Our parametric surveys are just for assessing these conceptual designs, so we don't have any real fixed numbers for the prototype parameters.

Mr. Benedict: I was intrigued by the composite design for the heat exchanger which you proposed for your second design, in which you had both the intermediate heat exchanger and the steam generator in the same shell. It wasn't clear to me what the flow directions for the different streams were and how you obtain circulation for the secondary sodium system. Could you explain that briefly?

Mr. Nozawa: Yes. For this particular steam generator, we have to circulate slightly the secondary sodium. We put the intermediate sodium between the primary sodium and steam. Between the steam pipe and the primary-sodium pipe, we have some barrier plates which avoid the propagation of sodium-water reactions. We have natural convection of the sodium in the secondary system, but also we have to force this circulation to get better monitoring of the leakage from any side (from the primary side and from the steam side) and also to permit purification. The calculation has not been performed entirely. We made some conceptual designs which are not yet completed.

Mr. Ash (AI): In your annular core, what uncertainties did you build in for sodium-void coefficient? In other words, will you still have a zero or negative void coefficient under a large range of uncertainties? Or did you just use nominal-type calculations?

Mr. Nozawa: In the case of the prototype design, we have adopted the zero or negative coefficient, because with the prototype we have a small core and this is not so difficult to get. But in the case of the 1000-MW design, we have a 5000-liter core, so that a positive sodium coefficient is inevitable. In the first design, we speculated that the real sodium void must be limited to channel boiling, not to the worst configuration. Also, if we have a scram, then we can avoid major damage to the core. This is the design philosophy for the first design. In the second design, so long as scram operates, we can keep the sodium coefficient from exceeding \$1. This is our second-design philosophy. With respect to uncertainties, we have not built them into these studies yet.

E

Testing of Feasibility of Ball Bearings in Liquid Sodium Environments*

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1. Introduction

It would be convenient if ball bearings could be applied to the fuel-handling system of sodium-cooled reactors. In high-temperature sodium, oxide layers are considered to be removed from metal surfaces of ball and race. Since the surfaces of the balls are in immediate contact with that of the race, the adhesion of both surfaces may be promoted by self-welding. On the other hand, corrosion, permanent plastic deformation, fatigue, and wear may also occur on the surface of balls and race.

Mechanical and metallurgical failures may be caused by combinations of the above conditions. Consequently, the rolling moment or coefficient of rolling friction and wear may increase with time. The above failures peculiar to a high-temperature sodium environment may reduce the mechanical life and reliability of ball bearings. Therefore, ball bearings have not been adopted for use in liquid-sodium systems.

Basic sliding contact tests with bearing materials in sodium environments have been conducted.^(1, 2) However, results from ball-bearing tests have not as yet been obtained. If ball bearings are to be used in sodium-cooled reactor systems, it is necessary to perform appropriate tests in sodium.

This report contains the results to date.

2. Specimens and Materials Selection

Conventional bearing materials and high-temperature and wear-resistant materials were chosen for the radial-type ball bearings.

2.1. Bearing

The general design of the ball bearings is shown in Figs. 1 and 2. They are single-row, Conrad or deep groove, medium radial ball bearings No. 6311 (JIS),

the most popular type. The system consists of 8 balls, an inner race, an outer race, and retainers with rivets.

The reasons for these selections are as follows:

- a) The medium size can be easily face-hardened.
- b) The disassembled bearing surfaces may be easily examined after testing.
- c) The load capacity is moderate.

2.2. Materials

Material couples are shown in Table 1, and their approximate chemical compositions and mechanical properties are tabulated in Table 2.

The materials chosen for testing were known to have satisfactory corrosion resistance in a high-temperature sodium environment, except the bearing steel, which is a conventional bearing material of high-carbon chromium steel. The contact surfaces of the test specimens were generally face-hardened with the tabulated high-temperature resistance superalloys, except specimen A. The temperature variation of hardness is shown in Fig. 3.

3. Selection for Test Conditions

The test conditions for the ball bearings were chosen with the following intentions:

3.1. Sodium Temperature

The most important parameter is the sodium temperature that the ball bearings can withstand. Consequently, testing was conducted with liquid sodium at 400°C, which is nearly the reactor inlet temperature. The temperature dependence of hardness of the bearing steel comes close to that of the superalloy at 400°C and decreases rapidly above 400°C, as shown in Fig. 3.

3.2. Oxygen Level in Sodium

A contaminant in sodium such as oxygen may have an adverse effect on the ball-bearing performance. Consequently, the cold trap was operated at tempera-

* Presented at the Conference in abbreviated form at the end of Session LFRD-III, by M. Nozawa.

TABLE 3. BEARING LOAD CAPACITY AND TEST CONDITIONS

Testing	Specimen No.	Temperature (°C)	Load Capacity (kg) ^a		Axial Load (kg)	Test Period (hr)	Speed (rpm)	Total Revolutions
			C ₀	C				
Static	A-1	400	4300	5600	1000	100	—	—
	A-2	400	4300	5600	100	100	—	—
	A-3	400	4300	5600	100	100	—	—
	B-1	400	775	1760	775	430	—	—
Rotating	B-2	400	775	1760	100	100	120	0.72×10^6
	B-3	400	775	1760	775	100	120	0.72×10^6

^(a) C₀ : Radial Static Load Capacity;

C: Radial Dynamic Load Capacity.

3.4. Amount of Load

The contact area between a ball and a race is very small, and depends on the mechanical properties of the materials and the applied load. Plastic deformation at the contact areas is caused by the contact pressure, and metal flow occurs until sufficient area is obtained to support the load. Consequently, material couples are adhesive and the friction force increases greatly.

To be tested under the same conditions, all bearings were subjected to an identical contact pressure of approximately 3000 kg/cm², which is equivalent to about 1000 and 775 kg for the axial loads of specimen A and B, respectively.

Test conditions are tabulated in Table 3.

4. Test Apparatus and Test Procedures

4.1. Test Apparatus

The principal object of this test is to find the rolling friction characteristic of ball bearings fabricated from various materials, both at rest and rotating under a definite contact pressure in the liquid sodium. Two types of tests were conducted. One was a static-load test in which the ball bearings were held under load without motion for a sustained period of time. The other type was a rotating-load test in which the ball bearings were rolled under load for a sustained period of time.

Both tests were conducted in an identical test vessel in which the ball bearing was immersed in liquid sodium, subjected to an axial load, and rotated by external means. The test vessel was connected with a $\frac{3}{8}$ -in. circulation process loop, which included stop valves, an AC electromagnetic pump, a cold trap, a flow meter, and a storage tank. Above the vessel, the load-transfer-and-drive shaft, with the specimen ball bearing at the bottom end in the liquid sodium, penetrated the test vessel and was connected to the drive mechanism and the external load-

FIG. 4. External Appearance of Test Apparatus.

ing apparatus. The external appearance of the apparatus is shown in Fig. 4.

a) *Test vessel*: The load-transfer-and-drive shaft with the specimen and the test tube were installed in the test vessel. The outer race of the specimen was set in the test tube, which transferred the axial load from the specimen to the upper flange of the test vessel. Sodium filled the inside of the test vessel in such a way as to immerse the specimen, determined by level probes.

A mechanical seal was installed on the load-trans-

FIG. 5. Bearing Test Apparatus.

FIG. 6. Sodium-circulating Loop for Bearing Test.

FIG. 7. View of Loading Apparatus.

fer-and-drive shaft in order to prevent air from contaminating the vessel atmosphere. To support the shaft, the angular-contact ball bearing in duplex mounting was installed in the mechanical seal box, which was connected to the test vessel by a flexible double bellows. Consequently, the axial load transferred through the shaft did not weigh upon the mechanical seal but was immediately applied to the specimen.

The test-vessel chamber was connected to an inert-gas (argon) supply system, and a gas pressure of 0.3 kg/cm^2 was maintained during the test period. The cutaway view of the test vessel is shown in Fig. 5.

b) *Sodium loop:* Liquid sodium was driven through the test vessel at approximately 5 l/min by the AC electromagnetic pump, which circulated liquid sodium through a $\frac{3}{8}$ -in.-diameter loop that included a cold trap, economizer, valves, and flowmeter.

Sodium was supplied from the storage tank for each test. The total sodium capacity was about 25 liters. The air-cooled cold trap was installed on a bypass line in the loop and operated at temperatures between 150 and 180°C .

Electric heaters and thermocouples were attached to the storage tank, pipings, valves, and other components.

The process loop including the test vessel was installed in a steel cabinet, as shown in Fig. 4. The sodium-circulation process loop is shown in Fig. 6.

c) *Loading apparatus and drive motor:* The desired load was applied to the bearing specimen by a lever-arm system that could be adjusted by the tension of a spring scale. A $\frac{1}{2}$ -hp variable-speed drive motor (AC) with gear-head reducer rotated the load-transfer-and-drive shaft through a chain and sprockets. The arrangement for the loading apparatus and drive motor is shown in Fig. 7.

4.2. Test Procedures

The ball bearings were immersed in sodium in the test vessel after grease and fat had been removed. Liquid sodium was charged into the test vessel and the circulation process loop from the storage tank, after preheating all components, and was circulated at a maintained low temperature for purification of the sodium. After the purification, the sodium was heated to the specified temperature of 400°C while circulating. These procedures were used in all tests.

The torques or moments required to rotate the shaft assembly including the ball bearing and mechanical seal were measured. Insofar as the torque of the mechanical seal did not increase with time,

the increasing amounts of the ball-bearing torque were obtained by eliminating the initial torque for the shaft assembly from the measured torque.

The torque measurements were made under the applied load and released load every 1-3 hr.

a) *Static test*: The ball bearing was held in sodium at 400°C, subjected to the loads shown in Table 3. The torques to start motion were measured at intervals of 1-3 hr. The test was discontinued after 100 hr.

b) *Rotating test*: Tests were performed at a medium-speed rotation of the shaft assembly under the axial load.

All runs were at 120 rpm, which was a 42-cm/sec velocity at the inner race. When the torque measurements were performed at 3-hr intervals, the run was stopped, and the shaft assembly with the ball bearings was disconnected from the drive motor and gear box; then the torque was measured in the same manner as for the static test.

The run was discontinued after 0.72×10^6 total revolutions.

After concluding a test, the sodium was drained, the test vessel was cooled to ambient temperature, and the shaft assembly was removed, cleaned, and disassembled, and the ball-bearing specimen removed.

The specimens were examined for appearance, dimensional change, weight change, hardness, surface roughness, wear, and torque at room temperature.

5. Discussion and Test Results

In these tests, the durability of the ball bearings in the high-temperature sodium was estimated by the amount of wear, the coefficient of rolling friction, and the deterioration of the contact surface. This information determines the useful life and the feasibility of use of the ball bearing in the sodium.

Friction for the rolling bearing may be defined as the rotating torque or moment when one body rolls upon the flat surface. The amount of a rolling frictional torque depends upon the force with which objects are pressed together. If there is resistance between the ball and the races to prevent rolling, the static torque to overcome the frictional resistance is increased and the coefficient of rolling friction is expressed by the formula

$$f = T/P,$$

where

- f = coefficient of friction,
- T = total torque required for rotation,
- P = load which presses the objects together.

Contact stresses are a very important determining factor for the friction and wear characteristics. These were taken to be approximately identical for all specimens and tests.

5.1. Torque Measurement

The results of the tests showed very irregular friction. It was found that the setting of the drive-shaft assembly and measuring errors affected the results, which are shown in Fig. 8 to Fig. 11.

a) *Static test*: For the specimens A-1 and A-2, fabricated with bearing steel, the rotating torques increased rapidly to over 70 kg-cm under the load and could not be measured with the torque-measur-

FIG. 8. Time vs Coefficient of Rolling Friction for Specimen A-3.

FIG. 9. Time vs Coefficient of Rolling Friction for Specimen B-1.

FIG. 10. Time vs Coefficient of Rolling Friction for Specimen B-2.

FIG. 11. Time vs Coefficient of Rolling Friction for Specimen B-3.

TABLE 4. CHANGES OF COEFFICIENTS OF ROLLING FRICTION

Test	Specimen No.	Contact Pressure (kg/cm ²)	Coefficients of Rolling Friction (cm)			
			Lubricated	Initial in Sodium	After 100 hr in Sodium	Lubricated after Testing
Static	A-1	3000	Avg. 0.008	0.019	>0.1	0.012
	A-2	3000		0.017	>0.1	0.016
	A-3	1500		0.008	0.25	0.014
	B-1	2800	0.008	0.007	0.03	0.009
Rotating	B-2	1500	Avg. 0.008	0.006	0.02	0.009
	B-3	2800		0.007	0.013	0.009

ing device. For the specimen A-3 under 100-kg loading, the coefficient of friction increased gradually after 50 hr. After 100 hr, the result was approximately over 30 times that obtained initially.

The test was continued for 430 hr for specimen B-1 with a Stellite ball bearing, because the results might be affected by corrosion in the sodium environment. The coefficient of friction increased rapidly between 50 and 100 hr, only slightly thereafter.

Table 4 shows the coefficient of rolling friction at the specified conditions for each specimen.

b) *Rotating test*: The rotating test was not conducted for the bearing specimens of steel because their performances in the static test were not good.

For a load of 100 kg, the coefficient of friction changed little. For 775 kg, it increased rapidly at the initial stage and then decreased, remaining approximately constant, until it showed a tendency to decrease after 90 hr.

5.2. Appearance Examinations

The specimens A, fabricated of bearing steel, are not suitable for continued use in sodium on account of degradation of the surface roughness, and the scratches

FIG. 12. Outer Race of Disassembled Specimen B-1.

of the balls and the retainers. In particular, the retainers were scratched on the surfaces contacting the balls.

Degradation of the surfaces of specimens B was slight when compared with specimens A, but wear tracks appeared on the contact surfaces of the races. No scratch appeared on the surfaces of the retainer.

The disassembled elements of the tested specimens are shown in Fig. 12 to Fig. 14 for the specimen B-1 (static test) and in Fig. 15 to Fig. 17 for the specimen B-3 (rotating test).

5.3. Weight Examinations

Little weight changes of the specimens were observed, but it was found that the weight of the retainer of specimen A-1 decreased slightly after testing.

5.4. Surface Roughness and Surface Trace

The surface roughness was degraded by a factor of a hundred for the balls of specimen A, but for specimen B only by a factor of approximately ten for the worst conditions.

Traces for the contact surfaces of the races of

FIG. 14. Balls of Disassembled Specimen B-1.

FIG. 15. Outer Race of Disassembled Specimen B-3.

specimen B-2 were made. The maximum depth of the cave was no more than about 1μ from the initial average surface before testing and about 2μ from a rising point for the surfaces after testing. The surface traces are shown in Fig. 18.

5.5. Hardness Measurement

Measurements of the surface hardness were made. After testing, the hardness of specimen A had de-

FIG. 13. Inner Race of Disassembled Specimen B-1.

FIG. 16. Inner Race of Disassembled Specimen B-3.

FIG. 17. Balls of Disassembled Specimen B-3.

creased to the same point recorded at 400°C shown in Fig. 3. The average hardness was H_{RC} 47 for the races and H_{RC} 44 for the balls, whereas H_{RC} 62 had been obtained before testing.

Of course, no hardness change was found for the Stellite bearing, but the hardness on the contact surface of the races increased on the wear tracks. It was found that these changes were restricted to the surfaces only. It was estimated that they did not occur due to plastic deformation of the wear tracks

with the repeated pressures, but with rubbing by the balls.

The hardness distributions on the grooves of the races are shown in Fig. 19.

6. Conclusion

It was found that the conventional ball bearings could not be used in high-temperature sodium environment. The static tests indicated that the hot hardness and the strength of the bearing materials decreased with temperature effects to promote the adhesion that increased the rolling friction and caused the self-welding due to the surface roughness with a small angular rolling at torque measuring.

Better results were obtained for the Stellite ball bearings. Self-welding and wear resistance were as expected.

(a) The coefficients of rolling friction increased gradually with time for the static test, but very

FIG. 18. Surface Trace of Races for Specimen B-3.

FIG. 19. Hardness Distribution for Races of Specimen B-3.

slowly. However, for the rotating test, little change occurred after 100 hr.

(b) Wear on the rolling surfaces was observed, but it was small. The surface roughness after testing was not out of line with the tolerance.

(c) Rolling accuracies and radial clearances, which are important factors for the ball bearings, were little changed.

(d) The hardness, the structure, and the grain size of the surface material changed but scarcely after test.

Further tests with Hastelloy and other Stellites are to be conducted.

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R

Design Considerations and Experimental Program for the Common Development of a 300-MWe Sodium-cooled Fast Breeder Prototype SNR by a Belgian-Dutch-German Consortium

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1. Introduction

For about ten years, several groups in Belgium, the Netherlands, and the Federal Republic of Germany have been engaged in efforts relating to the physics, fuel-element technology, and engineering aspects of sodium-cooled fast breeder reactors. To a different degree, and partly in some specific areas only, the following have participated in this work:

in Belgium: the CEN Research Center at Mol and the firm of Belgonucléaire;

in the Netherlands: the RCN Research Center at Petten, the technical research organisation TNO at Delft and Apeldoorn, as well as the firm of Neratoom;

in Germany: the Research Center of GfK at Karlsruhe, and the firms of Interatom and Siemens.

After the basic long-term economic potential of fast breeder nuclear power stations had become apparent and after it had been possible to establish more distinctly the physics as well as the technical and financial requirements for a materialization of such installations on a commercial scale, the governments, research centers, and participating industrial firms of the countries mentioned concluded that a concerted and coordinated approach on a project of this size would be of advantage to all concerned. Consequently, in 1967 the governments of the three countries signed a memorandum of understanding with respect to a cooperation in the field of sodium-cooled fast reactors; before that date, the research centers concerned had already reached accord on a pertinent coordinated program covering associated basic research work.

The industrial consortium formed in 1966 by Siemens/Interatom, which pursues a long-term cooperation between the two firms on all fast reactor projects, was extended early in 1968 by the firms of Belgonucléaire and Neratoom for the purpose of the development and construction of a 300-MWe fast

breeder prototype nuclear power station (SNR). Recently, the Luxemburg group of Luxatom joined the work to handle tasks in a specific area. The members of the consortium have expressed their intention to cooperate to the extent possible even beyond the construction of the SNR prototype plant for commercial fast breeder projects.

With regard to the work on the SNR prototype plant, those taking part in the project accepted the following division of competences:

The research centers are to support the consortium primarily by studying fundamental problems connected with

the technology of materials (for example, the irradiation and corrosion behavior of fuel-element-cladding tube materials, fuel irradiations, and fuel studies);

plant safety (for example, sodium boiling and the ejection of overheated sodium from fuel-element channels, and theoretical studies on nuclear excursions);

reactor physics (for example, investigations using critical facilities);

reprocessing and refabrication of irradiated fuel elements.

The international industrial consortium, on the other hand, has the full responsibility for the complete design, development, and construction of the prototype nuclear power station. The initial phase of the project, which comprises also an extensive experimental and testing program, is to be completed at the end of 1969 by the submission of a binding fixed-price bid proposal on the construction of the overall plant. If favorable results can be provided, a decision with respect to a construction of the prototype nuclear power station may be expected in the course of 1970. It is contemplated that the construction of the prototype nuclear power station will be financed partly by the governments and partly by a group of power utilities in the three participating

countries. It was agreed that of the funds required Germany is to contribute 70%, whereas the other partners will pay a share of 15% each.

Organisational provisions have been made with the object of ensuring a close and effective cooperation between the partners of the industrial consortium in all areas associated with the overall project. Within the framework of such cooperation, a division of work has been agreed upon according to which in the nuclear field Siemens with Belgonucléaire are responsible primarily for the fuel element and reactor core, whereas Interatom concentrates mainly on the sodium systems and fuel-element handling. The development of essential components for the sodium heat-transfer loops is a chief responsibility for Interatom (in conjunction with Siemens/Interatom).

2. Design Considerations for the Plant

The reference design prepared by the industrial consortium with regard to the 300-MWe SNR prototype plant constitutes a partly modified advanced version of the Na-2 Design⁽¹⁾ as completed in 1967 by the Karlsruhe Nuclear Research Center (GfK) in cooperation with Siemens/Interatom.

Since the concept of plant safety as the basis of the overall planning has great significance for essential design criteria, the current philosophy relative to this problem area is discussed below in summarized form insofar as the SNR plant is concerned. With regard to the concept of plant safety for the Na-2 Design, see Ref. (2).

2.1. SNR Safety Considerations

2.1.1. General Safety Concept

There are two extreme concepts to cover the protection of the general public from the effects of nuclear accidents, namely:

- the positive control of all consequences of any conceivable superprompt critical nuclear excursion by confining such consequences to the reactor building;

- the elimination with reasonable probability of any major nuclear accidents by implementing adequate safeguards.

No doubt it will prove economically impracticable to pursue the concept named first since it is not always possible to make strictly conservative assumptions as to maximum excursions, and since the long-term nonhazardous localization of a destroyed reactor cannot be guaranteed.

The second concept certainly looks more attractive and worthwhile, because it will permit the protection

not only of the general public, but at the same time also of the capital invested in the power station. However, due to our limited knowledge of the manner in which a number of credible primary accidents for excursions take place and because of the still unknown statistical reliability of certain preventing engineered safeguards, the consistent materialization of this concept is not feasible at the present time.

Thus, with the object of warranting on a short-term and a long-term basis at least the degree of plant safety accepted for light-water reactor systems, an engineered safeguards concept was set up for the SNR plant, which comprises design provisions devised to control at least those accidents which cannot be excluded by way of passive safety precautions. Such determination is by no means to imply that in principle active safeguards would provide a higher failure probability than passive ones. However, since the prerequisites for a quantitative safety analysis based on the probability theory have not been created to date, in the case of the SNR passive safeguards have been incorporated, for their function can be verified by experiments and they may thus be assumed to be operable at any time.

2.1.2. Analysis of Accidents

Table 1 gives a survey of major incidents that may result in grave excursions along with the presumable maximum consequences to be expected in the event of any failure of the engineered safeguards measures also indicated. The consequences of accidents according to items (d) and (e) can be avoided by permanent passive safety measures (gas separator; double containment for primary system). Accidents listed under (a) and (b) will entail a thermal-energy release of 2400 MWsec involving a reactivity ramp of \$60/sec with full-load operation. The same amount of energy release may be expected in the case of an accident as under (c), if very pessimistic, though not strictly conservative, assumptions are made. In this instance some initial experimental data available give rise to the expectation that the pressure pulses due to ejection and recondensation of the sodium can most likely be taken up by the fuel-element canning structure. However, still uncertain are the influence of conceivable Na-UO₂ reactions and the effects that any deformation of the fuel-element canning structure may have on neighboring fuel elements. The results of experiments required to solve these problems, some of which are already in the process of preparation, have to be awaited before a final appraisal of this accident will be possible.

TABLE 1. INCIDENTS INITIATING SERIOUS NUCLEAR EXCURSIONS

Doppler constant: wet core -4.10^{-3} ; dry core -2.10^{-3} . Neutron flux: full power $10^{15}/\text{cm}^2\text{-sec}$; zero power $10^5/\text{cm}^2\text{-sec}$

	Incident	Max Tolerable Failure not Causing Core Damage	Assumptions for Incident Leading to Excursion	Reactivity Ramp, $\$/\text{sec}$	Thermal-energy Release, MWsec	Engineered Safeguards	Uncertainties of Evaluation
a	Loss of pumping power. Failure of shutoff system	Failure of 2 primary pumps; failure of first shutoff system	Sudden stop of all 3 primary pumps; sodium superheat $>100^\circ\text{C}$	max 60	2400	3 independent scram signals, 2 independent shutoff systems	Assumption of equal sodium superheat in each channel
b	Control-rod ejection	Ejection by normal coolant flow	Ejection of superheated sodium	60	max 2400	Hydraulic holddown in lowest position. Mechanical lock in control-rod drive	
c	Fuel-element blockage and propagation of sodium ejection	Simultaneous sodium ejection in less than 10 fuel elements	Ejection at 120°C sodium superheat in 30 elements; 1600°C surface temp	60	2400	Strong fuel-element can. Monitoring of fuel element, exit temp, and fission gas	Effects of possible Na- UO_2 reactions; scheme of propagation
d	Introduction of large gas bubble into core	Bubble size less than 30 litres	Sudden release of 160 litres of entrained cover gas into core	max 60	2400 (3900 at low power)	Gas separator below core; degassing devices on coolant pipes	
e	Meltdown of dry core by after-glow heat	—	Sudden removal of coolant from core by system leakage	5-60	2000 to 10,000	Double envelope of primary system or syphon breaker or sodium-system arrangement	Mode of core meltdown

With regard to the criteria mentioned earlier, a reference accident in accordance with (a), (b), and possibly (c) of Table 1 has been established as the basis of the design. It is characterized by a reactivity ramp of $\$60/\text{sec}$ followed by a (thermal) energy release of ~ 2400 MWsec (reactor on full power and normal sodium level in the reactor vessel).

2.1.3. Phase Sequence of Reference Accident

The consequences of the reference accident for reactor vessel and top shield are shown in Fig. 1. The strain values as stated are obtained from the quasi-static pressure buildup and from the effect of water hammer subsequent to energy release with regard to remedial measures incorporated in the design, such as the pressure-relief system, perforated immersed plate below the top shield, and bracing of the top shield by means of expansion bolts. If the main parameters of the excursion analysis are varied reasonably (for example, the time behavior of the excursion and the extent of radial damping), then the strains to which the top shield and expansion bolts are subjected will still stay within the design limits

total energy yield 2400MWsec.
perforation ratio of the dip plate 0,1
sodium mass which participates
in the fuel-sodium reaction 600 kg

FIG. 1. Consequences of Reference Accident ($60 \text{ } \$/\text{sec}$).

all times, except in the event of a major lowering of the sodium level in the reactor vessel.

The long-term consequences of a destruction of the core will be:

- a pool-type sodium fire in the primary cells and in the reactor pit (aggravated by water emerging from the concrete and by sodium evaporation due to afterglow heat from the core);

- possible penetration of molten fuel through the reactor foundation in the event that the afterglow heat cannot be adequately disposed of by radiation and dissipation to the atmosphere of the reactor building which is cooled from outside.

The complete range of measures designed to reduce or limit the effects of all conceivable accidents (in particular those listed in Table 1) is shown schematically in Fig. 2.

2.2. Layout of the SNR Prototype Plant

2.2.1. Arrangement of the Primary Loop

Following detailed comparative studies for the arrangement of the primary loop, a design comprising three heat-transfer cells was selected, each of which has pipe connections with the reactor vessel on the one hand and with the pumps as well as with the intermediate heat exchangers on the other. Although this loop concept to control the loss-of-coolant accident (case c, Table 1) requires larger expenditures than the pool concept (which has the entire primary system accommodated in a sodium-filled tank), the loop design appeared to the consortium

- to offer advantages with regard to control of the reference accident involving a thermal-energy release of 2400 MWsec;

- to entail no prohibitive disadvantage with regard to the plant cost even if the primary system would be completely jacketed (increase of total plant cost by 1-2%);

- to afford advantages as to the plant availability in the event of necessary repair or maintenance work on one loop;

- to involve less technical risks (in particular, with respect to the experience derivable from the KNK reactor station presently under construction);

- to incorporate more development potential both with regard to the construction of installations with large outputs and with a view to a possible elimination of the secondary sodium system.

Ensuring the cooling of the core even after rupture of a primary pipe has a decisive bearing on the layout

and circuit arrangement of the primary loop. The concept of the Na-2 design which provided for an emergency cooling and a sodium-supply system as well as for free-volume suppression in the primary cells was abandoned because such measures appeared to be expensive; at the same time they largely impaired both the accessibility and repairability.

If, as a protection against the grave loss-of-coolant accident, one does not want to rely solely on active devices, i.e., those for which a successful action is required, then the only alternatives left will be the concept of completely jacketing the primary loop (with relatively great flexibility as to the arrangement of the plant components) or, under certain conditions, arranging the entire (unjacketed) primary system above the emergency-coolant level of the reactor vessel.

For the SNR prototype plant the double containment was selected despite the higher capital investment involved and the development work, as well as the experimental testing yet required, because this system will permit

- the most reliable protection in the case of very unfavorable assumptions as to the phased sequence of an assumed accident;

- dispensing with the requirement for using an inert gas cover in the primary cells;

- the rapid detection and localization of minimum sodium leakages (which, if humidity is added, may result in serious damage to pipe work because of corrosion effects);

- the safe preheating of the primary system by means of hot gas;

Fig. 2. Safeguard Measures (Schematic).

FIG. 3. Primary System.

the avoidance of cell contamination in the event of minor leaks in the primary system and the ability of continued operation of the plant in such an event over a limited period of time.

Figure 3 indicates the primary system layout.

2.2.2. The Double Containment for the Primary Loop

The jacketing system in its most conservative version must be able to meet the following requirements, among others:

- integrity even in the event of maximum operational strains (e.g., thermal shocks) and under the most unfavorable failure of primary piping under stress;
- maximum sodium leakage inside the annular gap must not be larger than will be permissible with regard to keeping up an emergency sodium level in the reactor vessel;
- no permanent contact between primary system and jacketing system under all feasible operating conditions.

With respect to all problems of the design and installation that may arise from such requirements, technically satisfactory and economically acceptable solutions have been found in the meantime; however, in some instances such solutions are still in need of verification by way of experiments. An important source of practical experience is expected from the construction of a large sodium pump test facility (see Sect. 3) which is to incorporate all essential design features with regard to jacketing.

The present concept of a tight double wall of great strength constitutes a maximum solution which can either be simplified step by step or even be dropped altogether with increasing knowledge of the most unfavorable failure conditions of pipework and with growing experience as to the reliability of active safeguards measures. The prerequisites to that effect are listed in Table 2.

2.2.3. Essential Plant Components

The reactor vessel and its rotatable top shield are shown in Fig. 4. The essential internals of the vessel include:

- below the grid plate, a device for trapping any gas bubbles entrained by the coolant (compare accident d in Table 1). The separated gas is conveyed to the upper plenum via the breeder elements;
- above the grid plate, the reactor core consisting of free-standing fuel elements which are individually held down by hydraulic means (for core data see Table 3). The core is surrounded by a breeder zone, followed further outward by the steel-shielded decay storage positions for core elements;
- above the core, a design item fitted to the rotatable top shield, comprising: (a) a perforated immersed plate (see reference accident in Sect. 2.1.3); (b) a device for positioning thermocouples above each single fuel element (at the same time serving as a mechanical stop against lifting of

TABLE 2. DESIGN REQUIREMENTS FOR PRIMARY LOOPS TO CONTROL LOSS-OF-COOLANT ACCIDENT
(No auxiliary cooling loops)

	Rapid, Large Rupture of Coolant Pipe				Envelope (Double Wall) Required for Parts of Primary Loops Located	
	Considered Possible	Severe Mechanical Effects of Rupture	Excessive Sodium Loss due to Pump Coast Down	Quick Action not Reliably Possible ^(a)	Below Sodium Emergency Level	Above Sodium Emergency Level
1	x	x	x	x	Tight and strong	Quasi-tight and strong
2	x		x	x	Tight	Quasi-tight
3	x		x		Quasi-tight (tight up to shutoff valve)	Quasi-tight
4	x	x		x	Tight and strong	No
5	x			x	Tight	No
6	x				Tight up to syphon breaker or fast shut-off valves	No
7					Tight up to slow-acting valve	No

^(a) For example, leaking loop cannot be rapidly identified and actuation of syphon breaker or high-speed shutoff valves is not reliable enough.

FIG. 4. Reactor Vessel and Associated Equipment.

fuel elements); (c) a linkage for guiding the control rods and the opening for the fuel-element change.

Under normal operating temperatures the arrangement of core elements is kept under tension, via spacer pads on the fuel-element boxes, by a ferritic steel ring which embraces the core. The core contains a total of 18 absorber positions. Of these, the 12 rods

of the outer zone belong to the control shim system; three rods of the inner zone belong to the first scram system; the three other rods of the inner zone belong to the completely separate second shutdown system, which is of an entirely different design. (The requirements which the control and shutdown systems have to meet are summarized in Table 4.)

The fuel-handling system is comprised of two stor-

TABLE 3. CHARACTERISTICS OF CORE AND BLANKET

Fuel of Core (PuO ₂ -UO ₂)	
Enrichment (2 zones)	17.2/25.5% fiss.
Inventory of fissile material	0.87 t Pu fiss.
Density	80% of theoretical
Pu 239/240/241/242	75/22/2.5/0.5%
Performance of Core	
Average fuel rating	760 MW/t fiss. material
Max nominal rod rating	420 W/cm
Breeding ratio/doubling time	1.33/10.9 full power years
Max/avg burnup	80,000/55,000 MWd/t fiss. material
Design Data of Fuel Elements	
Core (ϕ : 1.5 m)	150 elements, each consisting of 169 rods of 6-mm diameter
Blanket	312, each consisting of 91 rods of 9.5-mm diameter
Overall/active height	3.17 m/0.95 m
Distance across flats	110 mm

TABLE 4. REACTIVITY REQUIREMENTS FOR SHUT OFF SYSTEMS

Operation		Na-void Accident	
Cold (200°C)—hot	1.6 \$	Sodium void	6.5 \$
Hot-full power	1.4 \$	Fuel slumping	2 \$
Burnup (equil.)	8 \$	Uncertainties	6 \$
Uncertainties	5 \$	Subcriticality	3 \$
Subcriticality	1 \$		
Total reactivity requirements	17 \$	Total shut off requirements	17.5 \$
Shut off reactivity at full power	4 \$	Available from control/shim system	4 \$
		Requirements for scram system	13.5 \$
Control/shim-rod system		First scram system	
12 rods (Ta) outer core zone	19 \$	3 rods (42% B-10) inner core zone	14 \$

Failure of both control/shim and first scram system:
 second scram system
 3 rods (60% B-10) inner core zone: 18 \$.

age areas and two handling machines. Fuel elements are transported between the core and the in-vessel decay-storage positions under sodium by a special transfer machine that is fixed to the top shield. The in-vessel decay time is about 6 months. During the transport from the in-vessel storage area to the gas-cooled fuel-element storage vessel in the fuel-handling

building by the fuel-handling machine the element can be cooled with argon.

The intermediate heat exchangers are straight-tube units so designed as to permit the identification of leaking tubes in the bundle without dismantling and cleaning the entire heat exchanger (see Fig. 5).

For the reference design of the once-through steam generator a straight-tube concept was also adopted. In addition, a design with spiral tubes and a smaller output per unit is being developed and tested as a backup solution. In the former case, evaporator, superheater, and reheater are separate items of equipment, whereas the backup design combines evaporator and superheater in one vessel (see Fig. 6).

The operating conditions of the steam generators have been collected in Table 5, together with the general data for the heat-transfer loops. Connected to the steam generators is the pressure-relief system which prevents the buildup of a dangerous reaction pressure in the secondary loop and disposes of both the reaction products and the ejected sodium in a safe manner.

FIG. 5. Sodium-to-Sodium Heat Exchanger.

FIG. 6. Steam Generators.

3. Basis of Experience, Development, and Testing Program

3.1. Reactor Physics

Despite their continuous refinement with respect to methods and nuclear input data, in many cases the theoretical means available for determining the nuclear characteristics of a fast reactor core permit merely results of limited accuracy. Though the uncertainty in calculating the critical mass can possibly be compensated without substantial loss of core output by changing somewhat the ratio of the number of the two fuel-element types with different enrichment, nevertheless essential issues such as

reactivity values of the various types of absorbers in the control and scram systems,

details of the neutron-flux behavior (i.e., the power density distribution), particularly in the vicinity of boundary layers,

reactivity values of the sodium at different locations within the core and integral void-values, and

Doppler coefficients inasmuch as an accuracy of better than $\pm 20\%$ is wanted,

can be computed only with an insufficient degree of accuracy.

Early in 1967 a facility for critical experiments (SNEAK)⁽⁴⁾ was taken into operation by GfK, Karlsruhe, for the nuclear simulation of large-size fast

TABLE 5. MAIN TECHNICAL DATA OF SNR PLANT

Power	730 MWt/300 MWe net
Primary System (3 loops)	
Sodium temperatures	380°C inlet/560°C outlet
Max surface temp of fuel element	700°C
Pressure drop/sodium flow rate	6 atm/14,000 m ³ /hr
Secondary System	
Sodium temperatures	IHX: 340°C inlet/540°C outlet. Steam generator: 525°C inlet
Pressure drop/sodium flow rate	5 atm/12,750 m ³ /hr
Steam System	
3 x 4 steam generators	52 MW and 91 t/hr each (195 atm/285°C)/(170 atm/510°C)
Pressures and temp inlet/outlet	
3 x 2 reheaters	17 MW and 148 t/hr each (44 atm/325°C)/(42 atm/500°C)
Pressures and temp inlet/outlet	

plutonium cores (see Fig. 7). After numerous, chiefly basic critical experiments have been performed, during 1969 the reactor core of the SNR (due to the limited amount of plutonium available in a sector

FIG. 7. Facility for Critical Experiments: SNEAK.

FIG. 8. Test Vessel for Explosion Experiment.

FIG. 9. Facility for Sodium-Water Reaction Experiments: ASB.

experiment only) is scheduled to be investigated with regard to the above-mentioned problems.

In order to obtain experimental results regarding the behavior of fast cores also during nuclear excursions, GfK along with EURATOM decided to participate in the construction of the SEFOR reactor.⁽⁵⁾

3.2. Plant Safety

3.2.1. Experiments Related to the Avoidance or the Control of Nuclear Excursions

If the SEFOR experiments serve the purpose of investigating the nuclear excursion behavior of a fast core itself, a number of non-nuclear tests are destined to remove existing uncertainties as to the positive exclusion or limitation of accidents leading to excursions or else to eliminate or reduce the consequences of excursions which have taken place.

Investigations concerning the mechanics and typical data of the ejection of superheated sodium from tube-shaped channels have been performed since 1966 at the Ispra Research Center of EURATOM,⁽⁶⁾ partly in close cooperation with GfK. From June 1968 onwards, GfK too has started to perform its

TABLE 6. CHARACTERISTICS OF FACILITY FOR SODIUM-WATER REACTIONS

Sodium System	
Temperature/pressure	550°C/30 atm
Pressure drop and flow rate	8 atm/120 m ³ /hr
Heater/cooler performance	150 kW/100 kW
Water System	
Temperature/pressure	370°C/280 atm
Volume of water to be injected	Max 0.5 m ³
Rate of temp change of injected water	Up to 100°C/sec
Data of Experimental Equipment	
Sodium inventory of models to be tested	Max 2 m ³
Leak for tube-rupture experiments	Max 20-mm diameter
Leak size for experiments to determine pin-hole effect	0.2-0.5 mm in diameter

own pertinent experiments (initially using one single rod, but beginning in 1969 also with a rod cluster). The chief objective of the tests is a clarification of

the problem of propagation probability of the coolant ejection from a blocked fuel element.⁽⁷⁾

Of equal significance for plant safety are experiments which the consortium has under preparation concerning the inherently safe separation of large gas bubbles from the sodium coolant, as well as on the reliability of the double-wall primary system under different extreme strain conditions due to failure of the primary containment. Regarding both these problem areas, important interim results are to become available in 1969.

Model tests on the subjects of phenomenology and pressure-vessel loads during explosions adapted to the typical time behavior of major nuclear excursions are being prepared by one member of the consortium (the test vessel is shown in Fig. 8). Experiments performed so far were devoted to the development and testing of explosives with suitable time behavior during the explosion.

The question remains to be clarified whether and, if so, to what extent tests as presently envisaged by

both GfK and the consortium to improve and extend the core instrumentation will have a bearing on the prototype reactor.

3.2.2. Experiments to Control Sodium-Water Reactions Occurring in the Steam Generator

Since 1964 one member of the consortium has carried out a large number of tests concerning the manner in which sodium-water reactions take place, in order to study the safe protection of the secondary system against hazardous pressure surges.⁽⁸⁾ In the summer of 1968 a new large-scale facility (see Fig. 9) was operated. This facility permits the performance of experiments simulating major and minor water leakages into sodium at variable temperatures and pressures, as well as various geometries of the reaction vessels (for data for the facility see Table 6).

The facility, presently used at regular intervals to conduct about 2-3 tests per month involving sodium quantities in the range between 2 and 300 kilograms, furthermore permits the testing of the behavior of steam-generator prototypes in the event of water-tube ruptures. The first of two steam-generator types at present under development (see section 2.2.3) is scheduled to be subjected to a sodium-water reaction test yet before the end of the current year (amount of sodium to be used for the test will be approx. 2000 kilograms). Fig. 9 gives a partial view of the test facility (cyclone side) during a sodium-water experiment in which 250 kilograms of sodium were involved. Design details of the facility and test results will be reported to this conference in the context of another paper.⁽⁹⁾

3.3. Fuel-element Development

3.3.1. Cladding Material for Fuel Rod

Since 1965 different types of austenitic steels, in particular those with a content of 16% chromium, 13% nickel, and different alloying additions, have been examined by GfK for their in-pile irradiation behavior.⁽¹⁰⁾ As to the fuel-rod cladding of the SNR prototype plant, a decision will be taken in 1969 on the basis of the results then available. It is likely that one of the following two materials: 16 Cr, 13 Ni, 1 Nb (medium strength, high ductility) or 16 Cr, 13 Ni, 1 Nb, 1.3 Mo, 0.75 V (high strength, low ductility), will be selected. In this context, the results of corrosion tests in sodium performed with the said and similar materials by TNO in the Netherlands and CEN in Belgium will also have to be taken into consideration. Additional experiments of this kind have been anticipated by GfK and the consortium.

FIG. 10. Fuel-element and Control-rod Test Facility.

3.3.2. Fuel Rods

Irradiation tests of capsules containing mixed $\text{UO}_2\text{-PuO}_2$ oxides have been conducted by GfK and CEN since 1965. GfK and CEN assisted by the consortium have prepared a cluster of 39 fuel pins for an irradiation experiment in the Dounreay Fast Reactor. The cluster was inserted early in October 1968.

TABLE 7. CHARACTERISTICS OF FUEL-ELEMENT AND CONTROL-ROD TEST FACILITY

Temperature/pressure	650°C/14 atm
Flow rate	750 m ³ /hr
Thermal-shock conditions	200°C/15°C per sec
Heater/cooler capacity	0.75/1.2 MW
Material	Type 316 stainless steel
Operation Characteristics	
Temperature cycling (10 min/cycle)	580 ± 40°C, 50°C per sec
Pressure cycling (1 min/cycle)	4 ± 3 atm

It is expected that after an irradiation time of about one year a burnup of $\sim 50,000$ MWd/t will have been reached and that preliminary results as to the irradiation behavior of the various materials used in the manufacture of this fuel cluster will become available. The decision to proceed with the construction of the SNR prototype plant is considered to depend essentially on the successful completion of this experiment.

3.3.3. Fuel Elements

For determining and testing the non-nuclear properties of the complete fuel-element assembly, a large test facility (see Fig. 10) is under construction by one member of the consortium; completion of this facility is scheduled for 1969. Technical data of this facility, which can be utilized also for the testing of the control rods, are given in Table 7. Two varieties of the newly developed spacer design (spark-eroded, brazed metal-strip), of which several prototype specimens were manufactured, and fuel-rod-positioning plates have

FIG. 11. Sodium-cooled Experimental Reactor KNK at Karlsruhe.

FIG. 12. 5-MW Sodium Test Facility.

TABLE 8. CHARACTERISTICS OF SODIUM PUMP TEST FACILITY WITH REACTOR MOCKUP

Temperature/pressure	650°C/10 atm
Flow rate first pump/large pump	5000/20,000 m ³ /hr
Thermal-shock conditions	±180°C/10°C per sec
Reactor vessel mockup (diameter/length)	5/12 m
Double rotating top shield (diameter/weight)	5.50 m/200 t
Total sodium content of system	150 m ³
Material	Type 304 stainless steel
Main test objects	2 pumps (5000 m ³ /hr) of different design; Large sodium valves; Fuel-handling equipment; Prototype rotating top shield (actual size); Large-diameter pipes using double-wall concept

been subjected to initial hydraulic tests. For the fuel-element developmental program, see Ref. (11).

With the object of a final evaluation of the behavior of complete fuel-element assemblies under representative reactor operating conditions it is contemplated to insert, as from about mid-1971, in the sodium-cooled experimental reactor KNK at Karlsruhe⁽³⁾ 6 or 7 mixed-oxide SNR fuel elements as part of a fast core with a thermal output of 60 MW.

3.3.4. Investigation and Fabrication of Plutonium Mixed Oxides

GfK has had suitable facilities available for the fabrication of irradiation samples since 1966. Sub-

stantial quantities of mixed oxides (170 kg of plutonium) have, furthermore, been manufactured since 1966 by ALKEM within the premises of GfK for use with the critical experiments in the SNEAK facility. Other facilities for the manufacture and investigation of mixed oxides have been in operation for extended periods of time by one member of the consortium.

3.4. Sodium Technology

The basis of the development and testing of sodium components and of sodium systems for the SNR prototype plant is the developmental program for the sodium-cooled reactor facility KNK at Karlsruhe (see Fig. 11), as conducted by one member of the consortium between 1960 and 1967. The successful startup of this 20-MWe experimental nuclear power station, constructed by the same member of the consortium under turnkey contract, will be one of the prerequisites for obtaining a decision on the construction of the SNR prototype station. Completion of the erection and installation of the KNK facility is scheduled for early in 1969. Following the subsequent functional testing and zero-power experiments the commencement of power operation is envisaged for the end of 1969 or early 1970.

Development and testing of essentially all of the components and systems for the KNK reactor plant (such as pumps, intermediate heat exchangers, steam generators,⁽¹²⁾ valves, auxiliary sodium systems, sodium instrumentation, control rods, core internals, fuel elements, rotatable top shield, and fuel-element handling installations) have been performed in a sodium test facility with a thermal output of 5 MW, commissioned early in 1965 (see Fig. 12). The test facility has been run partly in continuous operation, partly under transient conditions, and partly to test

FIG. 13. Sodium Pump Test Facility with Reactor Mockup: APB.

reactor-vessel nozzles and valves, with extreme thermal shocks (up to $\pm 120^{\circ}\text{C}$ at $15^{\circ}\text{C}/\text{sec}$).

Although the said facility will continue to serve also for the SNR program (among other uses for testing the backup design of the steam generator as referred to in Sect. 2.2.3 from the end of 1968 onward),

TABLE 9. CHARACTERISTICS OF 50-MW STEAM GENERATOR TEST FACILITY

Sodium System	
Heater capacity	58 MWt
Temperature/pressure	$650^{\circ}\text{C}/12\text{ atm}$
Flow rate	$1800\text{ m}^3/\text{hr}$
Thermal-shock conditions	$5.5^{\circ}\text{C}/\text{sec}$ for 20 sec
Heat transfer in IHX (regenerative)	70 MW
Material	Type 304 stainless steel
Steam System	
Temperature/pressure	$600^{\circ}\text{C}/210\text{ atm}$

testing of essential major components for the SNR prototype plant will take place in two large-scale test facilities of the consortium. One of the facilities, consisting of two parts, will serve both for the testing of large sodium pumps and for experiments with important reactor components (full-scale rotatable reactor top shield, fuel-element-handling equipment, etc.). The chief technical data of this facility have been compiled in Table 8. The status of construction work as of the beginning of October 1968 is shown in Fig. 13. The facility is scheduled to start operation in mid-1969.

Experimental investigations on a number of particular problems in the sodium technology field (for example, sodium condensation in gaps, development of high-temperature rupture discs with a long service life for the pressure-relief system of the secondary loops, and behavior of seals and bearings when exposed to sodium⁽¹³⁾) have been carried out by means of several small sodium rigs, some of which have been in operation for long periods of time already.

FIG. 14. 50-MW Steam Generator Test Facility.

The second of the two large facilities mentioned is to be used preferably for the testing of intermediate heat exchangers and sodium-heated steam generators for outputs up to 50 MWt. The main technical details of the facility which is to be taken into operation by the end of 1969 have been accumulated in Table 9. Figure 14 shows the status of construction work as of October 1968.

In connection with intermediate heat exchangers and steam generators, both of which incorporate the design feature of repairable and replaceable straight tube bundles, a range of investigations is underway.⁽¹⁴⁾ Among the developments in the field of new fabrication techniques two achievements may be mentioned:

development and testing of different types of spacers (brazed strong grid, point-welded grid) for easy installation and positive suppression of vibrations;

development and testing of a design insensitive to thermal shocks and of a reliable welding process for the connection between tube and tube sheet, as well as of methods for testing welded joints (special mini X-ray apparatus, dye-check helium test).

At an early date the consortium had taken up also the question as to what long-term developments are apt to result in further improvements of the economics or operational safety of sodium-cooled fast breeder reactors. Perhaps the most important suggestion in this direction concerns the elimination of the secondary sodium loops. A concept of this kind, for which a loop design of the primary system will be a prerequisite, would require the development either of an inherently reaction-protected steam generator or—since a gas-turbine process based on CO₂ as the working medium is considered to entail acceptable degrees of efficiency in view of the sodium temperatures available—of a suitable sodium CO₂ heater.⁽¹⁵⁾ Both approaches are being pursued by the consortium for their feasibility under theoretical design and experimental aspects. However, at the present time neither of the two alternatives is expected to be applied to the SNR prototype.

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Discussion

Mr. Denielou (CEA-Cadarache): I see in the first column of Table 1 many incidents which seem very independent, such as control-rod ejection and introduction of a large gas bubble into the core. They have nothing to do with each other. But when you look at the assumptions for incidents (reactivity ramp and thermal-energy release) you always find \$60 per second and 2400 MW-second. Is this by chance?

Mr. Harde: I must say that psychologically this is not very well done. It only reflects a certain degree of uncertainty, which should be marked by indications of maximum in some cases and not in others. Actually, it sometimes depends on your model where you end up. Except for the first case, the reactivity-insertion rates shown are conservative. For example, it may have been 30 to 60, and it might have been a better practice really to put it that way.

Mr. Hobson (W): In Table 1 it appears that the siphon breakers are a passive element; then in a later

figure, it appeared that they might be an active engineered safeguard. Are they passive or active, and could you give us some more detail on where they are and what they are?

Mr. Harde: We are not relying on siphon breakers; this is a general statement. We chose the only passive safeguard possible, and that's a double envelope or the proper location of the design.

Mr. Cobb (Nuclear Power Group, UK): My question also relates to Table 1. What mechanism is it that you consider could suddenly release 160 liters of cover gas into the core?

Mr. Harde: Essentially, there are three means to achieve this, not all of them leading to 160 liters. The mechanisms considered are: first, the accumulation of transported cover gas and the sudden release by a change of operating mode; of course, against this, the gas-separating devices or the degassing devices of the pipes is an engineered safeguard. Secondly, if you have a major pipe rupture, then as long as no flow reversal occurs, you get suctioning of cover gas from the inert gas from the heat-transfer cells, or if you have a double envelope, from the gap. This would not come up to 160 liters, but it is limited to something like 30 or 40 liters before flow reversal occurs. The third mechanism conceivable is the sudden release (again this requires quite a bit of imagination) of the stored maximum amount of fission gases in the fuel elements (it's independent where the plenums are, but they are located below the core). If all the 100 or 200 rods come off this would mean a similar situation.

Mr. Loewenstein (ANL): Can you comment on your plans for subsequent loadings in the KNK reactor?

Mr. Harde: Yes. As I indicated, the first loading will be a thermal core using zirconium hydride for moderation. This core will be inserted in September 1969 and taken into operation early in 1970. Presently, we are preparing designs for a fast core that would consist of two fast core regions, an outer driver region using highly enriched U^{235} and which is slightly moderated, and a central zone consisting of seven elements which have the exact composition metallurgically of a large fast breeder fuel element, but which are enriched with regard to the uranium in order to achieve sufficient criticality. Actually, the softening of the spectrum in the driver zone is mainly for safety reasons in order to increase the Doppler coefficient. This core is presently being mocked up in the critical facility in Karlsruhe.

Mr. Okrent (ANL): If I understand Table 1 correctly, for the first three cases—a, b, and c—where a large thermal-energy release could occur, you propose

to design to cope with such a thermal-energy release. Is the core relatively intact or is it disrupted after this kind of energy release? If it's disrupted, what modes of heat transfer do you plan to institute to keep the fuel from working through the containment barrier?

Mr. Harde: Of course, the core will certainly not be in good shape after such an accident. As a matter of fact, we imagine it will not be in a shape to be cooled effectively at all. We will be in a situation similar to that after meltdown of a large water-cooled thermal plant. In other words, if we are lucky, the total heat being generated will be given off by radiation, circulation, and so forth, and could be cooled by the cooled containment. If we are not lucky, in the long run the fuel may melt down, and this may require some countermeasures. This situation is, I believe, very similar to that incurred with a light-water reactor.

Mr. Okrent: If one is coping with the first phase of this event which leads to disruption of the core, should one not make equivalent efforts to cope with the second phase? This is the sense of the question.

Mr. Harde: The short-range consequences have to be coped with by all means since nobody really can be evacuated, for instance, within say a few minutes or so. If a meltdown through the base of the reactor building should really occur, something like evacuation should be taken into account. I understand that in this country investigations have been carried out as to the behavior of the molten core. The conclusions, as I understand, have been that there is really nothing that can eventually prevent it from doing something like melting through. Is that correct?

Mr. Okrent: I believe that the published studies which relate to molten fuel in water are not optimistic about one having a grasp on enough of the processes going on to discuss the course of events with confidence. I was wondering whether you have looked at the disrupted core in sodium to see whether you could, in fact, take the heat away in a satisfactory manner.

Mr. Harde: In not quite a conservative manner we have a reactor pit with space suppression which would enable the sodium to stay there for an extended period of time. If, for instance, evaporation would occur, with recondensation at some other place, and flowing back and forth, you could think of some mode of cooling. In order to do some calculations you need to know some geometry, some configuration, and nobody knows what the configuration of the core will be here. I sincerely think that quite a bit of work has yet to be done in that direction; there is a lack of information, I believe, that is fundamental.

Mr. Melese (GGA): Could you please tell us what is the internal conversion ratio of your prototype, the time between successive refueling, and do you have partial or batch modes of refueling?

Mr. Harde: Will Mr. Guthmann answer that for me?

Mr. Guthmann (Interatom): Our refueling intervals are six months. The doubling time is 10.9 yr at full power; not including the external inventory. I don't know the internal conversion ratio. (Mr. Harde: It should be around 0.8 or 0.850.) Also, we have partial reloading.

Mr. Lagowski (APDA): I wonder if you could comment on the design basis for the reactor building—the double-containment building, in view of the fact that you're confining the energy release from the hypothetical accident below the operating floor and keeping it in an inert environment. Is the building designed for sodium fire and/or plutonium aerosols, for example?

Mr. Harde: The lower region below the upper floor is designed to withstand both short-term pressures as well as missiles. There is an inert atmosphere in the pit about the reactor shield and no short-term fire is actually expected; if you look at the long-term consequences, of course it cannot be guaranteed that the whole thing remains intact. We have designed for a pool fire in, I believe, at least two or all three heat-transfer cells; this will give a pressure rise in the total building up to 2 atm, about 3 atm absolute, and the surface of the inner containment will assume a temperature of about 200°C. This requires operations of the cooling system that I indicated.

Mr. Lagowski: Is the plutonium aerosol problem involved in that?

Mr. Harde: We are just in the process of evaluating this one. This comes to the question: "What kind of filtering system may you have for the intermediate space in between?" This is still under investigation.

The Sodium-CO₂ Fast Breeder Reactor Concept

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I. Introduction and Summary

Present fast breeder design calls for a 3-step system for removal of heat (see Fig. 1), consisting of a primary circuit of radioactive sodium, a secondary circuit of nonradioactive sodium, and a water-and-steam prime mover circuit. Although the reactor core is safely protected from chemical, mechanical, and reactivity effects of a sodium-water reaction by such a system, the operation of the plant depends on the number of leaks occurring and the seriousness of the damage they cause to the tube bundle. Moreover, the corrosive properties of the reaction products under certain conditions can cause further leaks to appear after repair. Probability of leakage is increased in the sodium steam generators because of corrosion and of the changing and intense heat exchange in the area of the boiling zone.

It is suggested that both the secondary and the water-and-steam circuits be replaced by a single CO₂ prime mover circuit (see Figs. 2 and 3). Indeed, although the sodium-CO₂ chemical reaction is exothermic, a hydrogen molecule (which could not be contained in the system) is not liberated. The reaction products, sodium carbonate and free carbon, are not readily corrosive.

A preliminary investigation of such a concept has been made and is being pursued in more detail. First, the reaction has been performed with small samples, and it has been found that pressure and temperature have a marked effect on its initiation, behavior, and degree of completeness, although the action of the pressure parameter is most probably due to the stirring effect during the injection. One must therefore provide (a) for means of filtering out both reaction products as well as the CO₂ that has not reacted (de-entrainment of bubbles which should not reach the core) and (b) for instant removal of the CO₂ in case of a large rupture, which should not be allowed to pressurize the primary circuit containing radioactive sodium (removal of CO₂ plus argon and gaseous fission products can be achieved through CO₂-absorber units and additional cover-gas containers). The relatively low sound velocity in CO₂ under the prevailing conditions provides safety in limiting the gas flow rate

in case of a major tube rupture. This is all important for the design of an emergency disposal system.

CO₂ has been preferred to other gases only because its thermodynamic properties are such that it is not required to heat it up to extremely high temperatures in order to obtain a good efficiency in a closed cycle. Indeed, its heat of compression near the saturation temperature is small, and that temperature is close to the normal temperature of cooling water in steam plants.

The use of CO₂ as a prime mover has been investigated, referring to studies made by different authors. The need for accurate CO₂ thermodynamic data in the region of the (*s*, *T*) diagram close to the saturation curve has been demonstrated. Three possible cycles have been considered: (a) a simple 100- to 150-atm direct cycle, using a compressor, a recuperator, and a turbine, exhibiting moderate efficiencies (35 to 40%); (b) a reheat cycle, requiring a higher pressure (180–250 atm) but well-fit to reheat through sodium and exhibiting high efficiencies beyond 40%; (c) a very high-pressure cycle (250–300 atm) with flow derivation exhibiting better efficiencies, but penalized by thick-walled components and by regulation and control problems. The efficiency of the first two cycles can be increased in the future by crossing the saturation line and pumping more dense CO₂ with lower energy requirements, once the startup and stability problems of such an operation have been solved with a demonstration unit.

The machinery to be used can be readily developed from former experience with steam turbines, CO₂ circulators, and air compressors. It results in compact, small-diameter units, which can be easily brought in the future to very large unit sizes, as the low pressure of all cycles is well above 1 atm. Most of the investment for cycle equipment is therefore not in the turbomachinery, but rather in the Na-CO₂ heat exchanger and safety devices, recuperator, and gas connections. This is the reason why an extensive preliminary investigation is required before drawing conclusions as to the industrial potential of this new concept. Such an investigation has been decided and will yield results in 1969, when a de-

FIG. 4. Sodium-Carbon Dioxide Compatibility.

II. The Sodium-CO₂ Reaction

The theoretical investigation of the reaction shows that it is at least as exothermic as the sodium-water reaction, and that it should be principally influenced by the temperature of the reagents.

Two schemes are possible: a complete one:

and a less complete one:

It was feared that under almost all circumstances only a fraction of the CO₂ would react, causing a great problem of gas entrainment as well as pressurization of the primary system. For that reason, and because the reaction behavior could be influenced by the stirring effect of the pressure injection of CO₂ in almost stagnant sodium, a series of preliminary tests were conducted to frame the problem.

A heated autoclave, containing sodium quantities of the order of 50 cm³, was pressurized at will with dry argon, while dry CO₂ was injected under pressure. A series of tests were performed in which the

pressure in the autoclave as well as the temperature of the wall was monitored. They are reported in Fig. 4. They confirmed that one should expect only partial reactions. One should therefore be prepared to take the following actions:

keep the water content of the CO₂ system low, using present CO₂ reactor technology in order to avoid sodium-water reaction as well as corrosion of the steel system;

provide in the Na-CO₂ heat exchangers ample space to collect the non-reacting CO₂ fraction, in order to allow time for the following actions:

- detect the leak;
- lower the CO₂ pressure, using the pressure-regulating or other dump tank;
- vent the heat-exchanger gas blanket, as soon as required, to the blanket-gas dump tank, possibly through a CO₂-absorber unit;
- isolate the CO₂ side of the heat exchanger;
- de-entrain the gas bubbles and filter the reaction products in an integrated de-entrainment and filtering unit included or not within the shell of heat exchanger.

After the incident, the heat exchanger must be

tested, which means that both tube sheets must be readily accessible, say through openings in the gamma shield. Then, one can either plug the defective tubes or remove the heat-exchanger tube-bundle for cleaning, drying, decontamination, and repair.

The present program covers further investigation of both reaction theory and practice. The installations available to the Consortium and used up to now for studying the sodium-water reaction will be used for that investigation. In addition, a new loop is being erected in which bubble formation and de-entrainment, as well as filtering out of the reaction products, can be studied in simple heat-exchanger geometries.

Preliminary tests have shown that the pressure effect of a ruptured tube is intermediate between those obtained when an inert gas or steam are being used, which is to be expected as the inert gas brings no reaction and therefore no heat, whereas steam brings both heat and additional gas molecules.

One cannot expect to design de-entrainment devices of 100% efficiency. Therefore, tiny bubbles could collect in some parts of the primary system and, at a given time, enter the reactor core inlet plenum. But the same is true for the argon blanket

gas, and the SNR prototype is already equipped for that reason with gas dividers to avoid damaging incidents.

Filters, too, will not be 100% efficient, and some very fine particles of both sodium carbonate and free carbon will be entrained. There is no reason why they should collect preferentially in the core. However, the free carbon might be slowly dissolved in the sodium. Dissolved carbon is also expected to be present in the sodium system of the SNR prototype, which will eventually be equipped with appropriate means for carbon removal, in order to avoid cladding carburization, if this effect is shown to be significant through the life of core and blanket fuel assemblies.

In conclusion, one expects the reaction to be manageable both for large and small leaks. Since self-propagating effects due to accelerated corrosion are not present in the case of the Na-CO₂ reaction, it is even expected that a substantial amount of small leaks will be tolerable.

III. Gas-Turbine Cycles Using CO₂ as a Prime Mover

There are several types of cycles which can be foreseen in the particular case of a secondary CO₂ gas-turbine loop connected to a primary sodium circuit.

The choice of the reference cycle is a matter of economical optimization which is a balance between thermodynamic efficiencies of the cycle and capital costs, leading to the lowest possible kWh cost.

For the rather low maximum temperature range of CO₂ in the case of a Na-CO₂ concept (520 to 575°C) there is a tendency to use high pressures and rather elaborate systems in order to reach cycle efficiencies comparable to those of steam circuits; but this must be limited by economical considerations.

Three basic CO₂ cycles were considered in a preliminary comparison and optimization study:

- a simple cycle (see Fig. 5) with two or three compressors, at high-pressure range of 100 to 150 atm;
- a reheat cycle (see Fig. 6) with mainly three compressors, at a high-pressure range of 180 to 250 atm;
- a high-pressure cycle (see Fig. 7) with flow derivation and work in the subcritical region of the *TS* diagram for CO₂; it has a high-pressure range of 250 to 300 atm.

Both simple and reheat cycles could also be fitted with a flow derivation and work in the critical region (either in subcritical or in saturated state). This would increase their efficiencies by one to two points.

Presently it is difficult to assess the efficiency for

FIG. 5. Simple Cycle.

cycles close to the saturation curve. In this region, there are quite large discrepancies between the existing *TS* tables on the one hand, and, on the other, even within each set of values, interpolations often lead to inconsistencies between some results, so that smoothing of the tables must be performed prior to calculations.

The power regulation of a closed-cycle gas turbine may be performed mainly by either:

a system keeping the efficiency about constant; it is based on the variation of the pressure level in the circuit and used mainly in the 100%/50% power range,

or a system which may act more quickly but leads to a very fast drop of efficiency; it is based on turbine or compressor single or double bypass and mainly works in the 50% power range or for very quick load changes.

Both systems may be used for the simple and reheat cycle. However, in the third case, additional regulating devices are required due to the fact that the pressure and temperature at the last compressor inlet should be maintained constant if one wants to avoid the rotating machinery to operate in the satu-

FIG. 6. Reheat Cycle.

FIG. 7. Supercritical High-pressure Cycle with Derivation.

rated phase. Flow variation must therefore be performed by means of throttling devices.

The variation of the properties of CO₂ near the saturation temperature are such that it has been found convenient to develop within the Consortium a computation code that balances automatically each suggested cycle and computes its efficiency, taking pressure drops and component efficiency into account.

This code has been first applied to the cycles that could use the secondary (nonactive) sodium system of the SNR prototype, with an assumed maximum CO₂ temperature of 520°C. Those cycles are those reported in Figs. 5, 6, and 7, and yield net efficiencies of 33.42, 35.14, and 35.30%, respectively, which can be compared to the $\approx 41\%$ net efficiency of the SNR steam cycle.

New calculations will then be made with a maximum CO₂ temperature of 575°C, and then with cycle additions crossing the saturation line. The efficiencies would increase by some points and approach the values obtained at present for the steam cycle applicable to the LMFBR.

The same calculation code will accommodate the economic formulas being now developed that describe the influence of pressure and temperature on the cost and efficiency of each component of the cycle.

As in all gas-turbine closed cycles, cycle efficiencies and cost will be much influenced by the efficiency of compressors, for which both improved calculation and manufacturing methods are available today, and by the design features enhancing heat transfer without affecting too much the pressure drop and tube-bundle costs.

IV. Economics of the New Concept

Fast breeder reactors are expected to provide a very cheap fuel cycle for two main reasons: the high burnup achieved and the breeding of plutonium, which, in turn, they will use most efficiently.

On the other hand, their investment costs should be minimized by all means; otherwise they will not penetrate the reactor market until there is a definite upwards trend in the cost of uranium. Two favorable features must be noted here: the high specific power and compactness of the LMFBR, and the high temperature one hopes to achieve at the reactor outlet (600°C).

Offering definitely low fuel costs and sufficiently low investment costs, the LMFBR is expected to be run as a very large power unit operated at high load factors throughout its useful life. Therefore, any new concept applicable to that reactor should exhibit the following positive qualities:

- 1) promote simplicity, reliability of operation, and short downtime periods;
- 2) promote compactness and efficiency, and thus low investment costs for very large-size reactors.

The concept which has been proposed in this paper is expected to exhibit the first set of qualities. The absence of any significant corrosion and erosion should make it possible, in spite of the much higher operating pressure, to produce equipment as reliable as the intermediate heat exchangers of the classical LMFBR, provided that the design is properly and extensively checked for leaks and vibrations.

The results of the studies being performed will determine, in the course of 1969, if the second set of qualities is also met by the concept. Fortunately, those studies can rely on the experience accumulated in Europe by the manufacturers of both CO₂ and gas-turbine equipment. Much will depend on the design of the CO₂-CO₂ recuperators, which are one of the most costly items of the system, and of the compressors.

Discussion

Mr. Melese (GGA): Are you sure you're going to get the best of both worlds, or the worst of both worlds? I mean this: most people prefer to have a complete sodium system and not worry about gas. Other people are more interested in gas and do not worry about sodium problems at all. In your system you have both the sodium problems and the problems associated with an extremely high-pressure gas. On top of this, from the capital-cost viewpoint, you do have, I believe, an expensive sodium-to-CO₂ heat exchanger if you have to take all the safety considerations into account. Then, as you properly say, in the gas cycle you have the recuperator which is quite a heat exchanger, especially with CO₂, because you're going to have a pressure difference in view of the expansion ratio between the high-pressure and low-pressure side. It is not going to be a cheap piece of equipment.

Mr. Van Dievoet: Of course, such questions were asked at the very beginning of our study, and the answers cannot be straightforward. They can only come after an extended period of rather intensive thermodynamic and cost studies. If both sodium and gas technologies are available, then why is it absolutely necessary to avoid mixing them as long as one does not bring additional problems? If the gas de-entrainment problem can be solved satisfactorily, there is no special reason not to investigate the application of the gas loop to the sodium loop. We have seen in the paper given by Mr. Harde that we have, unfortunately, to keep gas in the sodium loop to form an argon blanket and to expect in the sodium-CO₂ concept that some of the gas will not be de-entrained. A de-entrainment device, like a filter device, is never 100% effective. There is in both cases some gas to be subdivided in order to avoid having a big bubble through the core. In both instances with or without the CO₂-cooled cycle we have to take care of some gas in the primary system. I would concur with you about putting the difficulties of the gas-cooled reactor and the sodium-cooled reactor together, but we are doing partially that by coupling the sodium-cooled reactor and the steam-cooled reactor together, if I continue that reasoning.

Mr. Zebroski (GE): I wonder if you could give at least a semiquantitative indication of the relative heat-transfer areas required for this kind of a system as against the more conventional system.

Mr. Van Dievoet: There is an additional heat transfer area in the sodium-CO₂ cycle, depending, of course, on its type. The pressure has an influence,

But there is an additional heat-transfer area in the sodium-CO₂ cycle as compared to the three-zone steam generator. What counts is the total heat-transfer area: the three zones for the steam generator, and then the recuperator and the sodium-CO₂ heat exchanger. We are now studying in detail a series of models of those units just to obtain the

qualified answer that you would like to have. The preliminary results are not too far beyond what we could tolerate, but there is no doubt that especially the recuperator (gas-to-gas, CO₂-CO₂) is the largest capital unit in the cycle and that the effort has to be put especially there rather than in the sodium-CO₂ heat-transfer unit.

Plant Design Problems Associated with the Application of Vented Fuel to a Fast Breeder Reactor

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A. Introduction

Since 1964 the Italian Fast Reactor Program has devoted a considerable effort to the development of a vented fuel element.

This paper summarizes the results of a study of vented-oxide-fuel application in fast reactors, which was sponsored by CNEN and conducted jointly with CNEN and APDA personnel.⁽¹⁻³⁾ The study was begun in June 1967 and completed in August 1968. The conclusions reported in this paper are, therefore, a reflection of the latest thinking.

The potential advantages of fast reactor operation with vented oxide fuel are well-known. Among these advantages are reduction in the required thickness of the fuel cladding, thus improving the thermal performance and breeding capabilities of the reactor; the eventually higher fuel burnup to be achieved; and the elimination of the conventional gas bottle on the fuel pin, which offers the possible advantages of greater safety in handling irradiated fuel and improved core configuration.

The contamination of the primary system arising from the use of this fuel element represents an important obstacle to its success.

The CNEN study program was designed to cover two basic objectives: (1) to define the modifications required for an existing plant to accommodate vented-fuel operation, and (2) to define the facilities required for a new plant of the same general description. The Enrico Fermi fast reactor was chosen as the reference plant for the purposes of the first study. The new plant design developed in the second study is intended to be representative of a model vented-fuel FBR design that might be constructed at some future date.

It should be pointed out that the major emphasis in both studies was placed on the design of the reactor gas systems required to accommodate the high gas activities resulting from vented fuel, i.e., design of the reactor cover-gas system, waste-gas-

storage system, and waste-gas-disposal system. The design of these systems is felt to be the critical factor affecting a vented-fuel plant design. Very little attention was given to topics felt to offer no serious problems, such as the effect of operation with fission products in sodium, the effect on heat transfer and safety of possible fission gas-bubble accumulation in the coolant, and similar considerations.

Lastly, it should be mentioned that the results reported here are for the most part based on assumed optimistic predictions for the performance of the fuel-venting device, transport of fission gases in flowing sodium, and leakage of cover gas from the gas systems. Considerably more component-development work and experimental verifications of stated parameters is needed.

B. Reference Core Design

The reference core selected for the study was a zoned mixed-oxide core concept developed by APDA in an earlier program.⁽⁴⁾ Figure 1 shows the reference core at equilibrium conditions. It would operate at 380 MWt, or 415 MWt including the blanket, with a linear power density from 1 to 1½ kW/in. and an ultimate burnup of 100,000 MWD/T. Target burnup would be achieved in twelve to eighteen months, with a shutdown every month for refueling.

In order to realize the vented-fuel concept in the model fuel pin, the mixed-oxide APDA fuel pin has been modified by exchanging the fission gas plenum for the typical CORT-DISCAR configuration developed by CNEN (see Fig. 2). The functions of the CORT device are to delay short-lived fission gases, to condense solid and liquid fission products released from the fuel zone, and to prevent plugging of the blanket zone. The DISCAR is a "diving bell" device whose functions are to provide for an additional delay time for fission gases and to avoid sodium re-entry. For this model fuel pin a total

FIG. 1. Cross Section through Reactor Showing Oxide-core Regions.

FIG. 2. Layout of Fuel Pin.

TABLE 1. PRODUCTION OF NOBLE FISSION G

Isotope	$T_{1/2}$	λ , sec ⁻¹	Y, %	Holdup Effect, $e^{-\lambda\tau}$			Production, atoms/sec		
				$\tau = 1$ day	$\tau = 5$ days	$\tau = 45.8$ days	$\tau = 1$ day	$\tau = 5$ days	$\tau = 45.8$ days
Kr-90-95 and 97	Sec	Very large	0.290 to 0.590	v.s. ^(b)	v.s.	v.v.s. ^(c)			
Xe-139-144	Sec	Very large	0.290 to 0.590	v.s.	v.s.	v.v.s.			
Kr-89	3.18 min	3.61×10^{-3}	4.59	10^{-35}	v.s.	v.v.s.			
Xe-137	3.9 min	2.96×10^{-3}	6.00	10^{-35}	v.s.	v.v.s.			
Xe-135m	15.3 min	7.41×10^{-4}	1.80	10^{-26}	v.s.	v.v.s.			
Xe-138	17.0 min	6.80×10^{-4}	5.49	10^{-26}	v.s.	v.v.s.			
Kr-87	78 min	1.48×10^{-4}	2.70	2.5×10^{-6}	10^{-28}	v.v.s.	7.94×10^{11}		
Kr-83m	1.9 hr	1.01×10^{-4}	0.51	1.79×10^{-4}	2.5×10^{-19}	v.v.s.	1.07×10^{13}		
Kr-88	2.77 hr	6.88×10^{-5}	3.70	2.56×10^{-3}	1.1×10^{-13}	v.v.s.	1.12×10^{15}	4.77×10^4	
Kr-85m	4.4 hr	4.38×10^{-5}	1.50	2.28×10^{-2}	6.3×10^{-9}	v.v.s.	4.04×10^{15}	1.11×10^9	
Xe-135	9.2 hr	2.09×10^{-5}	6.30	0.164	1.22×10^{-4}	10^{-36}	1.22×10^{17}	9.05×10^{13}	
Xe-133m	2.3 days	3.49×10^{-6}	0.16	0.74	0.22	1×10^{-6}	1.39×10^{16}	4.14×10^{15}	2.7×10^{10}
Xe-133	5.27 days	1.52×10^{-6}	6.62	0.88	0.52	2.4×10^{-3}	6.86×10^{17}	4.06×10^{17}	1.88×10^{15}
Xe-131m	12 days	6.68×10^{-7}	0.025	0.945	0.75	0.071	2.78×10^{15}	2.21×10^{15}	2.09×10^{14}
Kr-85	10.6 yr	2.07×10^{-9}	0.30	1.00	1.00	0.99	3.54×10^{16}	3.54×10^{16}	3.50×10^{16}
Kr-82	Stable		3.5×10^{-5}	1.00	1.00	1.00	4.12×10^{12}	4.12×10^{12}	4.12×10^{12}
Kr-83	Stable		0.544	1.00	1.00	1.00	6.40×10^{15}	6.40×10^{15}	6.40×10^{15}
Kr-84	Stable		1.00	1.00	1.00	1.00	1.18×10^{17}	1.18×10^{17}	1.18×10^{17}
Kr-86	Stable		2.02	1.00	1.00	1.00	2.39×10^{17}	2.39×10^{17}	2.39×10^{17}
Xe-129	Stable		1.00	1.00	1.00	1.00	1.18×10^{17}	1.18×10^{17}	1.18×10^{17}
Xe-130	Stable		5×10^{-4}	1.00	1.00	1.00	5.88×10^{13}	5.88×10^{13}	5.88×10^{13}
Xe-131	Stable		2.93	1.00	1.00	1.00	3.45×10^{17}	3.45×10^{17}	3.45×10^{17}
Xe-132	Stable		4.38	1.00	1.00	1.00	5.16×10^{17}	5.16×10^{17}	5.16×10^{17}
Xe-134	Stable		8.06	1.00	1.00	1.00	9.48×10^{17}	9.48×10^{17}	9.48×10^{17}
Xe-136	Stable		6.46	1.00	1.00	1.00	7.60×10^{17}	7.60×10^{17}	7.60×10^{17}

Total

- (a) Conditions: (a) 380 MWt (core)
 (b) 100% release
 (c) 1-d, 5-d, and 45.8-d holdup.
 (d) $t_{\text{refuel}} = 1$ month
 (e) $t_{\text{lifetime}} = 2$ yr.

- (b) Very small.
 (c) Very, very small.

delay time (from birth to release) of 5 days for fission gas was assumed. This ensures the extinction of all short-lived noble fission gases produced and limits the radioactive gas release to the Xe-133, Xe-133m, and Xe-131m isotopes almost exclusively. The buildup of long-lived Kr-85 activity is negligible for the operating times considered. No release of the halogens was assumed to occur.

A limitation of this pin design is that if a vacuum condition were imposed in the cover-gas space of the reactor, some of the retained gases would escape; also, when cover-gas pressure were restored, the gas spaces above the fuel in the fuel pin would flood with sodium. This is unfortunate because it precludes the application of vacuum-transfer and vacuum-purge cycles for removing contaminated cover gas.

The study also took into consideration a phenomenon indicated by recent UKAEA measurements,⁽⁵⁾ i.e., the possibility that during or shortly after reactor shutdown, a burst release of all krypton and xenon gases contained in the oxide fuel pin may take place. This would thus include many short-lived isotopes that are normally retained to extinc-

tion in the venting device and result in additional complications in maintenance procedures.

C. Production of Fission Gas in the Reference Core

In Table 1 a tabulation is given of the rate of production of all xenon and krypton isotopes in the reference core and their accumulation in the cover gas as a function of the gas-holdup time τ . The table shows that for the idealized situation of a perfectly sealed reactor cover-gas system (no leakage or purge losses) and a 5-day holdup, equilibrium activity in the cover gas would be reached at the end of 30 days of operating time t , at which time radioactive decay balances production due to fission. For the postulated 5-day gas holdup in the fuel pin, release to the sealed gas space amounts to 16.6 Ci/sec and the equilibrium activity is 10^7 Ci. Homogeneous distribution of the activity in the cover-gas space was assumed in the early studies, and no absorption of the gases in the sodium coolant was considered. The table also shows that with only 1-day retention in the fuel pin, the cover-gas activity would double;

ACCUMULATION IN THE MODEL VENTED CORE^(a)

Activity in Sealed CGS, Ci						Gas Volumes in Sealed CGS at STP, Liters					
$\tau = 1$ day		$\tau = 5$ days		$\tau = 45.8$ days		$\tau = 1$ day		$\tau = 5$ days		$\tau = 45.8$ days	
$t = 1$ mo	$t = 2$ yr	$t = 1$ mo	$t = 2$ yr	$t = 1$ mo	$t = 2$ yr	$t = 1$ mo	$t = 2$ yr	$t = 1$ mo	$t = 2$ yr	$t = 1$ mo	$t = 2$ yr
21.5	21.5										
291	291										
3.02×10^4	3.02×10^4	1.28×10^{-6}	1.28×10^{-6}								
1.09×10^5	1.09×10^5	3.02×10^{-2}	3.02×10^{-2}								
3.30×10^6	3.30×10^6	2.43×10^3	2.43×10^3								
3.78×10^5	3.78×10^5	1.12×10^5	1.12×10^5								
1.85×10^7	1.85×10^7	1.10×10^7	1.10×10^7	5.5×10^{-1}	5.5×10^{-1}	0.22	0.22	0.04	0.04	0.05	0.05
7.51×10^4	7.51×10^4	5.99×10^4	5.99×10^4	5.09×10^4	5.09×10^4	16.80	16.80	9.94	9.94	0.01	0.01
5.18×10^3	1.24×10^5	5.18×10^3	1.24×10^5	5.67×10^3	5.67×10^3	0.15	0.15	0.12	0.12	0.01	0.01
0	0	0	0	5.13×10^3	1.23×10^5	3.44	82.5	3.44	82.5	3.41	81.7
0	0	0	0	0	0	0.0004	0.009	0.0004	0.009	0.0004	0.009
0	0	0	0	0	0	6.23	149.3	6.23	149.3	6.23	149.3
0	0	0	0	0	0	11.4	274.3	11.4	274.3	11.4	274.3
0	0	0	0	0	0	23.2	555.3	23.2	555.3	23.2	555.5
0	0	0	0	0	0	11.4	274.3	11.4	274.3	11.4	274.3
0	0	0	0	0	0	0.007	0.17	0.007	0.17	0.007	0.17
0	0	0	0	0	0	33.7	807.2	33.7	807.2	33.7	807.2
0	0	0	0	0	0	50.3	1206.5	50.3	1206.5	50.3	1206.5
0	0	0	0	0	0	91.9	2204.7	91.9	2204.7	91.9	2204.7
0	0	0	0	0	0	73.8	1770.7	73.8	1770.7	73.8	1770.7
2.24×10^7	2.25×10^7	1.12×10^7	1.13×10^7	6.17×10^4	1.80×10^5	322.7	7343	315.8	7336	305.8	7326

if the retention time could be extended to 46 days, the equilibrium activity would be lower by a factor of 170. In the case of 5-day pin holdup with a 5.27-day half-life, xenon-133 is responsible for 98.5% of the total activity. The long-lived Kr-85 becomes significant only for a 46-day holdup time. Volumetric accumulation of the fission gases in the sealed cover-gas space amounts to about 11 cubic feet per month for the reference core. Volumetric accumulation is essentially independent of pin holdup time and varies linearly with operating time, since most of the exhausting gases are made up of stable isotopes.

D. Fermi Plant Environment with Vented Fuel

In order to appreciate the effect of radioactive gas discharge of a vented fuel on the Fermi plant atmospheres, it is helpful first to briefly review the gas facilities of the plant.

1. Gas Facilities Inside the Fermi Containment Building

Figures 3 and 4 show the arrangement of the Fermi containment building and its associated gas

FIG. 3. Arrangement in Reactor Containment Building.

facilities. The 200,000-ft³ volume of air existing in the upper chamber of the reactor containment building above the operating floor is swept by a fresh air stream of only 200 ft³/min. The area surrounding the rotating plug in the reactor vessel is isolated by a machinery dome having an approximate volume of 3700 ft³. It is swept by 50 ft³/min of fresh air. Below

FIG. 4. Recirculating System for Primary Cover Gas.

the operating floor an inert argon atmosphere is maintained in the cover gas spaces at a slightly positive pressure of 4 in. water column above barometric pressure. The upper chamber is at barometric pressure, and the machinery dome has a slightly negative pressure of approximately -1 in. water column.

2. Activity Buildup in Fermi Cover-gas System

A sealed cover-gas system cannot be assumed for Fermi. There is an estimated 0.2-scfh leakage of cover gas through the various rotating plug seals into the machinery dome. Cover gas also leaks up the pump shafts into the upper chamber of the containment building by back diffusion through the shaft-seal spaces, but at such a low rate as to be undetectable. The detection limit is 3×10^{-4} scfh. From calculations the actual leakage is estimated to amount to only about 8×10^{-7} scfh. There is also a counter-flow of clean argon from the pressurized seals in the rotating plug, which passes into the cover-gas system at the rate of 4 scfh. This mixes with the high-activity cover gas and must be periodically discharged to waste gas, producing the effect of a constant

purge. Loss of activity by these leakage and purge actions reduces the computed activity level of Xe-133 at equilibrium in the cover-gas system from the 10^7 Ci value postulated earlier for a sealed system to 4×10^6 Ci in the actual Fermi reactor employing the reference vented core.

3. Atmospheric Activity Levels inside the Fermi Containment Building

Due to the small apparent leakage through the pump-shaft seals, it does not appear that the atmospheric activity levels in the upper chamber of the Fermi containment building would be excessive when vented fuel is used. The calculated value does not exceed the MPC limitation for Xe-133 of 10^{-5} $\mu\text{Ci/cc}$ for restricted plant areas; however, the leakage to the machinery dome is grossly excessive. Even if the radiation monitors on the vent exhaust of the machinery dome were defeated in order to maintain the 50-cfm supply of fresh air, the equilibrium activity level in this space would be $21.8 \mu\text{Ci/cc}$, ~ 2 million times the MPC. Also, the radiation dose rate 5 ft outside the machinery dome would be 5.5 R/hr, and 3 in. of steel shielding would be required around the dome to permit even limited access to the operating floor. If the radiation monitors were not defeated, i.e., if the machinery dome were isolated, the activity levels in the machinery dome would increase by a factor of 100 over those given above.

4. Activity Levels Outside the Fermi Containment Building

Several serious activity conditions would also be produced in the Fermi reactor's recirculating cover-gas system and waste-gas-disposal system using vented fuel. A brief review of the design of these systems follows.

Figures 4 and 5 show the recirculating cover-gas system, which is installed in a gas building located external to the containment building. Figure 5 also shows the waste-gas-disposal compressors and decay storage tanks located in the gas building. The recirculating gas system is essentially a closed loop that regulates the cover-gas pressure through a breather line. Cover-gas exhausts pass through a compressor to a vacuum tank and sodium-vapor trap, and then to a holdup gas-supply tank, before return to the reactor cover-gas system.

The waste-gas-disposal system, shown in more detail in Figure 6, consists of a vacuum collection header which receives all radioactive plant exhaust, a compressor, two decay storage tanks, and an underground discharge line which runs to a dilution blower plenum exhausting to a stack. All gas flows are

FIG. 5. Plan View of Inert Gas and Waste Gas Building.

FIG. 6. Fermi Plant Waste-gas System.

monitored by radiation meters to ensure that the activity levels are sufficiently low that the MPC is not exceeded at the site boundary.

For normal operation of vented fuel in the Fermi reactor with the reference core, it is possible to trace the progress of the 4-sefh input of purge gas from the reactor cover-gas system through the various

components of the waste-gas-disposal system. The effective residence time for the gas at each location can be computed and the gas activity decay determined until it is discharged through the stack. When this is done, it is found, rather surprisingly, that the resulting atmospheric activity

at the site boundary is only 1.9×10^{-7} $\mu\text{Ci/cc}$, consisting almost entirely of Xe-133 (5.27-day half-life). This is well within the MPC limitation of 3×10^{-7} $\mu\text{Ci/cc}$ for Xe-133 for off-site areas.

The present waste-gas system of the Fermi plant can adequately exhaust the large activities resulting from vented-fuel operation, principally because the system was oversized for the present core, i.e., the plant has a small rate of waste-gas exhaust and large capacity for waste-gas storage, which results in a very long effective decay holdup time (~ 45 days). In addition, the stack-blower dilution and atmospheric dilution factors, $\sim 10^5$ and $\sim 10^4$, respectively, are much larger than required for operation with the present core. It may be noted that even with a 45-day decay storage period, the 10.6-yr half-life of Kr-85 activity in the waste gas during vented-fuel operation is still insignificant at the time of its release to the stack.

The above value for the stack exhaust activity with vented fuel would be approximately doubled after either a reactor transient, such as a single circuit failure pushing additional gas out of the cover-gas system and decreasing the effective decay holdup time, or following a 10-volume purge of the cover-gas spaces preparatory to loading or maintenance operations. However, this would occur only occasionally, and the stack activity still would not exceed AEC regulatory standards on an annual basis.

The most serious activity condition existing outside the Fermi containment building using vented fuel is the large inventory of radioactive fission products of the gas building. Some 6 million Ci of fission product activity would be present in the holdup, vacuum, and decay storage tanks during normal operation. Since this building was designed to handle only low waste-gas activities, it is not designed as a leaktight containment building. A serious potential hazard would, therefore, exist during vented-fuel operation because the 6 million Ci could be released to the outside atmosphere by a single containment failure. Even without containment failure, this inventory would produce a radiation dose rate of 90 mR/hr outside the 5-ft-thick concrete walls of the containment building, considerably more than allowed by present AEC regulations.

E. Modifications to the Fermi Plant to Accommodate Operation with Vented Fuel

Based on the environment studies discussed above, certain design criteria were defined for the Fermi plant facilities to allow operation with vented fuel. Although these criteria apply explicitly to Fermi, they can probably be applied equally as well to

any FBR employing vented fuel. These criteria are:

Leakage of the contaminated cover gas to plant atmospheres must be reduced to undetectable levels—substantially to zero leakage.

Double containment must be provided for all radioactive gas inventories.

Allowance must be made for reasonable maintenance access to all working parts handling radioactive gas.

An optimum combination of waste-gas decay and dilution must be provided to limit site-boundary atmospheric activities to MPC levels.

The results of the Fermi studies showed that as presently designed the plant would fully comply with only the last of the criteria listed above.

1. Preliminary Investigations of Plant Modifications

Several methods were investigated for solving, or at least minimizing, some of the design problems associated with high activity of the cover gas in the Fermi plant, but all were eventually rejected. Particularly, in an attempt to eliminate the major difference between a vented plant and a conventional plant, various methods of reducing the activity level of the cover gas were studied. Continuous purging of the cover-gas spacings was one method considered, but it was at once apparent that a purge rate sufficient to allow more realistic leakage conditions would require an immense capacity for waste-gas storage. Increasing the sweep-purge flow of cover gas by a reasonable amount, say from 4 scfh to 4 scfm, would only reduce the equilibrium activity of the cover gas by a factor of 75, hardly significant when compared to the high activities involved. It was found that even this flow increased the required decay storage capacity more than was desirable. Preliminary designs for a cover-gas-purification loop, consisting of cold traps and cooled delay beds, were also studied, but incorporation of such a loop was deferred because of the limited data available on their use. CNEN is at present pursuing a more detailed study of various purification systems to determine their efficiencies and costs, and also the methods of dealing with accidental gas release if cooling failed. Finally, consideration was given to improved designs of vented fuel pins and fuel formulations that might increase the gas-holdup time and subsequent activity decay in the pin, but this was judged to be beyond the scope of present investigations.

2. Modifications Adopted

As indicated in the Fermi environment study, the present waste-gas decay holdup, stack, and dilution blower appear to be adequate to accommodate

vented fuel. The ventilation system in the upper chamber of the containment building is also adequate, even though it offers a relatively small flow of fresh air. It is adequate primarily because essentially zero leakage of cover gas to the upper chamber was assumed. Admittedly this may prove to be an optimistic assumption, even though no measurable leakage has been detected to date; however, if such leakage through the pump-shaft seals into the upper chamber did develop and prove to be a problem, it has been determined that it could be eliminated entirely by establishing a continuous small purge flow of clean argon down the shafts to defeat back diffusion up through the shaft annuli.

On the other hand, the leakage to the machinery dome was found to be excessive. Investigation showed it to be impractical to increase the ventilation flow sufficiently through the dome to relieve buildup of high activity. The large order of magnitude of activity reduction required would have resulted in unrealistic large flows. Improvement of the some five-dozen seals located in the reactor plug mechanisms to reduce cover-gas leakage to the dome was not attempted either. Instead, it was decided to isolate the machinery dome by eliminating its ventilation flow and to pressurize its 3700-ft³ volume with argon to prevent leakage. With this modification the upper chamber will be at barometric pressure as before (see Fig. 3), the cover-gas system pressure is reduced from 4 to 2 in. water column positive pressure, and the machinery-dome space is filled with argon at 4 in. positive pressure. Thus, the differential pressure profile across the plug-seal mechanisms is reversed, producing a slight inleakage from the dome to the cover-gas space.

The last major modification required was to provide double containment for all inventories of radioactive gas. This was achieved by complete elimination of the gas building and placement of its equipment inside the containment building. The primary cover-gas spaces, already in double containment, required no modification. The waste-gas decay storage tanks were relocated below the floor of the containment building in unused available space. The compressors, valves, and other working parts of the gas system were installed in the containment building in one of the equipment decay tanks originally intended for remote maintenance service of reactor components. In this way, ready access was provided to working parts for maintenance servicing if needed.

To reduce the space requirements for gas equipment inside the containment building, as well as the number of working parts subject to breakdown, another modification was made. The recirculating cover-gas system was eliminated by conversion to

FIG. 7. Conversion to Feed-and-dump System.

a feed-and-dump system, shown in Fig. 7, which supplies clean gas and discharges directly to the waste-gas-collection system. Figure 7 also compares the new system with the original system shown earlier in Fig. 4. In the new system there is a cover-gas breather line, together with a clean-gas supply and surge tank (formerly the holdup tank) to meet transient demands. The system exhausts through a vacuum tank to the waste-gas-collection header. The compressors and vapor trap of the former system are therefore eliminated.

Another refinement was proposed to reduce the decay storage volume required for purging operations. This consisted of altering the Fermi piping network and pressure-equalizing system so as to produce a sweep flow through the cover-gas spaces, thus eliminating dead legs and bypasses. The clean-gas purge will be made down the pump shafts as before, but will sweep through the intermediate-heat-exchanger tanks and the overflow tank, and will exit through the reactor vessel to the waste-gas-disposal system (see Fig. 4). This sweep purge, which will result in a certain amount of absolute displacement, is more efficient than a purge with perfect mixing, which would require ten volumes to reduce contamination to 45×10^{-6} ppm of the initial level.

F. Model FBR Design to Accommodate Vented Fuel

In concluding the study, the various criteria and design concepts developed for handling vented fuel

FIG. 8. Schematic of Modified FBR Gas Service for Vented-fuel Operation.

in the real environment of the Fermi plant were applied to the design of gas facilities for a model FBR. In this model FBR, the same reference fuel-pin design and core concept as stipulated earlier were used, i.e., 5-day holdup of the fission gases in the fuel pin and a core power of 380 MWt. Two new considerations were also incorporated as a result of advanced studies. First, the results were used of a study made which indicated that fission gas entrainment in the sodium coolant results in an 80-sec average gas-residence time in the primary sodium system. This time is not enough to result in any additional decay of fission product activity, but it is long enough to allow prediction that approximately 15% of the vented fission gases are released in the IHX tanks, the balance of the gas release occurring directly in the reactor vessel. Very little subsequent migration of the fission gases from the point of release to other cover-gas spaces due to thermal diffusion or convection is indicated. Also taken into consideration was the effect of the earlier-mentioned possible burst release of all fission gases contained in the oxide fuel, upon reactor shutdown.

1. Plant Layout

A schematic representation of the gas system in the model FBR design is shown in Figure 8. It is similar to the Fermi modified gas-system design discussed earlier and requires no detailed explanation. It may be noted, however, that a conventional cover-gas monitor loop which draws a 4-scfm gas sample has been retained in the new system. An electrostatic fission product detector (FPD), i.e., moving charged-wire detector, is one of the sampling devices located in this loop to detect failed fuel. A conventional FPD such as this will operate satisfactorily in a vented-fuel reactor because it is

not sensitive to the gaseous fission products normally vented to the cover gas. Being a charged-wire device, it only collects and counts the unstable daughters of fission products that undergo beta decay. Of all the fission gases (see Table 1), the only isotopes that fulfill both of these requirements are the short-lived isotopes Kr-88, Kr-89, Xe-137, and Xe-138, none of which is released to any appreciable extent from vented fuel having 5-day holdup. Thus, as long as adequate shielding is provided between the collection chamber and counting chamber of the device, the high activity present in the cover gas will have no significant effect on its operation. During normal operation, therefore, the detector will register only a small background signal due to the short-lived gaseous isotopes released from the fissioning of uranium impurities in the sodium coolant, and its sensitivity in detecting small fuel failures will not be adversely affected.

The other sampling device located in the cover-gas monitoring loop is a chromatograph gas analyzer (CGA) used to monitor for impurities. Its operation will similarly not be affected by the small concentrations of isotopic fission gases present in the cover gas, nor their decay heat. Over a period of time, the device will probably accumulate quite a large amount of radioactivity in the column, but adequate shielding can be provided.

The estimated cost of the complete gas system for the model FBR shown in Figure 8 is \$3.5 million. This is not very much different than the cost of similar equipment for a conventional plant.

2. Method of Satisfying the Four Criteria for Vented Fuel

The four basic criteria developed earlier for the gas facilities design of a reactor using vented fuel

were met in the model FBR as follows:

Leakage of cover gas upwards through the reactor rotating plug is prevented by a pressurized, argon-filled machinery dome over the plug, similar to the method used in the modified Fermi design. The possibility of leakage up the pump shafts into the upper chamber of the containment building is precluded by a constant purge of 1 scfh of clean argon down each of the three primary pump shafts and two overflow pump shafts.

It may be noted that during normal operation, the total flow of 5 scfh through the 5 pump shafts passes into the reactor vessel, where it is reinforced by an additional 4 scfh of exhaust from the reactor plug-seal mechanisms. This total flow of 9 scfh through the cover-gas system is relieved periodically, at 5-min intervals, to the waste-gas system in order to maintain cover-gas pressure at $\pm 1/2$ in. water column. It therefore acts as a constant purge of cover-

gas activity and reduces the activity value of 10^7 Ci given earlier for a sealed cover-gas system to 7×10^5 Ci in the model FBR. Although studies show that almost all of this activity is released in the IHX tanks and reactor vessel, with little thermal migration between spaces, it can also be shown that most of the activity (75%) becomes concentrated in the overflow tank due to the effect of the purge flow through the cover-gas system.

Double containment of all inventories of radioactive gas is accomplished by installing the entire waste-gas-disposal system below the reactor operating floor inside the reactor containment building.

All working parts of the gas system are located inside shielded tanks placed below the operating floor. For periodic maintenance they can be entered through access hatches in the floor 10 days after shutdown. The locations of these shielded equipment tanks are shown in Fig. 9. Each is shielded by a

FIG. 9. Plan View of a Containment Building for a Modified FBR.

FIG. 10. Schematic of Modified FBR Waste-disposal System Showing CGS Monitoring Loop.

3-in. thickness of center-post erected steel placed on the tank walls. A 3-in. shield thickness is sufficient to reduce the dose rate inside the tanks resulting from the residual Na-24 activity of 15-hr half-life in nearby pumps and IHX's to acceptable levels 10 days after shutdown. However, ten days after shutdown, the radiation levels from the waste-gas storage tanks located nearby will be higher than the Na-24 dose. This is because of the high concentration of 5.27-day half-life Xe-133 in the tanks, which decays more slowly. Therefore, an additional 3 in. of steel shadow shielding is required between the decay tanks and access tanks to reduce the radiation level from this source to an acceptable level inside the access tanks during maintenance periods.

The atmospheric activities at the site boundary are reduced to MPC levels by use of a combination of waste-gas decay storage and stack dilution similar to that used in the Fermi design. Since the meeting of this criterion is considered the most critical factor in the design of the model FBR, it is discussed in more detail below.

3. Waste-gas System and Stack Operation

As noted earlier, there is a total flow of 9 scfh of radioactive purge gas from the primary cover-gas system to waste gas in the model FBR design. The specific activity of this gas, based on 7×10^5 total Ci of Xe-133 in the cover gas and the previously mentioned distribution, is 0.086 Ci/cc. This must be reduced to the MPC value, $3 \times 10^{-7} \mu\text{Ci/cc}$, at the site boundary at the time of its release. The way in which the required reduction by a factor of 3×10^{11} is achieved during normal FBR operation is as follows:

The gas first passes to the two vacuum surge tanks, shown in Fig. 10, each of which has a volume of 500 ft³ and is maintained at 7 to 9 psia. Assuming that there is an approximately 4-scfh input of relatively low-activity waste gas from other plant services, the total 13-scfh input of diluted gas results in a 55-scfh discharge through the compressor every 4.3 hr. From this, the effective residence time of the gas in the vacuum surge tanks is calculated to be 36 h, producing an insignificant decay of only 19% in the predominant 5.27-day Xe-133 activity.

Each 55-scf exhaust is next received by one of three decay storage tanks operating in sequence. While one tank is being filled, another is being simultaneously discharged to the stack-and-blower system; the third remains fully charged and at rest. Three decay storage tanks are required in the model FBR, compared to only two in Fermi, because of the larger exhaust rate of waste gas of the former plant. Each decay storage tank in the model plant has a volume of 2500 ft³ and is charged to 90 psig at 150°F. Allowance is not made for vacuum emptying. Each tank therefore holds a maximum of 12,000 scf of new gas and 2,000 scf of old gas. On this basis, and considering the vacuum tank exhaust rate, 39 days are required to fill and/or empty a tank. The effective or average decay time for the total amount of Xe-133 entering or leaving a tank during either process is calculated to be approximately one-fourth of this time, or 10 days. A full 39-day decay is achieved during the time the gas is at rest in a fully charged tank. The overall effective decay period for the gas from the time it leaves the vacuum surge tanks until it enters the stack is therefore 59 days, and the resulting total reduction of Xe-133 activity is approximately 2×10^3 . Some additional credit can also be taken for the dilution effect of the mixing of old and new gas in the tanks.

In the last part of the waste-gas cycle, the gas is discharged from decay storage to the stack and blower at a rate of 0.2 scfm. A 22,000-cfm blower then dilutes the gas by a factor of 1.1×10^5 , following which it is dispersed to the site boundary. During the dispersion, it achieves an additional atmospheric dilution assumed to be the same as at the Fermi plant, namely, 1×10^4 . The total reduction in specific activity of the initial waste gas discharge is thus approximately 3×10^{12} , or ten times larger than required.

Although the stack exhaust activity at the site boundary during normal operation is only 0.3×10^{-7} $\mu\text{Ci/cc}$ of Xe-133, which is ten times less than the MPC, the effects of reactor transients and maintenance purges must also be considered. A single circuit failure produces the worst transient condition in the gas system. It results in the discharge of some 200 scf of cover gas in a 3-min period. At the most critical point in the sequential decay storage cycle, i.e., at the end of a charging period when all tanks are full, it would result in a 15-hr loss in the normal 59-day effective decay period; however, this is hardly significant.

A ten-volume purge of the cover-gas spaces

(4300 scf) therefore appears to be the most critical condition, since it fills a decay tank $\frac{1}{2}$ full in a short period of time. It would reduce the overall effective 59-day decay storage period by about 14 days, increasing stack exhaust activity at the site boundary by a factor of 6, to 1.8×10^{-7} $\mu\text{Ci/cc}$. This is still below the MPC value of 3×10^{-7} $\mu\text{Ci/cc}$, however. Therefore, transients and purges do not appear to be a problem in the model FBR.

To conclude the study, a prediction was made of what the effect of a sudden burst release would be upon reactor shutdown of all the gaseous fission products present at equilibrium in the oxide fuel, should this actually occur. This would include all the short-lived gaseous fission products normally retained to extinction in the fuel pin, plus the stored Xe-133 during its 5-day decay period. If this did occur, calculations indicate that a temporary tenfold rise in the normal cover gas operational activity would result. If purging were then carried out shortly after shutdown, the activity transferred to the receiving decay tank would also be ten times greater than in a normal purge, assuming no burst release. However, during the following decay storage period the short-lived activities would die out completely, and only the Xe-133 activity would remain. The Xe-133 activity remaining would be somewhat larger (50%) than in the case of a normal purge, because it would consist of gas which did not have the benefit of a full 5-day decay in the fuel pin. However, even a 50% increase in the normal stack exhaust purge activity still results in an activity of only 2.7×10^{-7} $\mu\text{Ci/cc}$ at the site boundary, which is less than the MPC. Therefore, a burst release of all fission gases at shutdown would not seriously affect the design of the model FBR gas facilities.

G. Conclusions

From the investigations of this study, it does not appear that gas facilities to accommodate vented fuel in a FBR are significantly more costly than those in a conventional plant. However, the conversion of an existing plant may not prove to be practical. In designing a new plant for vented-fuel operation, the most critical considerations are the integrity of the primary cover-gas system, i.e., little more than zero leakage can be tolerated, and adequate waste-gas decay storage capacity. It is apparent, too, that some delays must be accepted in the normal operating schedule, such as when performing refueling and maintenance service, in order to allow for the clear-

ing of gas from working areas. Planning for even larger power units than discussed here does not appear to present any particular additional problems and would require only an approximate linear escalation of the facilities discussed here.

In considering what work remains to be done to improve the design of vented FBR's, one of the more attractive prospects is the development of a method of increasing the gas-holdup time in the fuel pin to the order of 40 or 50 days. This would allow significant relaxation of the leaktightness requirements and allow considerable reduction in the gas-storage requirements. Alternatively, the development of efficient cleanup methods for cover-gas activity, by decay-bed and cold-trap techniques, would similarly improve the design. Finally, design problems could also be greatly relieved if some way could be found to utilize underground gas decay storage methods, such as is done with certain fluid wastes, possibly by using a solidifying foam.

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Discussion

Mr. Gibson (GE): Did you consider the possibility of using helium in the cover-gas space and reclaiming that by cold-trapping and removing the Kr⁸⁵, or alternatively trying to reclaim the leakage argon that you put into the cover gas in a cold-trapping arrangement and not discharging through a stack?

Mr. Bruzzi: We didn't in this study, but separately CNEN is going ahead with the program, and we are considering both helium and argon.

Mr. Zebroski (GE): I have a comment in regard to Table 1. The point I'm going to make is that you have the inventory of gas to contend with, and the problem for the designer is to decide where he'd like to keep it. I'd like, first of all, to agree very strongly with your last conclusion, which is that a reasonable delay time in the pin is very important. I think the really striking difference (and the one that I referred to in my paper this morning when I said that it may well be that a system with a vented fuel is much easier to live with than a nonvented fuel system) is that if you look in that first column and you allow, say, one in ten thousand of those fuel elements to fail and dump out its inventory of short-lived as well as long-lived fission gases, you find a much less comfortable situation than if you go with the right-hand column and if you have well-behaved vented fuel, hopefully failing a little less often, dumping out that much lower inventory, a much lower activity of fission gases, and not depositing out the long-lived solid daughters which you get from the short-lived fission gases. I think one of the real issues of the comparison is the increment in gas handling required for vented fuel over a realistic plant design which allows for steady operation with some fuel failure. It may be that the increment is negative.

SESSIONS ON LARGE FAST REACTOR DESIGN

SESSION LFRD-III

Friday Morning, November 8, 1968

Chairman: W. H. Zinn
Combustion Engineering, Inc.

Secretary: D. H. Thompson
Argonne National Laboratory

Introductory Remarks on the 1000-MWe LMFBR Follow-on Study Program

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The AEC has adopted a program for the development of liquid-metal fast breeders that is based upon a division of responsibility between government and industry. The responsibility of government, as represented by the AEC, is to develop the technology upon which the LMFBR must be based, whereas industry will build the power plants, initially demonstration plants with some government support, and finally the commercial or "target" plants. These final commercial plants must be safe, economic, and competitive with fossil and thermal reactor power plants at the time, and it is the AEC's goal that they be built by a competitive nuclear industry in a commercial market, eventually with no supports of any kind. One of the first steps in implementing this program, developing technology on the part of the AEC, is to define the technology that must be developed. And this is one of the main goals of the AEC Plant Study program.

The Plant Study program is a series of studies which are initially broad, and later will narrow down to specific problem areas that require detailed attention. The 1000-MWe Follow-on Studies that we are reporting upon today represent the first or broad step in this program.

The history of these studies goes back to 1963 and 1964 when an initial group of parallel studies were performed by four contractors or groups of contractors. These first studies were limited to the reactor core, the reactor vessel, core instrumentation, control and safety systems, and fuel-handling mechanisms. The current studies are a "follow-on" of the early studies (hence the name), but in these latest studies the scopes have been expanded to include the total plant concept, with the primary objectives emphasizing the identification of research and development needed. However, the objectives are *met* through the development of plant design concepts which are optimized through detailed trade-off evaluations and parametric studies. The present studies began in 1967 and are close to completion now. We expect that they will be almost entirely complete by the end of 1968.

The scopes of work of all five contractors have been essentially the same, with certain modifications depending on funding levels. The work was divided

into four major tasks, structured on the principle that before one can define research and development needs, one must define the plant toward which the research and development is to lead. Therefore, the first three tasks define the plant, and the fourth defines the research and development program.

Task I is a concept-selection task, wherein through preliminary trade-off studies and analysis, the contractor has selected his own company-favored preliminary reference concept of a plant that could be marketed commercially in 1975 to 1985, and be in commercial operation in 1980 to 1990. The second task was one of more detailed engineering wherein by conducting disciplined engineering studies on the selected reference concept and developing it, using a system-oriented approach, the contractor would wind up with a fairly well-defined design defined through the preparation of System Design Descriptions on the major plant systems. The third task was then to evaluate and optimize the concept by performing detailed trade-off evaluations on various plant features, such as the evaluation of the relative virtues of the pool-type primary system vs. the loop- or piped-type primary system, and other such major features of the plant. Task III also included parametric studies, plant optimization, and finally preparation of capital and operating cost estimates to define the economics. The fourth task, which is not yet completed, is one of delineating the research and development needed to build that plant. This task consists first in identifying the new information needed, and ultimately in delineating the program that is needed to obtain that information.

As I mentioned before, work on the first three tasks is essentially completed now by all of the contractors, and final report preparation is in progress. The work on Task IV will follow a few months later. I should point out that this is the first integrated public exposition of the results of these studies. We hope to be able to issue the reports on Task I within the next few months, through the AEC Division of Technical Information Extension at Oak Ridge. The remainder of the reports, concerning Tasks II, III, and IV, we hope to have released early in 1969.

Argonne's role throughout this entire study program has been to review the contractors' work during the

course of the contracts, and to provide guidance based upon Argonne's experience with EBR-II and in the fast reactor program in general. However, I would like to reemphasize one point, that the goal has not been to make all of the designs Argonne designs. The objective of the program is that each of the contractors is to develop his own company's design, and therefore we have been very careful not to specify design changes which would destroy that principle. Argonne will also prepare a final evaluation report on the studies, which should be issued about a year from now. The goal in this evaluation will not be to pick the best design of the five, but rather to expose the technological problems and recommend action for their solution.

Regarding future studies, you will recall that I mentioned earlier that the follow-on studies were the first broad phase of a study which will narrow down

to more specific problem areas. Within the next few months we hope to be able to initiate further, more detailed studies among the five contractors wherein the contractors will take their reference designs developed during the studies upon which they are reporting today, and look deeper into such factors as plant safety, detailed questions in the design of the pool-type primary system in some cases and the loop-type system in others, detailed questions regarding refueling, control, and such. We hope that these further studies will start shortly after the first of the year.

I should make a final observation that you will, I am sure, notice differences in the work covered by the various contractors, and you will notice differences in the depth to which the various items were studied. These differences are the result of variations in the funding level that was granted each contractor, which varied among some of the contractors by almost a factor of three.

Atomics International 1000-MWe LMFBF Follow-on Study Progress Report

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I. INTRODUCTION

A. Objectives of the Follow-on Study

Atomics International (AI) is one of the five contractors for the 1000-MWe Liquid Metal Fast Breeder Reactor (LMFBR) Follow-on Study Program which is being managed by Argonne National Laboratory for the Atomic Energy Commission (AEC).^{*} The overall objectives of this program are to identify the research and development (R&D) necessary to lead to safe, reliable, and competitive LMFBF central-station power plants in the 1980's and to establish the relative ranking of the needed R&D. The study objectives are being met through the development of a series of industry-favored reference plant designs and evaluation of these designs to determine the R&D program necessary to proceed with the final engineering and construction of the proposed plants.⁽¹⁾ This paper reports on the major considerations, evaluations, and analyses which led to the selection of the AI 1000-MWe LMFBF reference design.

The LMFBF Program Plan⁽²⁾ provides important guidance on the relationship between the 1000-MWe LMFBF Study Program and on-going individual demonstration plant programs, as follows:

"Major reactor manufacturers, in conjunction with utility groups, are studying demonstration plants for near-term commitment. These manufacturers, therefore, are studying both near-term demonstration plants and longer-range target plants. Accordingly, the reference designs resulting from the AEC funded studies of target plants are expected to be closely related to those from corresponding studies by industry of demonstration plants."

Atomics International, in developing its 1000-MWe reference design, has closely coordinated the Follow-on Studies with the Company-sponsored demonstration-plant program as well as with the recently completed study of the technical and economic performance of a minimum R&D 1000-MWe FBR performed for the Empire State Atomic Development Associates (ESADA). The relationship of these programs and the logic behind AI's approach to the Follow-on Study are shown in Fig. I-1.

^{*} ANL Contract No. 31-109-38-1996.

B. AI's Work Scope

The Follow-on Studies have been divided into four tasks.⁽¹⁾

Task I comprised the concept selection and the establishment of all significant design criteria. Under this task AI reexamined the major trade studies performed in the Company program in light of the criteria for the Follow-on Study Program, which are discussed in Sect. II. These trade studies included (1) Pool *vs* Loop, (2) Fuel Handling, (3) Vented *vs* Nonvented Fuel, and (4) Number of Loops. Because of their importance in setting the basic plant configuration and its performance characteristics, these tasks were selected for review in Task I. The pre-selection of mixed-oxide fuel and a regular core geometry was based on previous work, and was not reevaluated. The next step was to define a preliminary reference design for a 1000-MWe plant whose major design and performance characteristics are similar to those for the current AI demonstration-plant reference design. The results of the Task I effort were reported at the recent Cincinnati meeting⁽³⁾ and will therefore receive only a cursory treatment in this presentation.

Tasks II and III were combined for the AI study. These tasks comprised a detailed safety evaluation, parametric studies of core and system, and alternative studies of plant and equipment configuration. These studies have led to the reference design described in this paper. System-design descriptions are being prepared to describe this final reference design. An estimate of plant capital cost, a cost analysis for the fuel cycle, and an overall economic assessment will be performed at the conclusion of Task III.

Task IV, which is in the initial stages at AI, will describe the information required before final design and construction of the proposed plant can begin, and will outline the necessary R&D that can most effectively provide the needed information.

C. AI Program Logic and Study Approach

Fig. I-1 shows the AI program logic and the resulting study approach. When AI initiated work on its Follow-on Study contract, we had completed most of the key trade studies as part of the Company-

FIG. I-1. Program Logic.

sponsored Fast Breeder Reactor program. The conceptual design of the demonstration plant for this program was in an advanced stage, and we were in the midst of a parallel study of a 1000-MWe LMFBR plant under ESADA sponsorship. The following were the principal guidelines used in selecting the demonstration-plant design:

- 1) maximum use of state-of-the-art and proven technology in order to obtain a low-risk demonstration plant (this resulted in conservative design ratings, such as 1200°F for the clad temperature limit and use of ferritic material for superheaters);
- 2) schedule based on a 1974–1975 startup date, thus dictating completion of the required R&D program by about 1970;
- 3) conservative safety philosophy, which is believed to be required for the LMFBR demonstration plants;
- 4) basic applicability to future plants which are economic.

The primary objective of the ESADA study was the definition of the technical and economic characteristics of a 1000-MWe plant design that would represent a direct extrapolation of the AI demonstration plant and would thus require a minimum of R&D beyond that planned for the demonstration plant. In keeping with this objective, both plants use the same fuel elements, control elements, fuel-handling equipment, intermediate heat exchangers, and steam-generator modules, except for quantities involved; sodium pumps, valves, and reactor vessel and struc-

ture are components that will require extrapolation in size but would only require a minimum of testing beyond that planned for the demonstration-plant hardware. The ESADA study has thus provided a measure of the potential LMFBR performance under a minimum R&D program.

Information generated under these programs, as well as AI's evaluation of the expected information from the AEC and foreign on-going programs, provided the starting point for Task I. The ESADA study and Task I effort have been closely coordinated to produce the preliminary reference design mentioned earlier (see Block 1 of Fig. I-1). A preliminary estimate of capital costs, a fuel-cycle cost analysis, and an availability analysis were prepared for this design (Block 2) and compared with a LWR competitive target plant (Block 3). Both plants were assumed to begin operation in the early 1980's and were compared over a 20-yr period on the basis of anticipated economic trends from an expanding combined LWR/LMFBR reactor economy.

The term "competitive" requires further definition. We define a new energy source to be competitive if it has a sufficient economic advantage to provide the necessary incentive for its application. More specifically, we believe that to be competitive the LMFBR should have at least a 10% energy cost advantage (approximately 0.4 mill/kWh) over a contemporary LWR. The economic assessment performed under the ESADA study indicated a 0.1 to 0.3 mill/kWh advantage for the Task I/ESADA design. Since this cost advantage did not meet our established criteria,

TABLE I-1. MAJOR DIFFERENCES BETWEEN PRELIMINARY REFERENCE DESIGN AND FINAL REFERENCE DESIGN

	Preliminary Reference Design (Task I)	Final Reference Design (Task II)
Overall Plant		
Power output (MWt/MWe)	2470/1000	2400/1000
Net Plant efficiency (%)	40.7	41.7
Breeding ratio core/total	0.89/1.27	1.04/1.37 ^(a)
Reactor fissile mass (kg Pu)	2210	2830 ^(a)
Doubling time (yr)	10	9 ^(a)
Turbine Plant		
Steam conditions (psig/°F/°F)	2400/900/900	2400/1000/1000
Turbine type	Cross-compound, 3600/1800-rpm, 4-flow, 43 LSB	Tandem-compound, 3600-rpm, 6-flow, 33½ LSB
Reactor		
Core height/diameter (in.)	50/93	43/
Pin OD/ID (in.)	0.250/0.220	0.300/0.265
Outlet temperature (°F)	1060	1140
Temperature rise (°F)	300	360
Refueling interval (months)	6	12
Fuel-spacer type	Grid	Wire-wrap
Spent-fuel cooling	In-vessel	Ex-vessel
Cell-volume fractions—Na/fuel/SS	0.48/0.33/0.19	
Steam-generating Plant		
Number of heat-transfer circuits	3	3
IHX circuits	2	1
Steam-generator modules/circuit	66	13
Superheater material	Ferritic	Austenitic
Reactor building height/diameter (ft)	195/176	191/140

^(a) Estimated from parametric analysis and subject to change.

some further upgrading of the reference design was required. Various potential improvements have been identified and evaluated in Task III as a result of the parameter survey (see Block 4 of Fig. I-1) and the alternative configuration studies (Block 5). A preliminary assessment of these potential improvements on the basis of the required R&D led to a ranking of those improvements that were deemed achievable by the mid-1970's (Block 7), the period by which AI expects to market its first-generation commercial (1000-MWe) plants.

Having established the criteria for competitiveness (~0.4-mill/kWh margin) and the necessary improvements to meet this criteria (e.g., 0.1 to 0.3 mill/kWh), we entered the potential improvement list and selected as many of these as were required to meet the desired objectives. These improvements were then incorporated into the preliminary reference design to obtain a modified reference design which now meets both the technical and economic criteria established on this program (see Block 8 of Fig. I-1). The remaining steps involve the identification of the minimum R&D requirements necessary for the demonstration plant and the preliminary reference plant (Block 9), as well as the additional R&D requirements to facilitate the selected improvements (Block

10). This combination defines the total R&D program required for the final reference design, which will be described in Task IV (Block 11).

D. Summary of Program Status

Again referring to Figure I-1, we have just recently selected the final Task III (Block 8) reference plant based on the evaluation of potential improvements. The analysis of this design is now underway. The core thermal and hydraulic analysis is in progress. The heat balance for the new steam conditions and the sizing of the process equipment are complete. This work will complete Task III. The Task IV work defining the R&D requirements and R&D programs has just been initiated (Blocks 9 and 10).

E. Brief Summary of Major Design Changes

Since we have not completed the economic analysis of the final reference design, we shall attempt to arrive at an economics evaluation by using the analysis performed for the preliminary reference design as a base. The capital costs, fuel-cycle costs, and availability for this preliminary design will be modified to reflect the various improvements incorporated in our final design.

The preliminary reference design, which was the

product of the Task I effort, was reported at the recent Cincinnati meeting.⁽³⁾ Table I-1 highlights the major changes from this design to the new reference design. These changes and their economic impact are summarized below.

1) Reactor outlet temperature has been increased from 1060 to 1140°F, with a corresponding improvement in steam-cycle conditions and thermal efficiency. These lead to a significant reduction of capital cost with essentially no change in fuel-cycle costs. Although the higher outlet temperature may reduce fuel burnup somewhat, it should be largely compensated by the higher thermal efficiency.

2) The new core design features larger pins, a somewhat shorter core, higher reactor ΔT , and a continuous spiral-wire pin spacer in place of a grid spacer. These lead to a significant reduction in fuel-cycle costs while facilitating annual refueling in lieu of a semi-annual refueling schedule (because of lower specific power and higher in-core breeding ratio). Annual refueling, when integrated with annual turbine maintenance, results in 2 to 3% improvement in plant availability.

3) Spent fuel can now be removed from the reactor vessel in a programmed manner shortly after reactor shutdown and can be stored externally to the primary system until ready for shipping. This capability eliminates the need for prolonged in-vessel storage until the next refueling, with the attendant fuel-inventory penalties for such storage. This capability also facilitates the incorporation of annual refueling with little or no economic penalty.

4) The straight shell-and-tube steam-generator module configuration has been changed to a "hockey-stick" configuration. The new configuration preserves the many excellent features of the basic AI modular steam-generator design while removing its major disadvantage, namely, limited capability for accommodation of differential thermal expansion under severe transients. The new design permitted increase in module size by a factor of four with significant reduction in capital costs. In addition, the higher reactor outlet temperature justified a substantial increase in the sodium inlet temperature to the steam generator, thereby requiring the replacement of $2\frac{1}{4}$ Cr-1 Mo alloy with austenitic superheater material. The new temperature conditions also reduced the surface requirement with a further significant reduction in capital cost.

5) The new design retains the three-loop system but features a single IHX unit per loop instead of two half-size units. This change, coupled with more efficient utilization of space, led to a reduction in the reactor building diameter from 176 to 140 ft.

6) Because of the lower capital cost resulting from the use of the tandem machine, we have switched from a 3600/1800-rpm cross-compound turbine with 43-in. last-stage buckets to a 3600-rpm tandem-compound, six-flow, 33½-in. last-stage buckets.

F. Economics and Overall Conclusions

As stated, the economic evaluation of the final reference design is just getting underway. In this paper we shall review the analysis performed for the Task I/ESADA preliminary reference design and then evaluate the differential worth of each of the above improvements to arrive at a new energy cost (relative to a LWR) corresponding to the final reference design.

In assessing the economic competitiveness of the LMFBR, we addressed ourselves to the following three questions:

1) How much lower are LMFBR fuel-cycle costs likely to be as compared to those of light-water reactors, particularly during the early years of LMFBR introduction?

2) What is the probable differential in capital costs between a first-generation commercial LMFBR and the then current light-water reactors?

3) Taking (1) and (2) into account, what must be the performance characteristics of an early LMFBR, particularly fuel-burnup level, in order for it to be economically competitive with light-water reactors during the early 1980's?

The approach taken in attempting to answer these questions has been to establish ground rules by:

- 1) projecting an expanding combined LWR/LMFBR economy over a 20-yr period (1980-2000) based on a plausible rate of LMFBR introduction;
- 2) estimating the growth in fuel-fabrication and reprocessing requirements, uranium requirements, and plutonium production during the selected period;
- 3) coupling these growing requirements with correlations of processing costs vs production level to obtain trends in specific fabrication and reprocessing costs as a function of time (see Table I-2);

then determining:

- 4) fuel-cycle cost trends for both the preliminary reference LMFBR plant and a typical light-water reactor over the 20-yr period (see Fig. I-2);
- 5) the capital-cost differential compared with a light-water reactor (see Table I-3);
- 6) the economic competitiveness of the preliminary reference LMFBR by comparing the present

fuel-cycle cost savings at various burnup levels vs the estimated range of capital-cost differentials (see Fig. I-2).

Referring to Fig. I-2, we note that LMFBR fuel-cycle costs decrease sharply during the initial 10 yr of operation. These estimated fuel-cycle costs are based on a constant average burnup of 75 MWd/kgH. This trend reflects the anticipated sharp decrease in fuel-fabrication and -recovery costs with increasing processing loads. It does not reflect any improvements in fuel performance and fuel-fabrication techniques. Note that LMFBR fuel-cycle costs are projected to have a substantial edge (0.4 mill/kWh in the early 1980's to 0.9 mill/kWh by the late 1990's) over contemporary LWR's, even under the modest assumed

TABLE I-2. PROJECTED TRENDS FOR LMFBR AND LWR FUEL-CYCLE COST DATA

	1981-85	1986-90	1991-95	1996-2000
U ₃ O ₈ Cost (\$/lb)	8	9	10	12
Fissile Plutonium Value (\$/gm)	10	11.2	11.7	12.5
LWR				
Fuel-fabrication cost (\$/kg U)	50	45	42	42
Fuel-recovery cost (\$/kgU)	30	27	25	25
LMFBR				
Fuel-fabrication cost (\$/kg H ^(a))	320	240	200	170
Blanket-fabrication cost (\$/kgU)	60	50	45	40
Fuel/axial blanket-recovery cost (\$/kgH)	100	90	78	65
Radial blanket-recovery cost (\$/kgH)	85	65	58	48
Fuel ex-core holdup time (days)	300	235	180	180

(a) kgH—kg of heavy isotope (U + Pu).

TABLE I-3. LWR-LMFBR CAPITAL-COST COMPARISON^(a)

Account	Item	\$/Net (kWe)		
		Nuclear Island	Balance of Plant	Total
21	Structures and improvements	16.2	5.9	22.1
22	Reactor plant equipment	76.8	10.4	87.2
23	Turbine-generator plant equipment	—	39.8	39.8
24	Accessory electric equipment	1.9	3.8	5.7
25	Miscellaneous power plant equipment	0.6	1.1	1.7
	Total installed cost (rounded)	96	61	157
	LWR Cost	68 to 73	67 to 72	135 to 145
	Capital cost differential	23 to 28	(6 to 11)	12 to 22

(a) Costs are unescalated and based on late 1967 prices.

NOTE: INVENTORY CHANGES REFLECT A 12% WORKING CAPITAL CHARGE RATE

FIG. I-2. Comparison of LWR vs LMFBR Fuel-cycle Costs Trends (mills/kWh).

burnup level of 75 MWd/kgH. Also, the LMFBR will enjoy very low incremental operating costs (fuel expense), i.e., 0.5 mill/kWh lower than LWR, even in early life. The low incremental cost for the LMFBR should favor this plant in loading schedule. Therefore, the assumption of a capacity factor of 0.8 for both the LWR and LMFBR probably understates the economic benefits of the LMFBR.

The estimated capital costs for the preliminary reference design are summarized in Table I-3. The costs of engineering services, construction manage-

TABLE I-4. UNAVAILABILITY ESTIMATE FOR THE PRELIMINARY REFERENCE DESIGN

Item	Unavailability (%)		
	Un-sched-uled	Extensions of Sched-uled Shut-downs	Scheduled
Refueling—Nominal scheduled		1.97 ^(a)	2.80 ^(a)
Turbine overhaul			3.84 (14 days/ yr)
Reactor	0.53	0.03	
Heat-transfer system	5.92	0.16	
Turbine-generator plant	1.65 ^b	0.11	
Control and instrumentation	0.48 ^b	0.01	
Auxiliary systems	0.62	0.13	
Electrical	0.05	0.03	
Support equipment		0.02	
	9.25	2.47	6.64
Grand Total (rounded) (18%)			

^(a) Accounts for one 10-day refueling per year. Other semi-annual refueling masked by annual turbine inspection. 85% efficiency assumed throughout operations.

^(b) Included two environment-caused reactor trips per year.

ment, and other related construction indirects have been distributed over the five major accounts. These costs reflect late 1967 prices and make no provision for escalation. The total installed cost for the LMFBR is estimated at \$157/kWe.

Comparable estimated capital costs for a LWR are also noted in Table I-3. The total cost of \$135 to 145/kWe was determined by review of the mass of published cost data on LWR's.⁽⁴⁻⁷⁾ Since the balance-of-plant can be reliably estimated to be \$6/kWe to \$11/kWe less for the LMFBR unit, the indicated total differential cost therefore results in a \$23/kWe to \$28/kWe higher cost for the nuclear island of the LMFBR. We consider this differential to be somewhat of an upper limit, even for an early plant based on a comparison of the basic differences in system components and construction features of the respective nuclear islands.

The large investment in power stations consisting of single units with generating capacities greater than 1000 MWe, coupled with the ability of a breeder to produce excess fuel, causes the unit's "on-line" capability to have a very large economic effect on its commercial worth. Therefore, an appraisal was made of the availability of the preliminary reference design as part of the ESADA study.

A definition commonly used to describe the availability of conventional steam plants is "operating availability," defined⁽⁸⁾ as the percentage of time a

unit is available for service whether operated or not. This "time basis" availability is not completely suitable for the 1000-MWe FBR since the multi-loop design presents the possibility of a single-loop forced outage (forced power reduction to 70% of nameplate). With a time-basis definition, no availability penalty would result from this single-loop outage. Plant energy availability has therefore been used in this study. It is defined as the percentage of rated plant energy that the mature plant is capable of producing. For example, if the 1000-MWe plant is capable of producing 9,000,000 MWh in a 10,000-hr period, the availability would be 90%.

The estimated plant-energy unavailability for the preliminary reference design is shown in Table I-4. This results in an energy availability of 82% and a corresponding "operating availability" of approximately 85%. The target availability for this plant is 90%. As noted from the table, the incorporation of annual refueling with concurrent turbine maintenance in the final reference design reduces the potential unavailability by ~3%, thereby greatly facilitating the achievement of the target availability goals.

Finally, a 20-yr present worth analysis (at 7% discount factor) was performed to determine the competitiveness of the LMFBR. Differential capital cost and burnup level for LMFBR fuel were treated as major parameters. LMFBR operation, maintenance, and insurance costs were adjusted to reflect potential higher costs for early LMFBR plants compared to the then mature LWR plants. Differential capital costs were amortized at a 14% fixed charge rate. The results of this analysis are summarized graphically in Fig. I-3, which shows the 20-yr cumulative present worth savings as a function of differential capital costs with LMFBR fuel burnup as a parameter. The horizontal lines translate the dollar savings to a mills/kWh levelized energy-cost advantage for the LMFBR as compared to the LWR.

We thus note, for example, that at the estimated mid-range capital cost differential of \$17/kWe and the assumed reference burnup level of 75 MWd/kgH, the LMFBR offers an energy-cost advantage of 0.2 mill/kWh. Considering the probable range in the capital cost differential (\$12 to 22/kWe) and the uncertainties in achievable burnup (60 to 100 MWd/kgH), we note that this cost advantage may vary from zero (break-even) to about 0.4 mill/kWh. In our study, we assumed a more probable range for this cost advantage of 0.1 to 0.3 mill/kWh.

As noted earlier, this cost advantage is not sufficient to meet our established criteria for economic competitiveness. We have, therefore, modified our

preliminary reference design to a more advanced design incorporating the improvements described in Sect. I-E.

Table I-5 summarizes the economic impact of each of the selected design and/or performance improvements incorporated in the final reference design. The total reduction in energy cost due to each improvement is the net result of the changes in capital, fuel-cycle, and availability costs for the particular improvement. These improvements have been further grouped into two categories: those that apply at the 1060°F reactor outlet temperature (Lines 1 through 11), and those that result from the increase to 1140°F outlet temperature (Line 13). The combined worth of these improvements is 0.51 to 0.65 mill/kWh, corresponding to a burnup range of 67 to 100 Mwd/kgH. Note that about 80% of this cost improvement may be realized without the need to go to the higher reactor outlet temperature, and such a plant may in fact be considered competitive under the definition discussed earlier. This is quite significant since the increased outlet temperature design will require considerably more R&D, particularly on in-pile fuel-

FIG. I-3. FBR vs LWR Competitiveness as a Function of FBR Average Core Burnup and Differential Capital Cost.

TABLE I-5. IMPROVEMENTS TO PRELIMINARY REFERENCE DESIGN

PARAMETER	CHANGE IN PARAMETER	TOTAL ENERGY COST REDUCTION (mills/kwh) (a) + (b) + (c)	CAPITAL COST		FUEL CYCLE COST mills/kwh (b)	AVAILABILITY	
			10 ³ \$	mills/kwh (a)		%	mills/kwh (c)
1. REACTOR BLDG DIAM	176 → 140 ft	0.028	1,400	0.028	-	-	-
2. NUMBER OF IHX's/LOOP	2 → 1	0.005	260	0.005	-	-	-
3. STEAM GENERATOR MODULES	91 → 382 TUBE	0.104	5,200	0.104	-	-	-
4. AUSTENITIC STEAM GENERATOR							
T _{SI} *	950° → 1000°F	0.021	440	0.009	0.012	-	-
T _{STEAM}	900°/900° → 950°/950°F						
5. TURBINE TYPE	X-COMPOUND → T-COMPOUND - 3600/1800 rpm 3600 rpm	0.048	>3,200	0.064	(0.016)	-	-
6. REFUELING PERIOD	6 months → 12 months	0.051	(170)	(0.003)	-	2.7	0.054
7. PIN SIZE (in.)	0.250 → 0.300	0.042	(460)	(0.009)	0.051	-	-
8. SPACER TYPE	GRID → WIRE WRAP	0.076	300	0.006	0.070	-	-
9. CORE HEIGHT (in.)	50 → 43	0.014	590	0.012	0.003	(0.05)	(0.001)
10. SMEARED DENSITY (% T.D.)	80 → 85	0.030	-	-	0.030	-	-
11. LOWER AX. BLKT. THICK (in.)	12 → 15	0.006	-	-	0.006	-	-
SUBTOTAL WITHOUT INCREASE IN OUTLET TEMPERATURE AT 75 Mwd/kgH [SUM ITEMS (1) THROUGH (11)]		0.425	10,760	0.216	0.156	2.65	0.053
12. FUEL BURNUP (Mwd/kgH)	75 → 100	0.080	-	-	0.080	-	-
SUBTOTAL WITHOUT INCREASE IN OUTLET TEMPERATURE AT 100 Mwd/kgH [SUM ITEMS (1) THROUGH (12)]		0.505	10,760	0.216	0.236	2.65	0.053
13. REACTOR OUTLET TEMPERATURE (T _{RO}) WITH AUSTENITIC SG							
T _{RO}	1060 → 1140°F	0.090	5,500	0.110	(0.020)	-	-
T _{SI}	1000 → 1070°F						
T _{STEAM}	950 /950 → 1000 /1000°F						
FUEL BURNUP (Mwd/kgH)	75 → 67						
SUBTOTAL AT 1140°F REACTOR OUTLET TEMPERATURE AND 67 Mwd/kgH [SUM ITEMS (1) THROUGH (11) AND (13)]		0.515	16,260	0.326	0.136	2.65	0.053
14. FUEL BURNUP (Mwd/kgH)	67 → 100	0.140	-	-	0.140	-	-
TOTAL ALL IMPROVEMENTS [SUM ITEMS (1) THROUGH (11), (13) AND (14)]		0.65	16,260	0.326	0.276	2.65	0.053

NOTE: FIGURES IN () ARE COST PENALTIES

*T_{SI} - SUPERHEATER SODIUM INLET TEMPERATURE

cladding behavior at approximately 1300°F. We therefore conclude that our improved reference design should be economically attractive as compared to a contemporary LWR with both plants committed by the mid-1970's. Furthermore, it appears that even if the required R&D in support of the higher outlet temperature design could not be completed, the LMFBFR would still be competitive on the selected time scale.

II. PLANT DESIGN BASES

The established design criteria for this study fall into four categories: (1) ground rules, (2) utility operational requirements, (3) safety requirements, and (4) economic requirements.

The salient requirements falling under each of the above categories are summarized below.

A. Ground Rules (Established by ANL)

- 1) The plant will be a 1000-MWe (net) sodium-cooled fast breeder reactor operating on the uranium-plutonium cycle.
- 2) The proposed plant will be of a design which will permit its initial commercial commitment within the period from 1975 to 1985. As noted previously, AI has planned its plant design for the early part of this period.
- 3) Optimization of the plant design will reflect U.S. investor-owned utility-financing conditions.

B. Utility Operational Requirements (Established by AI)

The plant shall be designed as a base load plant of low incremental power cost, but shall have provisions for following normal load changes.

C. Safety Requirements

The AEC General Design Criteria have formed the basis for the main safety guidelines and safety design features incorporated in the reference design. A summary of those guidelines most influential in the design, together with brief statements of the manner of their implementation, is described in the following sections. This summary is provided to highlight the influence of safety on the plant design.

Guideline No. 1

The reactor building complex will be designed to assure its mechanical integrity under maximum accident conditions, so as to meet the guidelines established in 10 CFR 100.

Containment and radioactivity control over any radioactivity released from the primary system is

achieved by providing a reactor building consisting of an outer and inner containment barrier (see Fig. II-1).

The outer containment barrier is a reinforced-concrete, all-welded steel, containment building. This building is designed for conditions which exceed the maximum credible transients of pressure and temperature associated with the design-basis accident, and within leakage limits (0.5%/day at 10 psig and 100°F) selected, in part, to meet the site-exposure guidelines set forth in 10 CFR 100, with adequate margins for uncertainties.

The inner containment barrier, as defined by the metal-lined, reinforced-concrete vaults and the top shield containment dome, containing the reactor, primary system, and associated equipment, is designed to maintain an inert gas atmosphere (2% O₂ maximum) during reactor operation.

Engineered safeguards are provided to maintain the effectiveness and integrity of the containment boundaries, during and following the design-basis accident. The following systems are provided: reactor cavity liner cooling system, top-shield holddown, pressure-relief discs between equipment vaults, and outer containment atmosphere cleanup system.

Guideline No. 2

The system design will provide highly reliable containment and control over liquid metals, to prevent or limit fire and other chemical interactions.

All primary (radioactive) sodium equipment is contained within metal-lined shielded vaults with an inert atmosphere. Each of the three primary loops has isolation valves, as well as a check valve to minimize thermal shocks if a pump fails. The primary sodium is isolated from the water in the steam generators by the secondary sodium loops. The secondary sodium loops are protected against high pressure, which would result from a steam-generator failure, by a relief system that provides for the safe handling of the reaction products.

Guideline No. 3

Design provisions will be made to assure highly reliable and redundant emergency core-cooling systems.

1. Reactor Cooling

Three independent heat-transfer circuits are provided for normal and emergency reactor cooling. Each circuit is designed to provide the capability for removing all the reactor decay heat by natural convection.

Protection against Reactor Coolant Loss

A reactor containment tank and an elevated loop assure that, in the event of a pipe rupture or failure of the reactor vessel, the core inlet and exit nozzles remain covered with coolant. This feature, together with an elevated loop design, assures free convection cooling in the primary loops.

Guideline No. 4

The control and protective system will be sufficiently independent, redundant, and rapid to prevent significant damage to the core from all credible accidents.

Two separate and independent banks of absorber rods are provided for protective-system functions. Both banks can be driven into the core at a fast setback rate.

The shim-safety control-rod system is capable of being driven in or out of the core by positive electro-mechanical means, and of being released for gravity drop. The maximum worth of the most effective shim-safety rod is limited, such that a control-rod ejection at full power will not cause fuel-rod damage (cladding failure) in the hot channel. A fast setback rate is also available.

The safety-rod system employs gravity drop for emergency shutdowns, and provides a large margin of reactivity worth to override a major refueling error. The safety rods also can be driven in by a positive mechanical drive.

Guideline No. 5

The core will be designed to provide an overall negative power coefficient at all operating conditions; the integrated Doppler effect from operating conditions to the onset of fuel damage will be sufficient to counteract all credible rapid reactivity perturbations; the reactor and fuel elements will be designed to minimize interaction between fuel elements which could lead to significant fuel element-to-element failure propagation.

The core design is such that the combination of the positive coolant temperature coefficient and the Doppler coefficient results in a net negative overall power coefficient during startup and at power operating conditions. It is designed so that fuel-subassembly bowing does not result in a positive reactivity insertion. The local power coefficient is negative in all core positions. The core and reactivity control system are designed so that all credible rapid reactivity perturbations are less than those which can be compensated for by the negative Doppler effect. Each fuel element is provided with exit thermocouples to de-

FIG. II-1. Reactor Containment.

tect abnormal temperatures and to permit control against damage propagation. The element housing thickness is adequate to prevent failure propagation.

D. Economic Requirements (Established by AI)

The plant will be economically competitive with alternative energy sources beginning operation in the early 1980's. As discussed previously, we interpret this to mean that the proposed LMFBR design must show a levelized energy-cost advantage of at least 10% as compared to a contemporary LWR over the initial 20 yr of operation. This economic goal must be met with a design promising an inherent "operating availability" of 90%. The corresponding "energy availability" goal for a three-loop plant is estimated at about 87%.

III. TRADE STUDIES AND PARAMETRIC ANALYSES

A. Background and Introduction

The overall program logic of the 1000-MWe Follow-on Study Program was covered previously. As was discussed, an initial 1000-MWe plant was designed based on direct extrapolation of a 500-MWe demonstration plant, and is referred to as the "preliminary reference design." Many trade studies and parametric analyses were performed in order to select the basic design of the 500-MWe demonstration plant. Table III-1 summarizes the various trade studies which led to the design of the demonstration

TABLE III-1. SUMMARY OF DEMONSTRATION-PLANT TRADE STUDY

	Trade Study	Selection		Trade Study	Selection
Overall system	Pool <i>vs</i> loop Plant containment	Loop Double-inerted inner barrier	Reactor (cont'd)	Control-rod configuration Control-rod absorber Fuel-element spacer Forty-four others	Bottom latch—gas purged Tantalum Grid type
	Reheat <i>vs</i> nonreheat Number of coolant circuits Cover gas Eleven others	Reheat Three Helium	Heat transfer	Pump location IHX type Steam-generator type Steam-generator material	Hot leg Single unit Modular Ferritic
Reactor	Vented <i>vs</i> nonvented fuel Core geometry Emergency cooling Open <i>vs</i> closed lattice Location of gas plenum in fuel Fuel-element size Linear power of fuel	Nonvented Regular Natural convection Closed Top 217 pins 15 kW/ft	Fuel handling	Fuel-handling method Double <i>vs</i> triple plug Refueling interval Concentric <i>vs</i> eccentric core Thirty others	Rotating plug—in-vessel storage Double Six months Eccentric

TABLE III-2. POTENTIAL IMPROVEMENTS CONSIDERED FOR TASK III

Design Point Changes	Configuration Changes
Outlet temperature	Type of fuel-element spacer (wire wrap <i>vs</i> grid)
Fuel linear power	Superheater material (austenitic <i>vs</i> ferritic)
Primary/secondary ΔT	Elimination of valves
Primary ΔP	Fuel handling—Direct fuel removal from storage
Fuel burnup	Base load operation
Maximum coolant velocity	Alternative clad material
Steam conditions	Design Changes
Changes of Plant Parameter	Vented <i>vs</i> Nonvented fuel pin
Refueling interval	On-line fuel removal
Fuel-pin size	Bellows-type IHX
Fuel-clad thickness	Steam-generator turbulator insert
Size of reactor building	Turbine (cross compound <i>vs</i> tandem compound)
Fuel smear density	Fuel-element open housings
Number of coolant circuits	Variable blanket orifices
Number of IHX's per loop	Steam-generator tube joint—explosive bond <i>vs</i> welded
Core enrichment zones	Backup shutdown device/emergency cooling systems—elimination of present DBA
Size of steam-generator module	Elimination of outlet thermocouples
Amount of ex-vessel storage in building	Elimination of cover-gas cleanup
IHX sodium velocity	
Blanket length and residence time	

plant and indicates the basic design characteristics of that plant.

The preliminary reference 1000-MWe design was then established based on a straightforward scale-up

of this demonstration-plant technology, using little or no additional R&D work. This 1000-MWe preliminary reference design is described elsewhere.⁽³⁾

The economics of this early plant were evaluated in order to determine whether or not a plant based on this demonstration-plant scale-up would be economic in comparison to light-water reactor systems. The economic comparison of the preliminary reference design with the light-water plant was discussed in Sect. I-F (see Fig. I-3). It is seen that although the preliminary reference design is calculated to be competitive with light-water reactor systems, it is necessary to improve the competitive margin in order to meet the criterion of a 10% power-cost advantage discussed in Sect. I-C.

The improvements selected to establish an LMFRB design meeting the economic goals were determined from further trade studies and parametric analyses performed in Task III. The possible improvements considered fall in the areas of design configuration, material selection, and changes in design parameters (i.e., temperature levels, pin sizes, etc.). A complete list of the improvements which were considered is found in Table III-2.

The basic purpose of the trade studies and parametric analyses was to evaluate the economic improvements associated with the proposed changes. Based on this evaluation, an improved and advanced plant design which would meet the economic ground rules could then be selected, using the criteria that research and development work should be completed in time for an advanced plant in the early 1980. This advanced plant is referred to as the "reference" or "final reference" design.

B. Approach and Model

The requirements and constraints needed in order to begin the trade studies were first formulated based on the overall plant requirements. These are shown in Table III-3. The total possible list of improvements shown in Table III-2 was then limited, based on engineering judgment, to those which appeared to be most effective and which could be studied in the 1000-MWe Follow-on program. A list of independent variables and design options was selected for inclusion in the Task III studies. The selected items are shown in Table III-4. The next step was to construct an

TABLE III-3. REQUIREMENTS, CONSTRAINTS, AND ASSUMPTIONS

Performance	Design
1000-MWe net electrical ~2500 MWt	217 pins per element (direct fuel-removal limit)
Operation	Two-row radial blankets
90% availability	Double rotating plug fuel handling
Swing load operation	Tantalum control
Refueling either at 6-month or 1-year intervals	Modular steam generator
Direct fuel removal	Limit clad stress (adjust thickness and plenum height)
Safety	75 MWd/kgH fuel-burnup capability with 1200°F hot-spot temperature of clad
Negative power coefficient	
Contain major core excursion	
Adequate convection flow	
Elevated loop	
Double barrier containment	
Limited rod worth	

TABLE III-4. INDEPENDENT VARIABLES AND DESIGN OPTIONS STUDIED FOR TASK III

Independent Variable	Range
Fuel-pin size	0.200 to 0.300 in.
Core height	18 to 50 in.
Core outlet temperature	800 to 1200°F
Primary ΔT	150 to 470°F
Secondary ΔT	105 to 490°F
Fuel-element burnup (avg)	60 to 120 MWd/kg
Fuel smear density	72 to 88%
Linear power of fuel	12 to 20 kW/ft
Steam conditions	2400/900/900 to 2400/1000/1000
Axial blanket length	9 to 15 in.
Radial blanket residence time	2 to 7 yr
Steam-generator module	79 to 382 tubes

Design Options

- Fuel-pin spacer (grid *vs* wire wrap)
- Superheater material (ferretic *vs* austenitic)
- Turbine type (cross compound *vs* tandem compound)
- IHX units per loop (one *vs* two)
- Number of heat-transfer circuits (two *vs* three)

analytical model to describe and evaluate the economics of each of the proposed changes. The overall economics were measured by the incremental cost variation in those plant elements affected by the changes ("partial power costs" in mills/kWh). In general, the principal items which varied as a part of the trade study were capital cost, fuel-cycle cost,

FIG. III-1. Overall Analytical Model.

and plant availability. The independent variables were then evaluated over the range of interest and, in general, the variation was by perturbation of the preliminary reference design. Each perturbation was studied separately using a variation of one independent parameter at a time while holding all others constant. The sum of the total economic changes was then taken as an approximation of the total economic change which would be incurred if all changes were made. This assumes noninteraction of all of the variables involved; however, in many cases the interaction was important enough that study was needed. An example is the case of fuel-pin pitch and system temperature rise (ΔT).

When the analytical model had considerable uncertainty (such as in the effects of maximum fuel burnup with operating temperature), the range of expected behavior was bracketed by defining endpoint limits. The selection of the design point was then made based on an estimate of behavior somewhere between the upper and lower limits with consideration for what would happen if either of the limiting curves were true. Also, certain sensitivity studies of key parameters (e.g., fabrication costs and equipment costs) were performed.

A block diagram of the entire analytical model is illustrated in Fig. III-1. The overall model consists of a number of individual models which size the reactor and plant for each of the design options and independent variables, and then evaluate the overall economics of the proposed changes. Many of the individual analytical models are programmed for computer solution on an IBM 360. The individual computer codes which comprise the overall model are indicated in Fig. III-1. The overall integration of the codes was calculated by hand. Each of the individual portions of the model is briefly described below.

1. Heat-transfer Model of Fuel

The overall heat transfer of an oxide fuel pin is predicted by means of the Fuel Thermal Behavior (FTB-4) computer code,⁽⁹⁾ which treats the fuel pin as three fuel regions plus a central void region. It computes the temperature distribution in the fuel pin using this model and calculates the linear power rating (kW/ft) to cause centerline melting of the fuel. The regions include the central void, the region of columnar grain growth, the region of equiaxed grain growth, and the outer fuel region. Variable fuel properties are accounted for in each fuel region, and the effects of sintering and variable density on the fuel properties are included. Heat transfer across the gas gap is included as a function of time and irradiation.

The dilution effect of fission gases on the heat transfer across the gap is considered. Gap closure is accounted for as irradiation proceeds, and the effect of fuel cracking on effective thermal conductivity is included in the model. The fuel pin is divided into ten axial nodes and, by means of a statistical process,⁽¹⁰⁾ any degree of desired reliability margin to prevent fuel-element melting may be included in the design of the fuel pin.

2. Core Thermal/Hydraulic Calculations

These calculations are performed by the means of the Fast Atomics International Thermal-Hydraulics (FAITH) computer code, which performs a heat balance on the hot element in the core. Inputs to this code include the basic size of the fuel element: height, pin diameter, and number of pins per element. In addition, allowable housing stresses, linear power rating, power fractions, radial and axial peak-to-average ratios, temperature rise across the core, and inlet temperatures are code inputs. The FAITH code computes the total number of elements needed to size the core and computes the lattice size required for either a given pressure drop or a given maximum allowable coolant velocity, either of which can be specified as an input option. Volume fractions for the fuel element are then computed, along with housing wall thickness, overall lattice sizes, and hot-spot temperatures of fuel elements. The FAITH computer code uses a chopped-cosine power distribution for a temperature calculation and uses modified, smooth tube friction factors to allow for a number of different configurations of fuel-element spacers. Hot-channel factors for each configuration are computed.

3. Reactor Physics Model

The reactor physics calculations are done by use of the CAESAR computer code using an 18-group diffusion-theory model. Both axial and radial calculations are made for the core, and buckling iterations between these cases are performed to search for criticality and correct for axial and radial leakage. Two zones of core enrichment are included in the radial direction in order to minimize the radial peaking factor. In the core, each region is computed by use of 25 space nodes; in the blanket and the reflector, 15 space nodes are employed. Calculations, for parametric purposes, are done at equilibrium midcycle conditions. The fissile loading determined provides for sufficient excess reactivity to achieve the end cycle condition. Homogeneously distributed tantalum in the outer core region is added to reduce the k_{eff} to 1.0. Output of the CAESAR calculation includes

the power distributions, fissile loadings for the various regions, reaction rates, and breeding ratio.

4. Calculations of Fuel-cycle Cost

Outputs from the CAESAR calculations are fed into the COST computer code. Fissile loadings, reaction rates, and burnup as a function of each region are input to the program. Economic data including fabrication, reprocessing and shipping costs, ex-core time, and interest on working capital and inventory are also supplied. With all the required physics and economics data, the code proceeds to calculate the major cost components for each region in the core and sums them to give a total fuel-cycle cost.

5. Model of Fuel Burnup

A basic assumption in the trade study is that the preliminary reference design can achieve an overall average burnup of 75 MWd/kgH at a 1060°F outlet temperature. The way in which burnup varies as the operating temperature of the system changes is of concern. There are few data which can be used to predict this, and not much is known about the ultimate failure mechanism of pins irradiated to a high fluence of fast neutrons. If the assumption is made that the ultimate life will be determined by loss of ductility in the austenitic cladding at high temperature levels, a variable-burnup model can be constructed to present the limited life as the temperature as increased. This variable burnup model is illustrated on Fig. III-2. The cladding-strain data were based on a recommended allowable strain versus temperature curve (see Fig. III-3). A linear approximation of allowable strain based on an average slope in the temperature range of interest was used in the code, that is, for a reactor outlet temperature of 1200°F, the slope of the allowable strain curve at 1300°F was used; similarly

FIG. III-3. Cladding-strain Allowables.

for a reactor outlet temperature of 800°F, the slope at 955°F was used, as the maximum allowable strain is reached at this point.

Relative to the fuel, it was assumed that no clad strain due to fuel swelling takes place out to a burnup of 40 MWd/kgH. Beyond 40 MWd/kgH the clad is strained at a rate of $2.5 \times 10^{-2}\%$ per MWd/kgH. Combining the cladding-strain data and the assumed fuel-swelling characteristics, relationships between cladding temperature and allowable fuel burnup are then obtained. This model, if true, indicates that the ultimate burnup of a fuel element drops off quite rapidly as the clad temperature increases. Other effects, including reduced clad strength, may also tend

FIG. III-2. Models of Fuel Burnup.

to drop burnup off even more rapidly than is indicated in Fig. III-2.

An extreme in the other direction is the assumption that ultimate burnup is independent of temperature. There is some substance to this belief in that ultimate clad life may be limited by radiation-swelling considerations which may be somewhat independent of temperature and, in fact, may even increase the life as the temperature is increased. Because of the uncertainty in exactly how burnup varies with clad temperature, both the constant- and variable-burnup models were studied, and a best estimate of how burnup life varies with temperature was considered. This best estimate, in addition to the upper and lower limits, was used to study the effects of temperature level. Only continued R&D testing will verify how burnup varies with temperature.

6. Overall Plant Heat Balance and Determination of Capital Cost

An overall heat balance for the plant is performed by means of the System Optimization Code (SOC). This code, with a given set of operating temperatures and system ΔT 's, computes the various heat-transfer surface areas required in the intermediate heat exchanger and steam generator (superheater, reheater, and evaporator). It also calculates the costs associated with each principal portion of the heat-transfer system, including the heat-exchanger areas, the primary-system pumps, and piping. A description of the cost of the steam plant, including the turbine, high-pressure steam piping, and feedwater systems, is included in the SOC code. The code can then perform a cost calculation for any set of operating conditions desired. The fuel-cycle costs from the COST code are curve-fit and fed into the SOC code so that the overall plant optimization may be performed, if desired, by means of this code.

7. Model of Plant Availability

Availability of the plant can change as the independent variables are varied over the range of interest. This availability change can be due to the increased number of fuel elements required to be handled or to changes in residence time of fuel elements in the core. The worth of availability is assumed to be approximately 2 mills/kWh.

The significant factors which are included in each of the previous models are summarized in Table III-5. Table III-6 gives a summary of the key economic and plant data which were used as input into the analytical models. The overall mills/kWh associated with capital cost, fuel-cycle cost, and availability are then computed.

C. Results

Typical results from the parametric analysis are illustrated in Figures III-4 through III-10. These figures include only the portion of energy costs which change as the plant is changed and are called out as "adjusted cost" or "partial power cost."

The first variable to be examined is reactor outlet temperature. The system optimization code (SOC) was the main tool used to study outlet temperature effects. Figure III-4a gives the partial power cost contribution of the portion of plant equipment that varies as a function of temperature and the fuel-cycle cost for the variable-fuel-burnup model. Each point on the curves represents an optimization for the given temperature level from the standpoint of using the best values of primary and secondary ΔT 's. Each curve may be considered as a series of "optimum" design plants. Steam temperatures of 900, 950, and 1000°F with reheater were considered. Figure III-4a shows that equipment costs decrease as the outlet temperature is increased. This improvement, in general, is due to the decreased heat-transfer surface areas required in external equipment, such as the IHX and steam generators, and to the lower plant heat rate. As the temperature rises, equipment becomes more expensive because of the greater material thicknesses required as allowable material strength drops off.

The fuel costs, however, substantially increase as the burnup decreases. At 1200°F, the model predicts a burnup level of about 45 MWd/kgH, compared to 75 MWd/kgH at 1060°F. The summation of the cost of the portion of the equipment varying as a function of temperature and the fuel-cycle cost gives a partial plant power cost value that can be used to compare various designs. The minimum partial power cost for the variable-burnup model is at about 1070°F outlet temperature with steam temperature at 950°F. Below an outlet temperature of about 1020°F, steam is best at 900°F; above about 1090°F, steam is best at 1000°F.

Figure III-4b shows the same information for a constant burnup of 75 MWd/kgH. The equipment costs are very similar but, because of the different optimum system ΔT 's which result from the different fuel-cycle costs, they are not exactly the same. Fuel-cycle costs decrease slightly with temperature. The partial power costs are seen to be very near the minimum at an outlet temperature of 1200°F. Figure III-4c gives the same information for a constant burnup of 100 MWd/kgH. Except for the absolute level of cost it has the same characteristics as Figure III-4b.

Figure III-4d shows the comparison of results for the different burnup models. For the constant-burnup

model, an optimum reactor outlet temperature of the order of 1200°F is indicated, and optimum steam conditions for this temperature are 2400 psi/1000°F/1000°F. Above this optimum temperature the total costs increase due to (1) higher prices of components and (2) higher fuel-cycle costs which result from the necessity to increase the wall thickness of the fuel-element housing at higher temperatures.

For the variable-burnup model, the optimum is at a lower temperature (~1060°F). The "best estimate" burnup model indicates an optimum of the order of 1140°F, and this temperature has been chosen for the reference design.

It is seen that if the constant-burnup model actually describes the fuel performance, the chosen temperature results in a significant improvement in economics over the 1060°F outlet. If the variable-burnup model describes the performance, the chosen temperature results only in a small penalty. Therefore, this "best estimate" burnup represents a minimum-risk point. The optimum steam conditions for a temperature of 1140°F are 2400 psi/1000°F/1000°F, which have been selected as the steam conditions for the reference design plant.

At this higher operating temperature of 1140°F, the "best estimate" burnup model predicts a burnup of the order of 67 MWd/kgH, the reduction being due to a loss in ductility of the clad at higher temperature. The value of 67 MWd/kgH was chosen as the burnup for the reference design, and the specific power of the core has been adjusted by means of proper choice of pin linear power, core height, smear density, and pin size in order to achieve a one-year refueling period using a 50% core-reloading scheme.

Figure III-5 illustrates the effect of pin pitch on economics for the grid and wire-wrap types of spacers considered in our study, and includes all costs that vary as a function of the fuel-element pin pitch. These

TABLE III-5. SUMMARY OF MODEL FEATURES

Heat-transfer Model for Fuel (FTB-4 Code)	Reactor Physics Model (CAESAR Code)
Four regions	18 groups
Ten axial nodes	Two enrichment zones
Sintering effects	Two-region radial blankets
Fuel cracking	Axial and radial calculations with buckling iteration
Gas gap closure <i>vs</i> time	Equilibrium midcycle calculations
Variable fuel density	15 control rods
Variable thermal conductivity	Plutonium ratios (39/40/41/42):67/26/5/2
Core Thermal Hydraulics (FAITH Code)	Nuclear library W 50064
Chopped-cosine power distribution	Overall Plant Economics (SOC Code)
Computation of hot-channel factor	Plant heat balance
Pressure-drop calculation	Heat-transfer surface
Velocity determination	Heat-exchanger costs (IHx and SG)
Pin-pitch determination	Primary/secondary loop costs
	Reactor-size costs
	Turbine/steam/feedwater costs

costs include fuel-cycle costs, pumping power, and capital costs of the reactor structure. At constant power, constant pin diameter, and constant reactor ΔT , a decrease in pin pitch increases coolant velocity and reactor pressure drop. This decrease in pin pitch also results in "drier" cores (increased fuel volume fraction) and therefore tends to lower fuel-cycle costs. Figures III-5 and III-6 illustrate that the optimum pressure drop is ~120 psi for a wide variety of design conditions, and a pressure drop of 120 psi therefore appears a good choice for the ranges of pin size and reactor ΔT being considered. The wire-wrap spacer is seen to give a significant advantage because of the drier core that is achieved at the minimum cost point, due to the lower pressure-drop characteristic

TABLE III-6. KEY ECONOMIC DATA

Fuel-cycle Data		Capital Cost Data	
Fuel-fabrication cost (\$/kg) (50-in. core, 0.250-in. pin) varies inversely with diameter, height, throughput 1/(burnup)	310	Capacity factor	0.80
Blanket-fabrication cost (\$/kg)		Annual capital charge	0.13 ^(a)
Radial blanket	60	Differential capacity charge	105
Axial blanket	50	Indirect factor	0.4
Reprocessing cost (\$30,000 and criticality limit 67% of U-235 limit)		Present worth interest rate	7
Shipping cost (\$/kg)	20	Heat-exchanger cost (\$/ft ²)	
Working capital charge rate (%/yr)	10 ^(a)	IHX	64
Ex-core time (days)	270	Evaporator	77
Fissile value (\$/gm)	10	Superheater	77
Fabrication and reprocessing loss (%)	1	Reheater	77
		Plant Unavailability	
		Charge rate (mills/kWh)	2

^(a) Economic analysis of Section I based on 12% and 14% respectively for working capital and depreciable capital.

FIG. III-4. Variation of Costs with Reactor Outlet Temperature.

for a given velocity. The wire-wrap spacer also results in an estimated fuel-fabrication cost saving of 0.03 to 0.05 mill/kWh. A wire-wrap design has therefore been chosen for the reference design.

Figure III-5 also illustrates the effects of pin size on overall economics. It is seen that the increase in pin size results in a significant economic improvement, partly from the fact that, at larger pin sizes, the core becomes drier for a given pressure drop and partly due to the lower fuel-fabrication costs resulting from the use of larger pins. This analysis, however, was based on a constant radial-blanket burnup. Figure III-7 compares the economics of pin size with constant

radial-blanket burnup and constant radial-blanket residence time. It is seen from this figure that longer blanket residence times (constant radial-blanket burnup) are required to achieve the economic advantage indicated for larger pins. The residence time for the outer radial-blanket elements to achieve the estimated optimum exposure in a core using 0.300-in.-OD fuel pins is about 7 yr. Associated with these long residence times are many potential difficulties such as fretting, fatigue, corrosion, and irradiation induced distortion. These difficulties have been evaluated only superficially, and the residence time of the blanket elements may be limited by technical factors

FIG. III-5. Pin Pitch vs Cost for $\frac{1}{4}$ - and $\frac{3}{10}$ -in. Pins.

FIG. III-6. Pin Pitch vs Costs.

FIG. III-7. Effect of Radial Blanket on Economics of Pin Diameter.

FIG. III-8. Partial Power Costs vs Reactor ΔT for $\frac{3}{10}$ -in. Pin.

dition, time-dependent economic factors, which can be properly accounted for only by detailed fuel management and cash-flow analyses, become quite important as fuel inventory and residence time increase. It is clear that the economic and technical factors implicit in the selection of a 0.300-in.-OD pin for the reference design need more extensive investigation which was beyond the scope of our study.

Figure III-8 illustrates how fuel-cycle costs, capital costs, and partial energy costs vary with reactor ΔT for the previously selected reactor parameters. Increasing reactor ΔT dries up the core, thus improving the reactor neutronics (higher breeding ratio and lower fissile mass) and thereby reducing fuel-cycle

rather than economic factors. Although the blanket residence time requires further evaluation to determine the upper limit, we have selected approximately 7 yr as the maximum time for this evaluation. In ad-

FIG. III-9. Cost Variation with Core Height.

costs in the constant-burnup model. In the case of the variable-burnup model, higher ΔT results in higher hot-spot temperatures of the cladding, and therefore in lower burnup and consequent higher fuel-cycle costs. The cost of the greater heat-transfer areas required at higher ΔT causes equipment costs to increase. The optimum ΔT for the constant-burnup model is approximately 375°F. For the variable-burnup model, the optimum appears to be about 350°F. A compromise of 360°F has been chosen for the reference design as a best estimate with little penalty if the model is wrong in either direction.

Figure III-9 illustrates the effect of core height on plant economics. As the core height is decreased, the costs of fuel-element fabrication are increased, and the number of fuel elements required (for constant element size) is increased, leading to increased fuel-handling time. The increased fuel-handling time produces an unavailability penalty as core height is decreased. The unavailability penalty was included only if the total reactor fuel-handling time per year exceeded that required for an annual turbine-maintenance schedule (assumed to be two weeks per year). Due to the fact that the flow per element becomes less and the volume fraction of sodium is reduced in order to hold the optimum pressure drop, a decrease in fuel-cycle cost is obtained as core height is decreased. Shorter cores also reduce capital costs for the selected system configuration. These competing effects produce an optimum in core height, dependent upon which burnup model is assumed. For the constant-burnup model, the optimum core height is ~42 in. If the variable-burnup model is used, the optimum is ~38 in. This difference is due to the fact that the

axial peak-to-average power ratio becomes less as the core height is decreased and, for the variable-burnup model, results in a higher fuel burnup. Because of the flatness of this curve, the core height can be adjusted over a range of approximately 38 to 43 in. without significantly influencing the economics. Therefore, this parameter, in addition to the linear power rating of the fuel, is used to adjust the core design to achieve a specific power consistent with the one-year refueling period and the required integral number of fuel elements to give a good hexagonal pattern layout of the core. This results in a tentative selection of a 43-in. core height for the final reference design.

Table III-7 summarizes a number of the incremental power costs which were studied as part of the parametric analysis, and indicates a breakdown into fuel-cycle cost, capital cost, and availability worth so as to illustrate the relative importance of the various effects which were considered. Lines 1, 2, and 3 show the effect of pin size with other core parameters maintained at their reference values. Note the substantial economic penalty associated with small pin sizes, primarily because of lower breeding ratios and higher costs of fuel fabrication. Lines 4, 5, and 6 show the influence of linear power rating with all other parameters maintained constant. Here the energy costs (somewhat surprisingly) increase with higher linear power rating. This increase is primarily due to the larger flow area, hence coolant volume fraction, needed to put the flow through the core as the linear power rating is increased at constant coolant velocity. This increased volume fraction of sodium causes a decrease in breeding ratio that more than offsets the decreased inventory associated with higher linear power ratings. Whether or not advantage can be taken of the potential for going to higher linear power ratings, without incurring a significant reduction in breeding ratio, through changes in other core parameters, is of interest at this point. Specifically, the pin size, reactor ΔT , and core height were varied—one parameter at a time—and change in energy costs was noted. The results of this study are shown as lines 6, 7, 8, and 9. Note that none of these 20-kWt/ft cases actually produce an improvement compared to its counterpart at 15.3 kWt/ft. It is recognized that this (perhaps startling) conclusion may be partly due to the specific assumptions and method of analysis; further study is required of linear power ratings higher than ~15 kWt/ft for oxide fuel. Lines 10, 11, and 12 show the effect of smeared density on energy (mostly fuel-cycle) costs. This analysis is based on constant burnup, which probably favors the cases with higher smeared density. Most of the indicated fuel-cycle improvement with higher smeared densities is due to

TABLE III-7. SUMMARY OF PARAMETER STUDY

Line	Fuel-pin Diameter (in.)	Reactor T (°F)	Fuel-pin Power (kW/ft)	Core Fuel Density (% T.D.)	Average Coolant Velocity (ft/sec)	Core Height (in.)	Cost Change (mills/kWh; () Indicates Decrease)			
							Capital Cost	Availability Cost	Fuel-cycle Cost ^(a)	Total Change
1) Fuel-pin diameter	0.20	300	15.3	80	27	50	(0.010)	0.024	0.237	0.251
2)	0.25	300	15.3	80	27	50	Base	Base	Base	Base
3)	0.30	300	15.3	80	27	50	0.011	(0.030)	(0.051)	(0.070)
4) Linear power of fuel pin	0.25	300	12	80	27	50	0.003	(0.016)	(0.035)	(0.048)
5)	0.25	300	15.3	80	27	50	Base	Base	Base	Base
6)	0.25	300	20	80	27	50	0.001	0.012	0.117	0.130
7)	0.30	300	20	80	27	50	0.011	(0.012)	(0.026)	(0.027)
8)	0.25	300	20	80	27	36	(0.022)	0.044	0.098	0.120
9)	0.25	360	20	80	27	50	0.036	0.008	0.053	0.097
10) Smeared density of fuel pin	0.25	300	16.6	72	27	50	(0.002)	0.004	0.064	0.066
11)	0.25	300	15.3	80	27	50	Base	Base	Base	Base
12)	0.25	300	14.3	88	27	50	—	(0.004)	(0.047)	(0.051)
13) Coolant velocity	0.25	300	15.3	80	27	50	Base	Base	Base	Base
14)	0.25	300	15.3	80	32	50	0.015	—	(0.004)	0.011

(a) Variable-burnup model.

the lower fabrication cost for the more densely packed pins. Here 80% of the fabrication cost was estimated as inversely proportional to the smeared density.

The last two lines in Table III-7 show the effect of velocity on partial energy costs. It is noted that the increased capital (pumping power) costs more than offset the reduced fuel-cycle costs at the higher velocity. It appears that there is little economic incentive, if any, to push for coolant velocities higher than 30 ft/sec with oxide-fuel core designs.

Some sensitivity studies have been performed as part of the overall parametric evaluation in order to indicate how variations in some of the key economic assumptions affect the conclusions. Figure III-10 illustrates the sensitivity of fuel-cycle costs for three different pin sizes as a function of plutonium value. Note that, despite the wide variations in plutonium value, the 0.300-in. pin still appears as the preferred choice. The smaller pin is more sensitive to plutonium value because of its much lower breeding ratio, which more than offsets the reduction in fissile mass. For pin sizes in the range from 0.250 to 0.300 in. a change of \$5/gm in plutonium value results in a change of approximately 0.1 mill/kWh in fuel-cycle cost.

Figure III-11 illustrates the sensitivity of fuel-cycle costs for the three pin sizes as a function of the relative level of fabrication and reprocessing costs. The assumed base values for fabrication, reprocessing, and

FIG. III-10. Differential Fuel-cycle Cost vs Fissile Pu Value with Pin Diameter as a Parameter.

fuel-cycle costs are noted. This figure clearly indicates that fabrication-cost uncertainties have a greater (by a factor of three) influence on fuel-cycle costs

FIG. III-11. Differential Fuel-cycle Cost vs Percent Change in Fabrication and Reprocessing Cost with Pin Diameter as a Parameter.

FIG. III-12. Differential Fuel-cycle Cost vs Burnup with Fabrication and Reprocessing Costs as Parameters.

than uncertainties in reprocessing cost. Note also that a reduction of 30% in both fabrication and reprocessing costs—which will probably result from improved technology and increased production capacity—reduces the base-fuel cycle costs by about 0.2 mill/kWh.

Figure III-12 illustrates the influence of uncertain-

ties in fabrication and reprocessing cost on fuel-cycle cost as a function of the assumed burnup level, and shows the diminishing return on fuel-cycle cost with increased burnup, particularly as fabrication and reprocessing costs are lowered. This figure also highlights the importance of fabrication-cost and burnup-level assumptions in LMFBR fuel-cycle cost studies; for example, with a 30% reduction in fabrication costs and an increase in burnup from 75 to 100 mWd/kgH, a reduction in fuel-cycle cost of 0.25 mill/kWh is achieved.

Table I-4 summarizes the design changes proposed, progressing from the preliminary reference design to the final reference design, and summarizes the incremental economic improvements which have been calculated, to give a first-order estimate of the total economic improvement of the final reference design. It is seen that the recommended improvements are adequate to meet the economic goals which were established.

D. Conclusions

As a result of the Task III parametric evaluations, a final reference design has been proposed that

incorporates a number of improvements over the preliminary design. These design changes were summarized by Table I-5. The R&D areas which are required to make these improvements, and which must be completed by the 1975 period to allow such an improved 1000-MWe plant to start up in the early 1980's, are being defined. Some preliminary analysis has been performed on the resulting design to ascertain whether or not the design meets the various requirements and constraints previously mentioned. This preliminary check appears favorable.

IV. DESCRIPTION OF FINAL REFERENCE CONCEPT

The main features of the Task III 1000-MWe LMFBR Follow-on Study final reference design have been mentioned earlier in this paper. This new reference design has just recently been selected; because the analysis and design work in many areas has not been completed, many of the detailed design and performance characteristics are not available. Table IV-1 summarizes the characteristics of the new reference design as well as the characteristics of the preliminary reference (Task I) design for comparison.

The plant uses a loop-type configuration for the primary system. The basic flow diagram is shown in Fig. IV-1. Heat is transferred from the radioactive primary-sodium loop to the nonradioactive secondary-sodium loop for the production of steam. The primary-coolant system is composed of three identical heat-transfer loops operating in parallel. The reactor vessel provides a common inlet and discharge for the primary loops. The energy is transferred from the primary to the secondary system in the intermediate heat exchangers (IHX). The secondary system consists of three independent loops, one in series with each primary loop. The secondary sodium is pumped through the tube side of the intermediate heat exchanger and shell side of the steam generator. Steam is generated in single-wall shell-and-tube heat exchangers of the modular type. The turbine-generator complex is similar to that used in fossil fuel plants.

A. Plant Layout and Buildings

The overall plant arrangement is shown in Fig. IV-2. Although the figure shows a cross-compound turbine layout, a tandem-compound machine has been selected for the new reference design. The reactor building, shown in Figures IV-3 and -4, is of conventional reinforced concrete with a steel liner to control leakage. The building houses the reactor, fuel-handling equipment, and all portions of the primary-sodium system, including the intermediate heat exchangers. Containment to protect against the accidental release of

radioactivity is provided by the building liner and the cells which enclose the reactor and primary system.

As shown in Figure IV-2, the steam generators are located in a separate conventional steel building. This building also contains the steam-generator, sodium-side pressure-relief equipment, and secondary-sodium pump and surge tank. The turbine-generator building houses the prime mover and its associated equipment. The following sections describe briefly the major portions of the plant.

B. Reactor

The reactor consists of a central cylindrical array of 274 fuel elements and 15 control rods (i.e., the core) surrounded by two rows of blanket elements and a row of stainless steel reflector elements. The core fuel material is $\text{PuO}_2\text{-UO}_2$, and the blanket material is depleted UO_2 . Tantalum control rods are used.

The reactor is shown in elevation in Fig. IV-5. The reactor vessel houses the reactor core and blankets, provides containment for the sodium coolant, and provides transfer positions for new and spent fuel. Figure IV-5 indicates in-vessel spent-fuel storage. Immediate removal of fuel has been adopted for the new reference design; therefore, most of the in-vessel positions can be eliminated and the vessel can be reduced in diameter. The vessel is made of Type 304 stainless steel and is approximately 1 in. thick. The vessel has three inlet and three outlet main-coolant-loop pipe penetrations, and one small inlet penetration for coolant supply to the low-pressure region below the reactor inlet-plenum. Sodium enters the vessel at the same elevation as the outlet, flows downward to the high-pressure inlet-plenum, and flows upward past the fuel and blanket elements. The elements are held down against the upward flow of coolant by a hydraulic holddown arrangement at the inlet. Flow from the low-pressure region cools the control rods by natural convection. Three thermocouples are provided at the outlet of each fuel and blanket element.

Vessel internal structures consist of (1) the lower reactor support assembly, which also serves as the high-pressure inlet plenum; (2) radial neutron-shield and reactor restraint assemblies; and (3) fuel-handling transfer positions. The lower reactor support assembly is made of two plates perforated for the fuel and blanket element nozzles and control rods. Through-tubes are provided at the control-rod locations to allow flow from the low-pressure region for control-rod cooling. The core-subassembly nozzles seat on conical surfaces in the lower gridplate. All reactor vessel internals are designed to permit their removal through the opening resulting from re-

TABLE IV-1. 1000-MWe LMFBR FOLLOW-ON DESIGN DATA

	Preliminary Reference	Design Reference Design
Overall Plant		
Nominal		
Reactor thermal power (MWt)	2500	2400
Gross electrical power (MWe)	1052	1040
Net electrical power (MWe)	1004	1002
Plant efficiency (%)	40.7	41.7
Net heat rate (Btu/kWh)	8375	8175
Reactor outlet temperature (°F)	1060	1140
Primary loop ΔT (°F)	300	360
Sodium inlet temperature to steam generator (°F)	950	1070
Secondary loop ΔT (°F)	260	400
Steam Conditions		
Superheat/reheat temperature (°F)	900/900	1000/1000
Pressure (psig)	2400	2400
Condenser pressure (in. Hg abs)	1.5	1.5
Refueling Cycle (yr)	1/2	1
Reactor Containment	Double barrier	Double barrier
Fuel Material	Mixed PuO ₂ -UO ₂	Mixed PuO ₂ -UO ₂
Reactor Coolant	Sodium	Sodium
Reactor Geometry	Regular	Regular
Geometry of Heat-transfer System	Loop	Loop
Reactor		
Reactor Geometry		
Core height (in.)	50	43
Core L/D	0.54	
Axial blanket length (in.)	12 (upper)	12 (upper)
	12 (lower)	15 (lower)
Rows of radial blankets	2	2
Radial blanket height, inner/outer (in.)	62/50	
Rows of reflector elements	1	1
Reactor subassemblies (total)	469	505
Fuel elements	256	274
Control rods	15	15
Shim-safety	9	9
Safety	6	6
Radial blankets	126	138
Inner	60	66
Outer	66	72
Reflectors	72	78
Fission gas plenum height (in.)	64	
Inlet plenum height (in.)	36	36
Vessel OD (ft-in.)	22-9	
Vessel height (ft) (bottom head to floor)	49	
Vessel jacket ID (ft-in.)	24-11	
Reactor cavity ID (ft-in.)	26-4	
In-vessel fuel-storage locations	91	0
Top shield type	Double rotatable plug	Double rotatable plug
Large-plug diameter (ft)	25	25
Small-plug diameter (ft-in.)	13-6	13-6
Overall thickness (ft)	10.25	11.25
Steel plates (in.)	15	
Serpentine (in.)	12	
Steel top plate (in.)	15	
Seal type	Double lip, He purge	Double lip, He purge
Containment dome ID (ft-in.)	28-4	36-0
Gas atmospheres		
Reactor cover gas	He	He
Containment dome	N ₂	N ₂
Reactor cavity	N ₂	N ₂
Thermal-Hydraulics		
Thermal power (MWt)	2500	2400

TABLE IV-1--Continued

	Preliminary Reference Design			Reference Design		
Reactor (<i>Continued</i>)						
Reactor power fractions (mid-equilibrium)						
Core	0.899					
Axial blankets	0.032					
Radial blankets	0.069					
Sodium outlet temperature (°F)	1060			1140		
Reactor ΔT (°F)	300 Average			360 Average		
Core ΔT (°F)	314 Average			372 Average		
Flow control	Fixed orifices			Fixed orifices		
Peak fuel-pin power (kWt/ft)(100% power)	15			16		
Maximum nominal clad ID temperature (°F)	1130					
Hot-spot clad ID temperature (°F)	1200					
Maximum heat flux (Btu/hr-ft ²)	785,000					
Average core heat flux (Btu/hr-ft ²)	495,000					
Cladding hot-channel factors	1.16					
Radial peaking factor	1.26					
Axial peaking factor	1.25					
Reactor ΔP (psi)	100					
Coolant velocity						
Maximum (at spacer) (ft/sec)	32.8					
Average (maximum element) (ft/sec)	27					
Reactor flowrate (lb/hr)	93×10^6			75.6×10^6		
Nuclear (mid-equilibrium) Characteristics						
Volume fractions	<i>Fuel</i>	<i>Sodium</i>	<i>Metal</i>	<i>Fuel</i>	<i>Sodium</i>	<i>Metal</i>
Core and axial blanket	0.310	0.501	0.189			
Radial blanket, inner	0.550	0.298	0.152			
Radial blanket, outer	0.577	0.292	0.131			
Core enrichments (at % Pu)						
Inner zone	11.6					
Outer zone	16.0					
Fissile masses (kg Pu)	2187					
Core	1758					
Axial blanket	98					
Radial blanket	331					
Initial loading (kg Pu + U)						
Core	13,339					
Axial blanket	7,463					
Radial blanket	15,365					
Breeding ratio	1.32					
Core	0.83					
Axial blanket	0.28					
Radial blanket	0.21					
Average fuel burnup (MWd/te)	75,000			67,000		
Doubling time (yr)	10					
Reactivity Coefficients						
Doppler constant ($T dk/dT$)	0.0096					
Maximum radial sodium void (%)	6.82					
Total sodium void (%)	6.03					
Isothermal sodium temperature ($\Delta k/k$)	3.3					
Full-flow Power Coefficient ($10^5 \Delta k/k$ % P)	-4.4					
Prompt (Doppler) portion	-5.1					
Delayed (sodium) portion	+0.7					
Fuel and blanket elements						
Geometry	Hexagonal			Hexagonal		
Lattice spacing (in.)	5.389					
Distance across flats (external) (in.)	5.339					
Housing wall (in.)						
Fuel element	0.130					
Inner radial blanket	0.103					

TABLE IV-1—Continued

	Preliminary Reference Design	Reference Design
Reactor (<i>Continued</i>)		
Outer radial blanket	0.091	
Pins per subassembly/OD		
Fuel and axial blanket	217/0.25	217/0.30
Inner radial blanket	91/0.47	91/
Outer radial blanket	61/0.58	61/
Fuel and cladding	Mixed PuO ₂ , UO ₂ -type	Mixed PuO ₂ , UO ₂ -type
	304 or 316 stainless steel	304 or 316 stainless steel
Blanket and cladding	Depleted UO ₂ -type	Depleted UO ₂ -type
	304 or 316 stainless steel	304 or 316 stainless steel
Smeared fuel density (% theoretical)		
Fuel and axial blanket	80	85
Radial blanket	93	93
Pin spacers		
Fuel elements	Grid	Wire-wrap
Radial blankets	Wire-wrap	Wire-wrap
Fuel-element length (in. overall)	214	
Fuel (in.)	50	43
Axial blanket (each) (in.)	12	12 upper/15 lower
Gas plenum (in.)	64	
Blanket-element length (in. overall)	214	
Inner blanket	62	56.5
Outer blanket	50	43
Gas plenum	10	10
Control and safety rods		
Absorber material	Tantalum pins	Tantalum pins
Cladding	Type 304 stainless steel	Type 304 stainless steel
Thermal bond	NaK	NaK
Pins in array		
Shim-safety	16	16
Safety	19	19
Average rod worth (\$)		
Shim-safety	1.6	
Safety	2.0	
Type of cooling	Thermal convection	Thermal convection
Type of drive	Electromechanical	Electromechanical
Heat Transfer		
Number of loops	3 primary and 3 secondary	3 primary and 3 secondary
Reactor thermal power (MWt)	2500	2400
Total Reactor Flow (lb/hr)	93×10^6	75.6×10^6
Reactor Inlet Temperature (°F)	760	780
Reactor Outlet Temperature (°F)	1060	1140
Pressure at Top of Reactor Vessel (psig)	1	1
Primary-system Sodium Volume ^(a) (ft ³)		45,300
Primary-sodium Volume/Loop (ft ³)		7,200
Secondary-sodium Volume/Loop (ft ³)		18,200
Primary Sodium Pumps ^(b)		
Type	3 (one/loop)	3 (one/loop)
	Free-surface centrifugal	Free-surface centrifugal
Flow (gpm)	77,000	62,500
Head (ft)	355	
Brake horsepower (Bhp)	6660	
NPSH available (ft)	30	
Intermediate Heat Exchangers		
Type	3 (one/loop)	3 (one/loop)
	Shell-and-tube unit	Shell-and-tube unit
Duty/IHX (Btu/hr)	$2,850 \times 10^6$	$2,730 \times 10^6$
Surface area/IHX (ft ²)	32,200	29,900
Secondary-sodium Pumps ^(b)		
Type	3 (one/loop)	3 (one/loop)
	Free-surface centrifugal	Free-surface centrifugal
Flow (gpm)	87,000	55,200
Head (ft)	250	
Brake horsepower (Bhp)	5,400	

TABLE IV-1—Continued

	Preliminary Reference Design		Reference Design	
Heat transfer (Continued)				
Steam Generator Banks				
Evaporator duty/bank (Btu/hr)	3 (one/loop) 1,830 × 10 ⁶		3 (one/loop) 1,610 × 10 ⁶	
Evaporator surface area/bank (ft ²)	29,700		21,800	
Superheater duty/bank (Btu/hr)	510 × 10 ⁶		670 × 10 ⁶	
Superheater surface area/bank (ft ²)	12,800		11,200	
Reheater duty/bank (Btu/hr)	476 × 10 ⁶		450 × 10 ⁶	
Reheater surface area/bank (ft ²)	15,000		12,200	
Sodium inlet temperature (°F)	950		1070	
Sodium outlet temperature (°F)	690		670	
Steam Generator				
Superheater module				
Type construction	Shell-and-tube unit		Shell-and-tube unit	
Total number in plant	3 banks		3 banks	
Number of modules per bank	20		4	
Duty per module (Btu/hr)	25.5 × 10 ⁶		167 × 10 ⁶	
Surface area per module (ft ²)	640		2,800	
Tube data per module	79 tubes, 5⁄8-in. OD by 0.109-in. wall, 50-ft 6-in. effective length		380 tubes, 5⁄8-in. OD by 0.095-in. wall, 44-ft 11-in. effective length	
	Tube Side	Shell Side	Tube Side	Shell Side
Fluid	Steam	Secondary Na	Steam	Secondary Na
Flow per bank (lb/hr)	2,500,000	18,400,000	2,280,000	13,450,000
Temperature in (°F)	715	950	715	1070
Temperature out (°F)	905	860	1005	905
Outlet steam pressure (psia)	2515	—	2490	—
Design pressure (psi)	2700	200	2670	200
Design temperature (°F)	965	965	1085	1085
Material	2¼ Cr-1 Mo	2¼ Cr-1 Mo	321H	321H
Evaporator module				
Type construction	Shell-and-tube unit		Shell-and-tube unit	
Total number in plant	3 banks		3 banks	
Number of modules per bank	39		6	
Duty per module (Btu/hr)	46.8 × 10 ⁶		268 × 10 ⁶	
Surface area per module (ft ²)	762		3640	
Tube data per module	91 tubes, 5⁄8-in. OD by 0.095-in. wall, 51-ft effective length		380 tubes, 5⁄8-in. OD by 0.095-in. wall, 58-ft 7-in. effective length	
	Tube Side	Shell Side	Tube Side	Shell Side
Fluid	Steam/water	Secondary Na	Steam/water	Secondary Na
Flow per bank (lb/hr)	2,500,000	35,500,000	2,280,000	22,500,000
Temperature in (°F)	478	860	478	905
Temperature out (°F)	715	690	715	670
Inlet feedwater pressure (psia)	2600	—	2780	—
Design pressure (psi)	2800	200	2968	200
Design temperature (°F)	875	875	920	920
Material	2¼ Cr-1 Mo	2¼ Cr-1 Mo	2¼ Cr-1 Mo	2¼ Cr-1 Mo
Reheater module				
Type construction	Shell-and-tube unit		Shell-and-tube unit	
Total number in plant	3 banks		3 banks	
Number of modules per bank	7		3	
Duty per module (Btu/hr)	68 × 10 ⁶		149 × 10 ⁶	
Surface area per module (ft ²)	2140		4060	
Tube data per module	161 tubes, 1-in. OD by 0.095-in. wall, 50-ft effective length		380 tubes, 1-in. OD by 0.058-in. wall, 41-ft effective length	
	Tube Side	Shell Side	Tube Side	Shell Side
Fluid	Steam	Secondary Na	Steam	Secondary Na
Flow per bank (lb/hr)	2,340,000	17,100,000	2,130,000	9,040,000
Temperature in (°F)	553	950	630	1070
Temperature out (°F)	905	860	1005	905

TABLE IV-1—Continued

Heat transfer (Continued)	Preliminary Reference Design		Reference Design	
	Tube Side	Shell Side	Tube Side	Shell Side
Outlet pressure (steam) (psia)	505	—	515	—
Design pressure (psi)	600	200	600	200
Design temperature (°F)	965	965	1070	1070
Material	2¼ Cr-1 Mo	2¼ Cr-1 Mo	321H	321H
Turbine Generator	Cross-compound		Tandem-compound	
Type of Machine	3600/1800		3600	
Speed (rpm)	7.54 × 10 ⁶		6.85 × 10 ⁶	
Main Steam Flow (lb/hr)	2,400		2,400	
Main Steam Pressure (psig)	900		1000	
Main Steam Temperature (°F)TT	900		1000	
Reheat Steam Temperature (°F)TT	900		1000	
Reheat Steam Pressure (psig)	515		515	
Reheat Steam Flow (lb/hr)	7.0 × 10 ⁶		6.4 × 10 ⁶	
Condenser Pressure (in. Hg abs)	1½		1½	
Number of Feedwater Heaters	7		7	
Final Feedwater Temperature (°F)	478		478	
Gross Electrical Output (kW)	1,062,000		1,040,000	
Net Electrical Output (kW)	1,004,000		1,002,000	
Net Plant Heat Rate (Btu/kWh)	8375		8175	
Net Plant Efficiency (%)	40.7		41.7	
Support Equipment Segment				
Refueling Cycle (mo)	6		12	
Elements Handled per Refueling (avg)				
Fuel	86		137	
Inner blanket (also rotate)	10		17	
Outer blanket (also rotate)	7		11	
Control rod	0.5		1	
Sodium Temperature (°F)	500		500	
Site Structures and Services				
Reactor Building				
ID (ft)	176		140	
Inside height (cylinder) (ft)	105		137	
Radius of hemispherical dome (ft)	88		70	
Overall height (ft)	193		191	
Type of containment, leakrates	Double barrier		Double barrier	
Inner barrier at 20 psi (%/day)	10		10	
Outer barrier at 10 psi (%/day)	0.5		0.5	
Atmospheres				
High bay	Air		Air	
Heat-transfer vaults	N ₂		N ₂	
Seismic design ground accelerations (g)				
Design earthquake				
Horizontal	0.33		0.33	
Vertical	0.22		0.22	
Maximum hypothetical earthquake				
Horizontal	0.5		0.5	
Vertical	0.33		0.33	

(a) Includes three primary loops.

(b) For 100% power condition.

moval of the small shield plug should the need ever arise.

Helium is used for the reactor cover gas. Cover-gas pressure is maintained slightly above that of the operating area to prevent in-leakage. A cover-gas cleanup system is provided to permit reactor operation

with a small fraction (~1%) of failures of fuel-pin cladding. Provision is made for the later expansion of the cover-gas cleanup system to permit operation with vented fuel, if and when such modifications are justified.

A double-rotatable shield plug forms the upper

FIG. IV-1. Basic Flow Diagram.

closure of the reactor vessel and is an integral part of the fuel-handling system. It provides radiation shielding for the operating area, a seal for the reactor cover gas, a heat-flow barrier, mechanical support for the control-rod drives, and the support and positioning mechanism for the in-core fuel-handling machine. The thickness of the rotatable shield is approximately 11 ft. The lower 15 in. consist of horizontal stainless steel plates that act as neutron and thermal shields. Above this stack of plates is a cover-gas space, a serpentine concrete layer, and a 15-in. structural steel plate. All penetrations are stepped to prevent radiation streaming. A portion of the lower plates are submerged in the sodium of the upper pool to reduce the interface between cover gas and sodium, and thus to inhibit cover-gas entrainment.

The top shield consists of two rotatable plugs. The smaller rotatable plug is eccentric with respect to the larger. Rotation of the plugs permits positioning of the fuel-handling equipment over any location. The reactor is refueled on a 1-yr cycle, with $\frac{1}{2}$ of the core at 67 MWd/kgH or $\frac{1}{3}$ of the core at 100 MWd/kgH, $\frac{1}{4}$ of the inner blanket, and $\frac{1}{7}$ of the outer blanket being replaced at each refueling. Two refuel-

ing machines are used, one for in-vessel fuel handling and one for ex-vessel handling. In-vessel fuel handling is all done under sodium. The fuel elements are placed in sodium-filled canisters while still in the reactor vessel. The ex-core fuel-handling machine is inert-gas cooled.

The fuel-handling procedure is shown in Fig. IV-6. Spent fuel is removed from the core and placed in a sodium-filled canister in the in-vessel transfer position by the in-vessel machine. The spent fuel is then removed from the transfer position and placed in the spent-fuel storage vessel by the ex-vessel handling machine. The fuel-handling sequence is so planned that the blanket and lower-powered elements are handled first, thus allowing the high-power elements to decay to a power level which can be handled without difficulty.

Secondary containment of primary sodium in the event of a leak is provided by the reactor vessel jacket. The vessel jacket provides a void volume which is less than that of the volume of sodium in the reactor vessel 2 ft above the downcomers of the outlet nozzles. This precludes uncovering the core should there be a sodium leak in the reactor vessel, and

assures continued cooling through the main coolant loops. The vessel jacket also acts to protect against missile penetration of the cavity liner if the reactor vessel ruptures as a result of the DBA. The containment dome above the shield and the reactor cavity liner provide part of the primary-containment barrier for radioactivity released from the reactor vessel during and following the DBA.

The fuel element consists of a bundle of 217 fuel pins of 0.300-in. OD, inside a hexagonal stainless steel housing. The fuel pins are spaced laterally by wire-wrap spacers and vertically supported at their lower ends on support bars. Figure IV-7 shows a typical 217-pin configuration. This drawing, however, is for the preliminary reference design and does not show the larger pins, wire-wrap spacer, or fuel-element dimensions for the final reference design. The radial-blanket elements are of similar configuration, but with fewer and larger pins.

The fuel elements are positioned and supported at their lower end in the reactor-support assembly. The tubular lower end of the nozzle of the fuel element fits into a tube in the support assembly. Sodium flows into openings in the nozzle and upward through the fuel bundle. Coolant-flow distribution to the elements is controlled by varying the size and number of openings.

A gap is maintained between adjacent fuel elements to provide for assembly of the elements into the core array and their removal therefrom, and to accommodate manufacturing tolerances in the dimensions of the fuel-element housings. A tight core is achieved by hard-faced overlay spacer pads at each corner of the fuel-element housings, which provide interelement bearing points. The entire core array is laterally supported at the outer edge by scalloped, radial flexible fingers mounted on the cylindrical stainless steel neutron-shield assembly. These flexible fingers act as cantilever beam springs that apply inward radial compressive forces. The elevation of the spacer pads and scalloped support plates is established to reduce changes of core reactivity due to thermal bowing of the fuel elements.

Reactivity is controlled by vertical movement of control rods, containing a neutron absorber, in and out of the core. Nine control rods are used as combined shim and safety rods, and six are used strictly as safeties; thus a total of 15 provides the necessary shutdown margin. All rods can be inserted from any position and at any time.

The control-rod assembly consists of the lower guide-tube, the absorber assembly, the actuator, and the drive. The external shape and size of the lower guide-tube are identical to that of the fuel-element

housing, and the tube fits into the grid-plate structure in the same way as a fuel assembly. The lower end of the guide-tube nozzle acts as a dashpot for the ram at the bottom of the absorber assembly.

The absorber assembly consists of a pull rod, an absorber column, and a snubber ram. During normal shim-rod operation the absorber column is moved vertically in the guide-tube by the actuator rod. The control rod is released by de-energizing the latch magnet, allowing the absorber assembly to fall freely into the core. Snubbing of the released rod is accomplished as the ram at the bottom of the absorber assembly enters the dashpot at the lower end of the guide-tube. During fuel handling the absorber assembly is disconnected from the actuator so that the actuators and drives can be raised sufficiently to permit rotation of the top shield.

C. Sodium Heat-transfer System

Three $\frac{1}{3}$ -capacity heat-transfer circuits are provided. The intermediate heat exchangers are elevated so that core decay-heat removal is assured by natural-convection circulation through any one of the three loops, and uncovering of the core as the result of a heat-transfer system leak is prevented. Figure IV-4 shows the relative elevations of loop components. In the event of a component failure in one of the loops the reactor can be operated at a reduced power on two of the three circuits, with the third isolated to allow decay of sodium activity. One-loop operation is not permitted.

The sodium pumps are variable-speed free-surface single-stage centrifugal units. The main primary loops contain loop-isolation valves, a flow-control butterfly valve to control thermal-convection flow following a scram, and a check-valve to prevent backflow from the high-pressure plenum on loss of a pump. The IHX's are counterflow hockey-stick shell-and-tube heat exchangers, with the primary sodium on the shell side.

All primary-loop components are located in inert-gas-filled shielded equipment vaults. Except for the piping to the IHX, the secondary-system components are located in the steam-generator building.

The steam generators are divided into three banks, one for each secondary loop. Each bank is arranged separately in an in-line array with four superheat modules, three reheater modules, and six evaporator modules. The modules are mounted vertically and are shell-and-tube heat exchangers with a hockey-stick configuration. Steam is on the tube side. All the modules are similar in design, having 382 tubes and varying only in the length and diameter of the tubes.

Each module has a hydrogen detector mounted on

Fig. IV-2. Plant Arrangement.

EQUIPMENT

1. REACTOR TOP SHIELD
2. REACTOR VESSEL
3. REACTOR VESSEL LINER
4. CAVITY LINER
5. REACTOR CONTAINMENT DOME
6. SPENT FUEL STORAGE VESSEL - 10' ID
7. FUEL CANISTER ENCAPSULATING CELL
8. REACTOR COMPONENT WASH CELL AND MISC. STORAGE PITS
9. SHIPPING CASE
10. SHIPPING CASE AIR LOCK - 8' ID CLEAR
11. FUEL HANDLING GANTRY
12. PLUG FOR BELOW-FLOOR MAINTENANCE OF FUEL HANDLING MACHINES.
13. PROCESS EQUIP. WASH CELL - 8' DIA - SERVED BY PREL. PUMP
14. VIEWING WINDOW
15. COOLING EQUIPMENT FOR SPENT FUEL STORAGE VESSEL
16. RECIRCULATING H₂V UNITS
17. PRE-AMP ROOM
18. PRIMARY PUMP DRIVE
19. VALVE OPERATORS
20. ACCESS PLUGS FOR IHK MAINTENANCE
21. COLD TRAP ACCESS PLUGS
22. IHK ENCLOSURE
23. EQUIP. ACCESS HATCH (THROUGH TO BOX LEVEL)
24. PRIMARY PUMP
25. IHK
26. PRIMARY BLOCK VALVES
27. CHECK VALVE
28. THROTTLE VALVE
29. SECONDARY PIPING PENETRATION SEALS (DOUBLE BELLOW)
30. COLD TRAPS
31. H₂V AND SERVICE PIPE CHASE
32. PRESSURE RELIEF PANELS FOR VAULT RELIEF TO DRAIN TANK BELOW (DBA RELIEF) - TYPICAL EACH VAULT
33. ATMOSPHERE SEAL - TYPICAL
34. REACTOR OVERFLOW TANK
35. DEGASIFIER / SURGE TANK
36. DEGASIFIER RETURN PUMPS
37. PRIMARY NO. SERVICE PUMPS
38. PRIMARY NO. SERVICE PIPE CHASE
39. PRIMARY NO. DRAIN TANK - 55,000 GAL
40. PRIMARY NO. DRAIN TANK - 30,000 GAL
41. R/A LIQUID WASTE TANKS - DRAINS FROM WASH & ENCAPSUL. CELLS
42. COVER GAS PURIFICATION SYSTEM
43. POST DBA COOLING EQUIP. - USED DURING NORMAL OPERATION - EMERGENCY EQUIPMENT IS OUTSIDE BUILDING
44. PERSONNEL AIR LOCK - 5'4" x 7' CLEAR - AT GRADE
45. LARGE EQUIPMENT ACCESS OPENING - 52' DIA. - WILL CLEAR REACTOR TOP SHIELD AND IHK

FIG. IV-3. Plans for Arrangement of Equipment in Reactor Building.

SECTION D-D

SECTION E-E

SECTION H-H

SECTION F-F

SECTION G-G

EQUIPMENT

1. REACTOR TOP SHIELD
2. REACTOR VESSEL
3. REACTOR VESSEL LINER
4. CAVITY LINER
5. REACTOR CONTAINMENT DOME
6. SPENT FUEL STORAGE VESSEL
7. FUEL CANISTER ENCAPSULATING CELL
8. SHIPPING CASE
9. SHIPPING CASE AIR LOCK
10. FUEL HANDLING GANTRY
11. PROCESS EQUIP. WASH CELL
12. VIEWING WINDOW
13. COOLING EQUIPMENT FOR SPENT FUEL STORAGE VESSEL
14. RECIRCULATING HEV UNITS
15. PRIMARY PUMP DRIVE
16. VALVE OPERATOR
17. ACCESS PLUG FOR INX MAINTENANCE
18. INX ENCLOSURE
19. PRIMARY PUMP
20. INX
21. PRIMARY BLOCK VALVE
22. CHECK VALVE
23. THROTTLE VALVE
24. SECONDARY PIPING PENETRATION SEAL
25. COLD TRAP
26. ATMOSPHERE SEAL
27. REACTOR OVERFLOW TANK
28. DEGASIFIER/SURGE TANK
29. DEGASIFIER RETURN PUMP
30. PRIMARY NO. SERVICE PUMP
31. PRIMARY NO. DRAIN TANK - 55,000 GAL.
32. PRIMARY NO. DRAIN TANK - 30,000 GAL.
33. R/A LIQUID WASTE TANKS (PUMPS - DRAINS FROM WASH & ENCAPSUL. CELLS)
34. COVER GAS PURIFICATION SYSTEM
35. PRIMARY FLOWMETER
36. BUILDING CRANE - 12.0 TON CAPACITY
37. EX-VESSSEL FUEL HANDLING MACHINE
38. TOP SHIELD HOLD-DOWN STRUCTURE

Fig. IV-4. Equipment Arrangement in Reactor Building, Sections.

FIG. IV-5. Vertical Section through Reactor.

FIG. IV-6. In-plant Fuel-handling Sequence.

the sodium-outlet piping to detect any water leakage into the sodium. It is expected that any leak normally will be detected in time to shut down the affected bank before significant damage is done. Rupture discs and a relief system are provided in the event of a failure large enough to cause excessive pressures. The relief system separates liquid sodium from the gases and holds the sodium in a dump tank. The gases are discharged to the atmosphere through separators and a vent stack.

D. Turbine-generator Plant

The turbine-generator is a 3600-rpm, tandem-compound, six-flow, 33½-in., Last Stage Blading (LSB) unit. The rated capability is 1040 MWe at 0% makeup and 1.5 in. Hg abs. Steam conditions are 2400 psig and 1000°F at the throttle with 1000°F re-heat. The heat-balance diagram is shown in Fig. IV-8.

V. INFLUENCE OF SAFETY ON DESIGN AND RESULTS OF SAFETY ANALYSIS

All accidents may be grouped into two classes: those which may be expected to occur on the order of once

in the lifetime of the plant (Class B), and those which are highly unlikely to occur during the plant lifetime (Class A). Examples of Class B accidents are loss of pumping power and steam-generator leaks. Examples of Class A accidents are a guillotine pipe rupture, a large gas bubble, and the handling of an overpower element in the ex-core fuel-handling machine. Figure V-1 illustrates the safeguard actions for the two classes of accidents. Because of the assumed greater probability of Class B accidents, two independent safeguards are provided, followed by containment should both safeguards fail. Because of the very low occurrence probability of Class A accidents, only one level of safeguards is provided prior to requiring containment.

It may be possible to design a highly reliable protective system with greater confidence than an equally reliable containment system for core excursions. Those accidents which do not involve the core, but release radioactive material to the containment system (such as a large sodium fire), can be contained with a very high level of confidence. If then, an additional safeguard were added for all Class A and Class B accidents which involve the core, a safer plant might result

even with the removal of many features which are provided to contain a major core excursion.

A. Bases for Present Approach

The present approach of providing containment for a major core excursion was selected because it is believed to be conservative with respect to showing that a 1000-MWe FBR is economically viable. Furthermore, it was uncertain as to whether or not, at this time, the additional safety features required for the alternative approach could be defined well enough to assess their performance. It was therefore concluded that the objectives of the program could best be met by (1) designing the reference plant based on containment of a major core excursion; (2) recommending a development program which would allow a decrease in the large design margins for present containment uncertainties; (3) recommending additional development which would lead to the inclusion of additional features that would preclude any reasonable probability of a core excursion.

B. Accident Selection

A review of the preliminary reference design⁽³⁾ and reactor safeguard reports resulted in considering the accidents listed below as having the potential of significantly influencing the design:

- 1) unprotected† loss of coolant flow to the reactor;
- 2) unprotected† guillotine pipe rupture with double-ended flow;
- 3) large gas bubble ($\rho > \$1.50$) passing through the core of the reactor;
- 4) control-rod ejection while at full power;
- 5) fuel element dropped into a just-critical core;
- 6) large sodium spill in an open cell;
- 7) loss of cooling in ex-core fuel-handling machine.

One of these accidents will later be selected as the Design Basis Accident (DBA), but, as will be seen, several of these accidents will have a significant influence on the plant design.

To estimate the credibility and design implications of these accidents, fault trees were constructed and detailed analyses of the events following the accident initiation were performed. Based on these studies, design or procedural requirements were established to minimize the probability of initiating the accident and the required protective action was identified, as shown in Table V-1.

It was concluded that the design measures listed in Table V-1 would adequately preclude the possibilities of a rod-ejection accident or the dropping of a fuel element into a critical core. It was also con-

cluded that a loss-of-site-power accident, with subsequent protected loss of reactor flow, was a Class B accident. Order-of-magnitude estimates were made of the probability of the occurrence of a given accident relative to the protected loss-of-coolant flow accident (loss of pump power). From these estimates, shown in Table V-1, it is apparent that the pipe rupture, large gas bubble, open vault sodium spill, and loss of cooling in the fuel-handling machine are Class A accidents, requiring only one level of protective action prior to containment.

The last column in Table V-1 (the product of the preceding two columns) represents the estimated probability of occurrence of an unprotected accident relative to a loss-of-flow accident. From the evaluation it can be concluded that, although all of the accidents must be considered in the design, only five need be considered for containment. The detailed evaluation of the unprotected accidents results in equivalent consequences for the two most severe accidents: the loss of flow and the guillotine pipe rupture. Because the loss-of-flow accident is the most probable, it was selected as the DBA. Therefore the DBA consists of the following postulated sequence of events:

- 1) Power is lost to the pumps.
- 2) Action of the control system fails.
- 3) Both the primary and secondary protective systems fail to act.
- 4) No effective operator action is taken.
- 5) Voiding of the core is initiated in 7 to 10 sec as the flow decreases to about 25% of its initial value.
- 6) Excessive reactivity is inserted, following core voiding, due to the positive core-void coefficient. The maximum reactivity insertion rate is 66 \$/sec when \$1.50 has been inserted.
- 7) The transient is terminated by disassembly of the core.
- 8) The fuel mixes and comes to thermal equilibrium with the sodium in the core, which subsequently expands isentropically, resulting in an estimated 2200 MW-sec of work.
- 9) The top shield rises, releasing sodium and fission products to the containment dome.
- 10) Fission products and sodium are vented to the reactor cavity and to the vaults through rupture discs. They are also vented from the reactor vessel if it ruptures.
- 11) Some fission products and sodium leak from the inner containment to the outer containment.
- 12) A small fraction of these fission products leak through the outer containment structure to the outside.

† Protective system fails to operate.

FIG. IV-7. Fuel-element Assembly.

FIG. IV-8. Heat-balance Diagram.

C. Some Design Implications of Accident Evaluations

Evaluation of the protection requirements of the pipe-rupture or vessel-leakage accidents resulted in the design features illustrated in Figure V-2. The three elevated loops provide redundant emergency free-convection cooling. The elevated loops, the in-vessel inlet lines, and the pump and IHX pits prevent siphoning of the sodium below the safe level for convection flow in the remaining loops. A jacket is provided for the vessel to assure maintenance of a safe sodium level in the event of a vessel leak.

Evaluation of the DBA resulted in the plant containment features shown in Figure V-3. Following the postulated core excursion, the motion of the top shield is controlled by the top-shield holddown structure. This structure subsequently transmits the load to the building. Inert gas (maximum 2% oxygen) is provided to the inner containment, which is designed for a maximum leakage rate of 10%/day at 20 psig and 100°F. To provide adequate attenuation of the fission

FIG. V-1. Accident Sequence and Safeguards.

product release, an outer containment is provided that has a design leakage rate of 0.5%/day at 10 psig and 100°F. Additional features required are the building air filter, building concrete shielding, missile barrier for the control-rod drive system, and the emergency cavity-liner cooling system.

The alternative approach of providing additional

TABLE V-1. ACCIDENT SELECTION

Accidents	Design or Procedural Action	Protective Action	Estimated Probability of Accident Initiation Relative to Loss-of-flow Accident	Estimated Probability of No Protective Action Relative to Loss-of-flow Accident	Estimated Probability of Unprotected Accident Relative to the Unprotected Loss-of-flow Accident
Core-excursion Accidents Loss of coolant flow	1) Alternative sources of pump power 2) Design highly reliable pump and pump-drive systems	Reactor trip on high power-to-flow ratio with high temperature as a backup signal		1 (Reference)	1 (Reference)
Pipe rupture	1) Assure ductile piping during all operating conditions 2) Provide adequate stress design margins 3) Provide sodium leak detection of small leaks	Reactor and pump trip on loss of sodium level in reactor vessel	$\ll 1$	$\gg 1^{(a)}$	~ 1
Large gas bubble	1) Design to minimize entrainment 2) Design to avoid all possible gas-collection sites 3) Provide vents at all remaining potential gas-collection sites 4) Design system to break up and disperse all large gas bubbles	Reactor trip on high power-to-low-flow signal from gas bound flowmeter	$\ll 1$	$\gg 1$	~ 1
Rod ejection	1) Design to minimize probability of subjecting rod to sufficient pressure to eject it if not held down 2) Design to provide adequate holding force to prevent ejection even if maximum theoretical pressure were available 3) Design to avoid melting of rod cladding in event of blockage of coolant inlet	None	~ 0	$\gg 1$	~ 0
Dropped fuel element	1) Provide adequate refueling margin to preclude achieving a critical core during refueling 2) Design mechanical keys into fuel elements to prevent misloading 3) Establish procedures and tests to minimize probability of a gross loading error	None	~ 0	$\gg 1$	~ 0
Ex-core Accidents Large sodium spill	1) Provide adequate procedures to avoid gross error resulting in a large sodium spill with vault open	1) Valve interlocks 2) Administrative control	$\ll 1$	$\gg 1$	~ 1
Loss of cooling in the fuel handling machine	1) Provide redundant cooling capability 2) Establish procedures and fuel-handling-machine programming to prevent removal of a high-powered element 3) Provide low-leakage seals to contain released fission gases within fuel-handling machine	1) Provide instrumentation to detect loss of cooling 2) Remote means of returning fuel to reactor storage position	$\ll 1$	$\gg 1$	~ 1

^(a) According to the reference approach only one safeguard system is provided for Class A accidents.

FIG. V-2. Safety Features—Coolant Integrity and Core Cooling.

safeguards to eliminate the core excursion as the DBA, which was mentioned previously, is expected to have the net effect of increasing the reliability of the protective system, thereby reducing the risk to the public and to capital investment. This would result in the large sodium fire becoming the DBA and would allow the following changes in the containment features shown in Figure V-3:

- 1) removal of the containment dome and its storage structure;
- 2) elimination of the top-shield holddown;
- 3) considerable decrease in the size of the support ring structure;
- 4) considerable decrease in structural requirements of the inner containment;
- 5) removal of test channels from weld seams in inner containment liner;
- 6) considerable decrease in the air-cleanup system.

The cost savings which would result from these changes, although not specifically estimated, are expected to be several percent of the plant capital cost. In addition, the absence of the containment dome and top-shield holddown would result in some savings in refueling time. Thus, this alternative ap-

proach would likely provide increased safety at lower cost. It is recommended that this approach be evaluated in detail as part of any contemplated future safety studies.

D. Summary of Accident Analyses

1. Inner-containment Loads

The maximum pressure loads for the inner containment are set by the DBA to be 51, 22, and 20 psig for the reactor cavity, containment dome, and equipment vaults, respectively.

2. Outer Containment Loads

The maximum pressure loads for the building are set by the open-cell sodium spill and subsequent fire to be 2.2 psig. This pressure is reached in about 4 hr after the accident, and the maximum gas temperature in the building is about 180°F. The maximum building pressure reached during the DBA is 1.2 psig.

3. Radiological Consequences of the DBA

Based on the upper limit consequences of 73% of the core melting and 3% vaporizing, the initial activity releases to the inner-containment atmosphere were

FIG. V-3. Features of Plant Containment.

TABLE V-2. SITE-EXCLUSION RADIUS DOSES FOR LOSS-OF-FLOW ACCIDENT

Critical Organ	Exclusion Radius ^(a) (2-hr Exposure, rem)	Ratio: Calculated Guideline ^b
Bone	1	0.073
Thyroid	0.5	0.015
Lung	0.88	0.012
Whole body	0.048	0.0019

(a) 2000 ft.

(b) Dose guidelines (rem); whole body, 25; thyroid, 300; bone, 150; lung, 75.

TABLE V-3. SITE-EXCLUSION RADIUS DOSES FOR LARGE SODIUM FIRE AND FUEL-HANDLING ACCIDENTS

Accident	Critical Organ	Exclusion Radius ^(a) (2-hr Exposure, rem)	Ratio: Calculated Guideline ^(b)
Sodium fire	Whole body	0.012	0.00048
Fuel handling	Whole body	0.0085	0.00034

(a) 2000 ft.

(b) 25 rem.

assumed to be 100% of the rare gases and halogens, and 10% of all fission products and plutonium. No allowance was made for the probable trapping of a considerable portion of the nongaseous activity in that fraction of the sodium which did not become an aerosol. Credit was taken for aerosol fallout. As shown in Table V-2, a large margin is provided between the site-exclusion radius doses and the guidelines. There are no restrictions as to low-population zone.

4. Radiological Consequences of the Large Sodium Spill and Fuel-handling Accident

Of these accidents, analysis showed that doses from the fuel-handling accident would be less than one-half of the doses resulting from a sodium spill. The doses resulting from a large sodium spill (250,000 lb) would be small compared to those resulting from the DBA. This can be seen by comparing the results shown in Table V-3 with those in Table V-2.

In conclusion, the design of a safe 1000-MWe LMFBR appears technologically and economically feasible.

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Discussion

Mr. Harde (Interatom): I wonder if I correctly understood your fuel-handling procedures. You indicated that you would take out fuel subassemblies im-

mediately after a shutdown of the reactor. My questions are: (1) How do you cope with the afterglow heat immediately after shutdown? (2) What is the purpose of your in-vessel decay storage? (3) Why did you put your secondary pump into the hot leg?

Mr. Aronstein: With regard to fuel handling, the subassemblies are put in a sodium pot. There is a certain amount of decay time during the preparation for shutting down the reactor and in preparation for fuel handling. This is approximately 10 hr. By that time substantial decay has taken place; the fuel subassemblies are then taken from the core and placed inside a canister or pot which remains filled with sodium, and are handled by the ex-vessel machine. There is sufficient capacity for heat removal in the sodium-filled canister to allow this handling.

Mr. Harde: What amount of afterglow power are you talking about, 50 kW?

Mr. Aronstein: The afterglow power is approximately 35 to 40 kW. Subassemblies are handled in a programmed manner, too, starting with the radial blanket subassemblies. The highest-power subassemblies are not replaced until late in the refueling cycle.

Mr. Harde: I understand that the heat is transferred through the surface of the canister by forced-convection helium cooling.

Mr. Aronstein: It appears that forced-convection helium cooling is not required, although more detailed analysis may prove that it is. The detailed analysis that has been done to date indicates that the canister and spent-fuel subassembly can be cooled simply by cooling the wall of the fuel-handling machine, and forced convection will not be required. However, that could be added should we run into any difficulty in that regard.

Mr. Harde: What about the secondary pump?

Mr. Aronstein: We chose to locate the pump at the high point in the system to avoid difficulty in problems of level control. We would have a trade-off between locating the pump in the hot leg or running the secondary piping up to the high point again to locate the pump in the cold leg. It appears to be a break-even and possibly a little economical advantage to locate the pump in the hot leg. The only penalty that we would anticipate is the increased pumping power by locating in the hot leg.

Mr. Anderson (W): The drawing in your summary description shows a 30-in. gas plenum on the fuel rod, but your table shows 64 in. I assume that the 64-in. plenum is correct, but can you explain this discrepancy?

Mr. Aronstein: That fuel-element drawing was included only to show a typical element. We have since gone to the wire-wrapped configuration and a

larger pin, as well as a different core height. At the higher temperature, the 1140°F outlet temperature, the longer plenum length is required.

Mr. Noyes (CE): I would like to ask a question about your conclusion that there appeared to be no economic incentive in increasing the linear heat rates above 15 kW/ft. We have found that the optimum pin size is also larger. I was wondering if that comparison on heat rating was done at a constant fuel-pin diameter, or if the pin size was re-optimized for the higher heat rating.

Mr. Ash: The analysis was done for the 0.250-in. pin. We didn't do the optimization on kilowatts per foot for the 0.300-in. pin, and you may be right; the conclusions may be different.

Mr. Margen (AB Atomenergi): The last question raises the point that although very impressive trade-off studies have been made, no complete optimization program was apparently used. I am wondering really, when such a big effort is going to the fast breeder program, why I have not heard any reference to such an optimization program, which doesn't seem to need very much additional effort. Very often these trade-off calculations done at one point don't give the complete answer. This is one question, and I am wondering whether, if the private firms are not doing this, would Argonne or some central institute provide such a program in which the private firms could put in their own data. The second point is about the basic assumptions you put in your optimization, such as the burnup relation as a function of temperature. This also seems to be something which could, with advantage, be dealt with on a common basis between the different firms and Argonne. Can you tell us exactly what numbers you used in megawatt days per degree and to what extent this has been discussed commonly? The same applies again on safety considerations: to what extent has modification of the negative power coefficient been agreed upon and how has this affected your optimization?

Mr. Ash: It appears as though there are about four questions there. Let me answer the first one on the overall optimization. I think that this is something that could easily be done; for example, the various codes that we have could be easily integrated together into a comprehensive code to do an overall trade-off and get the complete interactions, and possibly use more refined optimization than individual linearization. I am not sure that the data available at this time is good enough to warrant such an optimization. Maybe it will be someday. The assumptions you have to make as regards the economics, the sensitivity of which is extremely important, and to refine the model to that extent, require some pretty good data.

Maybe someday we will be there but, in my opinion, at this time it is really not warranted.

Mr. Margen: The last question was on safety and whether you have had a common discussion on safety philosophies.

Mr. Ash: Yes, we tried to take the light-water reactor criteria and modify them to the extent necessary to make them adaptable to an LMFBR, using the intent of light-water criteria. The negative power coefficient came from the light-water reactor safety criteria and is pretty uniformly accepted for light-water reactors. We have had some discussions about whether the overall or the local power coefficient should be negative; that is a separate argument. I think the overall coefficient should be negative, and I think that this also is reasonably accepted. Regarding the local coefficient I am not sure; this is still an open question.

Mr. Denielou (CEA-Cadarache): I have two short questions. You made a safety analysis based on three completely different types of accidents and got a \$40 per second ramp rate. Is it due to chance? (Laughter.) My second question is: how do you take into account the effect of your modifications of the total fuel cycle, let us say, for instance, capital cost of the treatment plant, transportation, and so on?

Mr. Lancet: I'll answer the first question. The pipe-rupture and the flow-coastdown accidents have, as far as we can tell, turned out to be identical for all practical purposes. The resulting ramp rates came within a dollar or two per second of each other in the analyses. The gas-bubble accident actually covers a wide range, going up to the order of \$80 a second, for a coherent large bubble, completely undisturbed. In the viewgraph a symbol was left out that indicated *less than* \$40, which was supposed to represent our estimate of the kind of breakup that we could anticipate. In our own program we are doing some experimental work to verify that the breakup indeed does occur.

Mr. Golan: Regarding the assumptions for fuel-cycle cost, I believe you are asking about the economic parameters. On the fabrication cost, we developed our own economic model, which essentially was predicated on an assumed capacity of about 180 kg/day. We varied the fabrication costs as a function of pin diameter and burnup. By going to lower burnup we increased the capacity, so we tried to account for that. We accounted for core height based on studies we have done at AI. Regarding reprocessing costs, we were furnished some ground rules for the study; these are basically the AEC assumptions regarding reprocessing costs as a function of enrichment. On shipping

costs, we have made our own estimates in this regard, and we have used these estimates and haven't varied them for the various capabilities, but have kept them constant.

Mr. Busch (Kaiser Engineers): The gentleman from Sweden raises a point that I think you really ought not to dismiss so easily. Your optimization comes to two general conclusions, such as a larger pin and a higher temperature, which really belong to your elementary training. The corrections which you say are large in terms of lifetime for the equipment are not fed right back into your conclusions very quickly. If care is not used, you end up making little circles in your analysis. The higher temperature looks better for the economics, but a year or two later we find it is worse for the equipment costs, and we invest a great deal for a program with few returns.

Mr. Zinn (Chairman): AI will agree to that statement, and we can go on to the next question. (Laughter)

Mr. Ash: We agree.

Mr. Hobson (W): In your containment scheme you indicated 0.5% leakage per day for the outer vessel. Do you expect to demonstrate this by testing, and do you believe current techniques provide sufficient accuracy?

Mr. Lancet: If you mean demonstrate it in an actual plant, yes, and yes to the second question. Lower values up to 0.1% per day have actually been demonstrated and, I believe, published.

Mr. George (W): A characteristic of the AI design has been the large number of steam-generator modules. I get the impression that you are proposing to reduce the number of modules now, but you don't show a decrease in availability. If this is so, what was the economic rationale for using the number of modules; also, does AI propose to isolate both on the sodium side and the steam side to increase availability on the modules?

Mr. Aronstein: There was an availability correction made for the larger modules, and we do plan on isolating the sodium side. The water side is also isolated, and both are flooded with nitrogen.

Mr. George: Do you intend to repair while continuing plant operation?

Mr. Aronstein: I think the procedure would probably be to cut the affected module out of the system and go back into operation with that module missing. However, detailed studies of which way is the best to go for the larger module have not been made. There are a number of possibilities. We could envision stocking spare modules, but I don't believe this would be required or would be economically feasible.

Mr. Wolfe (GE): What do you do with the debris after the Bethe-Tait accident?

Mr. Lancet: I tried to go over that fairly quickly. (Laughter) If you noticed on the slide, we have, conceptually, a cavity-cooling system which size-wise is fairly easy to define, about 50 MW, taking into account the time it would take for the fuel to settle down. After a large excursion the vessel would probably still be mostly intact. There is no assurance, at least as far as we can determine, that we could cool the fuel in the vessel, and therefore we would expect it to melt through the bottom and fall onto the bottom of the reactor cavity. We have not gone into detailed design, but we are anticipating that we can remove the heat from the cavity liner. Preliminary analyses indicate some basic controlling mechanisms that prevent large humps of fuel collecting or transferring much heat, just due to boiling. The poor conductivity of the fuel is kind of a self-limiting effect which limits the amount of local heat you get to a given area. And therefore we feel that cooling is feasible as long as we can figure out a way to prevent buckling and some of the thermal problems that would bother the liner.

Mr. Beckett (W): There was a conflict in two slides; one showed a top-entry vessel and the other showed a bottom-entry. I would like to know which is your preferred vessel design and why?

Mr. Aronstein: Top entry is the preferred system for the reactor vessel. Bottom-entry piping, where the inlet piping was not incorporated internal with the vessel, was our previous design. We have since gone to the internal downcomer in the vessel.

Mr. Beckett: Why did you change from a bottom- to a top-entry vessel?

Mr. Lancet: In considering the guillotine pipe-rupture accident, we were very concerned. We originally had a bottom-entry pipe, and we were very concerned about the ability to contain a pipe rupture with a shroud that would essentially provide double containment, so that if a pipe ruptured we would not drain the whole system. As soon as we put any pipe below our minimum level we suffer from the problem of siphoning if we rupture a pipe down there. So, one aspect was to avoid that particular problem. We raised the inlet of the pipe to above our minimum level so that we could not then drain the loop under any circumstances. Secondly, it gave us then a very clean vessel. Part of our inspection system is a skate device which is remotely controlled; it runs down the guard vessel and has a capability for inspecting the vessel

and also remotely making repairs, if necessary. So we got a cleaner vessel that was easier to inspect and re-

pair, and we also avoided this rather ticklish problem of pipe rupture.

Mr. Zebroski (GE): The ground rule for this study, I believe, was to take a look at 1980 to 1985 technology or project to 1980-1985 technology. This is for two reasons, first to get to the large size and target performance, but also so that the trade-offs would give you a guide to the development between now and then, the next 12 to 17 years of development. I know this is a very complicated question, but I am trying to phrase it constructively. I wonder if you would identify at least a few of the major items which would serve as a guide to development. So much of the design does seem, except for size and the small difference in temperature, not very different from what you have designed for current planning.

Mr. Golan: We didn't quite interpret the guidelines for this program the way you did. Mr. Fromm introduced this morning's session by reviewing the guidelines. Basically, they are to look at plants that various reactor manufacturers would favor for production in the 1980 to 1990 period. This is what we have addressed our approach to. Now, we happened to select the early part of this period as our target date, and that had a major influence on our design. As far as the second part of your question, we have identified several important areas of R&D about which we are concerned. Mr. Ash mentioned the larger pin size. Right now we have very little data on the larger pins, but would like to get much more. We have to look at problems associated with leaving the radial blanket in the reactor 5 to 7 years, and also to look at going from 1060°F to 1140°F in outlet temperature. Here are substantial increases in technology. I think based on your comments yesterday, I would come to that conclusion. I think that in going to larger steam-generator modules we also present another step in testing. I think we have defined quite a substantial R&D program, and we have quite a bit of work to do before we can design and build that plant.

Mr. Wyckoff (ComEd): Did you find that if you did not increase the core outlet temperature from 1060°F to 1140°F that the FBR would not be competitive with the light-water reactor? Your economics seem marginal all the way through, and I noticed you had an availability of 90%. Was this increase in conditions necessary to have a break-even with the water reactors?

Mr. Golan: As I indicated in my presentation, right now it does not appear to us that our LMFBR design

needs to go the higher outlet temperatures to be competitive with the then-current light-water reactor. Of course, what is this moving target? That is a debatable point, but based on our economic assumptions with respect to this light-water reactor target plant, we believe that we do have sufficient margin at the

1060°F outlet temperature to make that plant attractive for utility systems.

Mr. Zinn: I will have one question as chairman. You mentioned a moving target for the light-water reactors. Which way did you assume that it was moving? (Laughter)

R

Babcock & Wilcox 1000-MWe LMFBR Follow-on Study Reference Design

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1. Introduction

1.1. Background

From the beginning of the nuclear age, the need for a commercial power reactor that could fully exploit the vast amount of potential energy in our uranium reserves has been recognized. Further, fast breeder reactors have been established as the most likely contenders to fulfill this need. Over the years, the specific type of fast breeder with the greatest long-range potential has emerged from the continuing assessment of various concepts; this type—the LMFBR—has been selected as the prime candidate to meet the worldwide need for abundant, economical power.

There are many incentives for developing the LMFBR, but all of them originate inherently from the need just identified. The major incentives involve two major objectives:

- 1) to reduce power-generation costs directly by developing nuclear power plants that are more efficient and economical than those now in use;
- 2) to protect the large investment in light-water reactor plants (LWRs) by minimizing the effects of increases in uranium ore costs through efficient use of our natural uranium and the plutonium produced in LWRs.

Obviously, both objectives must be met soon if the LMFBR is to fulfill its important role. To achieve these objectives, the LMFBR must be economically competitive with alternative sources of electric power, and it must be developed for commercial service before our low-cost uranium reserves are exhausted.

In recognition of the need for fast breeders and the selection of the LMFBR to fulfill that need, the general LMFBR development activities, which have already clearly established the technical feasibility of the concept, have recently been accelerated by both government and industry, and oriented specifically toward the stated objectives. To continue the favorable trend toward a planned and disciplined program, the specific technology required for the LMFBR to compete successfully with existing power plants must be clearly defined from an overall plant standpoint. Since these requirements could be de-

scribed best by a series of plant design studies, the AEC in 1966 and 1967 awarded five major plant design contracts to potential vendors of fast breeder reactors; these contracts were awarded under the AEC's 1000-MWe LMFBR Follow-on Study. Babcock & Wilcox is participating in the Follow-on Study under Argonne National Laboratory (ANL) Contract No. 31-109-38-1998; the results of the conceptual design phase are presented in the remaining sections of this paper.

The scope of work for the Follow-on Study is summarized briefly in order to place subsequent discussions in the proper perspective. The selection and the evolution of the reference plant arrangement are reviewed in Sect. 2 in order to identify some significant considerations involved in the choice of the concept. This is accomplished by a short summary of the work performed in Task I^(1a) and an outline of the basic philosophy or criteria employed during the development of the reference design. This short, historically oriented review is supplemented and expanded in Sect. 3, which presents bases for selecting the major plant features along with a description of the reference design. Thereafter, an overall description of the 1000-MWe LMFBR plant is presented, and the core and the major considerations involved in its selection are discussed, including safety characteristics, fuel-management results, and fuel costs. Finally, the trade-off and parametric studies that aided in establishing the functional parameters and design conditions are discussed, and estimated power-generation costs are presented.

1.2. Program Scope and Objectives

The current Follow-on Study contracts were awarded to extend the fast reactor studies of 1963 and 1964, which involved a limited scope of work on the core, reactor vessel, and related areas in order to encompass the conceptual design of a complete 1000-MWe LMFBR central-station power plant. Parametric evaluations were made in order to optimize the design, and many variables were considered, including the effects of fissile- and fertile-isotope costs, installed capacity, safety criteria and requirements, breeding ratios, and doubling time; however, the conceptual design was based on established ground

FIG. 1. Work Scope and Schedule.

- Task I Develop Four Plant Concepts—Select Reference Design
- Task II Engineer Reference Design—Prepare System Design Description
- Task III Perform Trade-off and Parametric Studies—Prepare Cost Estimate
- Task IV Define R & D Requirement—Perform Cost-benefit Studies

rules and on the estimated state of technology in 1980.

The primary objective, which originally emphasized the design aspects, was redirected during the study to stress the research and development portion of the work. The revised objective as stated by ANL is as follows:

"The overall objective of the current 1000-MWe LMFBR Follow-On Study Program is to provide the AEC with information needed to establish research and development programs which will lead to the achievement of economic and safe LMFBR power plants by a viable nuclear industry. This information is to be developed through studies of 1000-MWe LMFBR power plant reference conceptual designs favored by the nuclear industry for further development and eventual sale to the electric utilities in the period 1975 to 1985."

To achieve this overall objective and the specific objectives logically ensuing from it, B&W's work under the Follow-on Study was divided into four major tasks as identified in Figure 1. Task I, which involved the development of four basic plant concepts

and the selection of a preliminary reference design, was completed in 1967. The work under Tasks II and III, which included parametric and trade-off studies conducted in parallel with the conceptual design of the preliminary reference design, has also been completed. The final phase, specifically oriented to R&D considerations, will be completed before the end of the year. This work under Task IV includes the identification of R&D requirements, the preparation of program plans that will, when executed, generate the necessary technology, and the performance of cost-benefit studies to establish the incentives for conducting the R&D programs.

2. Choice of Design

2.1. Concept Selection and Preliminary Design

Many basic choices must be made in selecting a particular LMFBR concept; these include such major decisions as the type of fuel, basic core geometry, refueling method, and arrangement of the heat-transfer system.

To explore the advantages and disadvantages of the most important features before selecting one design, four basic plant concepts—two loop or spread-out arrangements and two integral pot arrangements—were developed under Task I during the first nine months of the contract. These concepts are described in Volumes 2 through 5 of BAW-1316.^(1a) The major features of each concept are briefly summarized below.

Although the preliminary design that was selected after these concepts were developed and evaluated combined the best features of each concept to the extent that they were compatible in one plant arrangement, the composite design was largely similar to the Concept IV design. This concept incorporates an under-the-plug refueling method and an integral pot arrangement in which the major components are submerged in the primary sodium. It was selected because the pot arrangement seems to be more re-

	Concept			
	I	II	III	IV
System configuration	Integral pot	Compact loop	Spread-out loop	Integral pot
Core geometry	Pancake	Cylindrical	Annular	Pancake
Fuel	Oxide, non-vented	Oxide, non-vented	Oxide, vented	Oxide, vented
Refueling	Through-rotary plug	Hot cell	Hot cell	Under-rotary plug
Primary loops	6—open	6—closed	3—closed	6—open
Intermediate loops	3	3	3	3
Control	Integral hydraulic shim/mechanical regulating	Mechanical	Integral hydraulic shim/mechanical regulating	Integral hydraulic shim/mechanical regulating
Steam cycle, psig/°F/°F	1450/1000/—	1450/1000/—	2400/1000/1000	2400/1000/1000

table and should provide the best operating and economic characteristics. Safety considerations were also paramount in the choice. An annular core geometry was selected pending the results of the "spoiled versus unspoiled geometry trade-off study" which was then in progress. Table 1 is a summary of the key features and parameters selected for the preliminary reference design.

2.2. Work Leading to Reference Design

During the nine-month period following selection of the preliminary design, more extensive engineering work was performed to establish definitely which features and parameters should be retained in the final reference design and to define the changes that should be made to the initial selection. Architect-engineer services were obtained to establish the turbine island and overall plant arrangement, and trade-off and parametric studies were conducted in parallel with the design of the selected concept. Wherever these studies identified features, arrangements, or parameters that were better than those in the preliminary design, the superior item was incorporated into the reference design. The key features that were initially incorporated into the preliminary design but changed in the evolution of the reference design are summarized as follows:

Preliminary Design	Final Reference Design
Primary components: combined IHX—pump assembly	Separate IHX and pump assemblies
Control-rod actuators—safety/shim: hydraulic; regulating: mechanical	Safety/shim/regulating: mechanical
Core geometry: annular	Cylindrical
Fuel-element support structure: honeycomb, fixed can	Solid integral can
Pot location: central in containment	Off center in containment
Steam cycle: 2400 psi/1000°F/1000°F	2400 psi/950°F/950°F

2.2.1. Pumps and IHXs

In the preliminary design the combined pump-IHX units produced a more compact vessel, in which the smaller diameter was offset to some extent by greater depth. A re-evaluation of this unit was prompted by new data on the IHX and subsequent optimization of the unit, by a reduction in the diameter of the pump due to a decrease in core pressure drop, and by a rearrangement of the vessel internals. The revised layout showed that separate pump and IHX assemblies could be incorporated by increasing the diameter of the vessel by about 2 ft. In addition to reducing the complexity caused by the combined

TABLE 1. SUMMARY OF KEY FEATURES AND PARAMETERS FOR PRELIMINARY REFERENCE CONCEPT

Plant	
System configuration	Integral pot
Refueling	Under-rotary plug
Primary loops	6—open
Intermediate loops	3
Control, shim/regulating	Integral hydraulic/mechanical
Net cycle efficiency, %	43
Steam cycle, psig/°F/°F	2400/1000/1000
Core	
Geometry	Annular, honeycomb, fixed can
L/D ratio	1.16
Fuel assemblies	177
Volume fraction, %	
Fuel	0.46
Steel	0.14
Sodium	0.39
Control	0.01
Fissile loading, kg	2150
Average enrichment, %	12
Average burnup, MWd/T	82,000
Core breeding ratio	0.80
Total breeding ratio	1.35
Average linear heat rate, kW/ft	10.2
Specific power, MWt/kg fissile material	1.12
Δk void (EOL), \$	4
Doppler effect, $-T'(dk/dT)$	0.005
Doubling time, yr	10.5

units, separation of the components reduced the depth of the vessel by about 15 ft. Consequently, the change to separate units enabled us to reduce the depth of the reactor vessel, and thus the sodium inventory, and to reduce the height of the reactor vessel. However, the combined unit could conceivably offer some advantages depending on the size of the reactor and the conceptual layout.

2.2.2. Control Rods

The preliminary design utilized conventional overhead mechanical actuators for the regulating rods, and hydraulically actuated rods integral with the fuel assemblies for the shim-safety rods. The integral rods, because of their small physical size and large number, provided a very homogeneous core and offered greater flexibility in power shaping. The main disadvantages of the integral control rod are as follows:

1. lack of positive and dependable position indication;
2. acceptable scram accelerations and command valve response times have not been proved;

3. repair of any leaks in the many hydraulic lines in the reactor vessel would be difficult.

Lack of development of the integral control-rod concept was the primary reason for changing to conventional top-mounted control rods of the reference design. It was felt that there was not enough time to design these units under the Follow-on Study or to perform the required R&D by the 1980 target date. However, if further fast reactor development indicated that an independent shutdown system operating on a different principle was indeed required, then the integral rod would be considered again as a prime candidate.

2.2.3. Core

The core proposed for the preliminary design comprised canless fuel assemblies located in a honeycomb cell constructed of stainless steel. In the fixed honeycomb structure, which replaces the removable cans around each fuel assembly, only one wall is required between the fuel assemblies. The wall is required to withstand only the small differential pressure between adjacent assemblies. With canned assemblies, on the other hand, the boundary ligament between adjacent fuel bundles contains two thicknesses of can material plus the clearance between assemblies; each membrane takes full core pressure differentials and therefore must be thicker. Thus, the honeycomb structure enables the use of an orificed, high-pressure-drop core without requiring an increase in can thickness and, hence, steel fraction. Therefore, both fuel cost and doubling-time characteristics are improved. The higher the pressure drop across the core, the greater the effective saving. At the outset of this work a fuel cost savings of 0.1 to 0.2 mill/kWh was estimated, but it was realized that the structure would probably have to be replaced sometime during the lifetime of the plant, and that additional thermal and stress analyses were needed to prove the ability of the structure and the selected materials to withstand the operating conditions.

In the initial months of the preliminary design effort, a change in core configuration from annular to flattened cylinder reduced the core pressure drop significantly (discussed in Sect. 4). The lower pressure differential in turn reduced the wall thickness of canned assemblies to 0.130 in. At the same time, our stress analysis showed that the wall thickness in the honeycomb structure would have to be increased considerably to reduce the thermal stress in regions with large thermal gradients. Thus, the savings in steel fraction offered by the honeycomb was reduced. This, coupled with an allowance for an increased capital cost of \$2 million for equipment needed to

remove and replace the structure, reduced the net advantage of the honeycomb structure to 0.035 mill/kWh. The smaller economic advantage associated with the revised core configuration, coupled with potentially adverse thermal distortion characteristics and problems in completing the research and development within the time available, prompted the change to the more conventional canned assembly. The merits of this concept, however, still warrant consideration, particularly for core configurations with high pressure drops.

2.2.4. Containment

In the preliminary design, the diameter of the containment was 110 ft, and the reactor vessel was located centrally within the containment. This original design left only marginal room for the movement of primary reactor equipment for maintenance, and resulted in uneconomical use of areas for general access and equipment. Therefore, the vessel was offset approximately 10 ft from the geometric plan center of the reactor building, so that available building space could be used more efficiently, enabling the containment diameter to be reduced to 100 ft. A reduction in the length of the primary reactor equipment also helped to make this reduction possible.

2.2.5. Steam Cycle

The 2400/1000/1000 steam cycle was selected after an initial study in which preliminary information about component costs was used; subsequent studies, described in Sect. 5, led to a modification to the 2400/950/950 cycle.

3. 1000-MWe LMFBR Reference Design

In the preceding section we discussed the selection of the basic concept and identified some of the major changes to the preliminary design as it evolved toward the reference design. In this section we summarize the major features and design conditions for B&W's 1000-MWe Reference Design.

3.1. General Description

This LMFBR power plant is designed as a complete central-station, electrical-generating facility. It includes all systems, components, structures, and services necessary for constructing the plant on the AEC's hypothetical site in New England (as described in TID-7025, *Guide to Nuclear Power Cost Evaluation*). The site and the general building arrangement are shown in Fig. 2.

A schematic representation of the heat-transport path which functionally relates the primary coolant, intermediate coolant, feedwater, and energy-conver-

FIG. 2. Site and Building Arrangement.

FIG. 3. Diagram of Heat-transport Path.

sion systems is given in Fig. 3. This diagram shows the turbine generator, one of three intermediate loops, and one-sixth of the open primary loop. Individual components are discussed in more detail following a brief description of the systems.

Pool primary sodium at 800°F enters the suction side of the six primary pumps and flows at a total rate of 92.7×10^6 lb/hr, under high pressure from the pumps, through the connecting pipework and into the lower plenum of the core vessel. It then passes through the core and blankets, which generate 2450 MWt, and exits into the upper plenum of the core vessel at a bulk outlet temperature of 1100°F.

The hot sodium then flows from the upper plenum down through the shell side of the six IHXs by gravity flow and discharges to the sodium pool to complete the primary circuit.

The temperature of the sodium in the intermediate system is raised from 700 to 1000°F as it circulates through three identical loops. Each loop contains two IHXs, one once-through steam generator, one reheater, one sodium-bypass line, and one intermediate pump which is located in the cold leg. The IHXs, steam generator, and reheater are connected in parallel, and the sodium exiting from the steam generator and the reheater enters the suction side of the pump

and discharges to the IHX. A sodium-bypass line, which is also in parallel, permits variation of the flow and temperature conditions in the secondary system and thus simplifies control at partial loads.

Steam at 2400 psig and 950°F leaves the steam generators and flows to the high-pressure cylinder of the single-shaft, multicylinder, tandem-compound turbine with 30-in.-diameter last-stage buckets. The sodium reheaters raise the temperature of the exhaust steam to 950°F before it enters the intermediate-pressure cylinder. The IPT exhaust steam then flows to the turbines which drive the feedwater pumps and to the low-pressure cylinder, which has an operating exhaust pressure of 1.5 in. of Hg absolute. The full-power turbine output is 1,050,790 kW. The generator is a conventional 3600-rpm unit with a gross output of 1,033,926 kW and an average auxiliary load of 27,000 kWe; therefore, the net plant output is 1,006,926 kWe.

The associated feedwater system consists of a condenser, three condensate pumps, six feedwater heaters, and two turbine-driver feed-water pumps.

Figure 4 is a sectional view of the reactor building, showing the general location of major equipment

FIG. 4. Sectional View of Reactor Building.

in the building. All the primary reactor equipment, which includes the heat-generating and reactor-coolant heat-transfer equipment, the fuel-handling facilities, and the radioactive auxiliary systems, is located in the reactor building, or containment structure. The other heat-transfer equipment, including the intermediate sodium-coolant system, is located on the reactor island between the containment structure and the turbine island.

The reactor building is designed to sustain all normal loads and the initial effects of the DBA condition without loss of integrity and to retain its functional capability to protect the public. This cylindrical concrete structure has a welded steel liner on the inside surface to ensure a high degree of leaktightness. The structure has an inside diameter of 100 ft, and is accessible to reactor personnel during normal plant and refueling operations.

3.2. Reactor Vessel

The 52-ft-diameter reactor vessel, which is offset from the plan center of the building, is a simple cylinder with a semielliptical bottom. Thus, the vessel has no blisters, penetrations, nozzles, or stress raisers on any surface that is wet by sodium during normal operation. A biological concrete shield of 6.5-ft radial thickness completely surrounds the reactor vessel, and supports the vessel and its cover structure. The inside surface of the biological shield is steel lined; the interspace between the reactor vessel and the biological shield is kept small to reduce the loss of primary coolant. The interspace is cooled by a closed-circuit argon cooling system so that no insulation is required. The outside of the vessel is therefore accessible for indirect viewing or inspection as necessary. In addition, under DBA conditions with a postulated loss of all ability for internal decay heat cooling, the interspace between the vessel and the shield can be filled with sodium from the intermediate systems and used for emergency heat removal. Heat is removed from the biological shield by a twin system of NaK coils located in the concrete adjacent to the shield membrane.

The cylindrical reactor vessel has a flat, shielded, vault cover structure on top. The reactor vessel cover supports all the primary equipment and is directly attached to the cylindrical wall of the reactor vessel to form the pressure-vessel head. The head is a radial plate beam, and the upper side of the head is at the operating floor level. Three eccentrically mounted plugs, supported by bearings, facilitate indexing during refueling, are located in the cover structure above the core.

The cylindrical core vessel is centrally located in

the reactor vessel and is supported from brackets attached to the upper strake of the reactor vessel. The vessel has a semielliptical bottom, which forms the high-pressure plenum, and a flat top-grid structure. This structure forms the grid plate which supports and holds the lower end of the fuel and blanket assemblies for the reactor core. The wall of the vessel extends up around the core to form a flow divider between the core and the lower radial neutron shields. The upper extension of the core vessel separates the upper plenum from the rest of the pool.

The advantages of the reference primary system arrangement are as follows:

1. The primary-system design is compact, and the containment reactor building structure is small.

2. The sodium-retaining wet surface area is considerably reduced.

3. The reactor vessel is subjected to the lowest possible system pressure and temperature conditions, and cyclic conditions are reduced.

4. The nvt damage to the reactor vessel is reduced.

5. The pressure vessel has maximum integrity, so that leaks will not jeopardize reactor safety conditions.

6. The plant-control problems are eased because of the large primary sodium heat sink.

3.3. Core

The core contains 288 identical fuel assemblies and 25 control rods which are surrounded by 138 radial blanket assemblies (see Fig. 5). All assemblies are set

FIG. 5. Cross Section through Core.

on a 6.5-in. triangular pitch. Each fuel assembly comprises a cluster of 331 pins (each 0.280 in. in diameter) which are set on a 0.337-in. pitch and enclosed in a solid hexagonal can. Thus, each fuel assembly can be individually orificed. The radial blanket assemblies contain ninety-one 0.608-in. pins set on a 0.638-in. triangular pitch. The pins are held together by top and bottom assembly fixtures and intermediate spiral-twisted spacers. All fuel and blanket assemblies are located between the top holddown and a bottom fuel-element-support structure and are preloaded upward to prevent hydraulic lifting during operation.

The stainless steel-clad fuel pins are of the vented-to-sodium design, and have an active fuel length of 34.7 in. of gas-bonded, vibratory compacted $\text{PuO}_2\text{-UO}_2$ and a clad thickness of 0.010 in. Approximately 14 in. of depleted UO_2 material is assembled above and below the active fuel length to form the axial blankets. The radial blanket pins contain a 43-in. length of vibratory compacted UO_2 . The clad thickness is 0.017 in.

Twenty-five identical control rods and associated drives fulfill shim, regulating, and shutdown requirements. Each control rod consists of a cluster of tantalum pins clad with stainless steel. Extension rods connect the control rods with the drive mechanisms, which are mounted on the intermediate rotating plug in the cover structure. The extension rod is uncoupled from its control rod for refueling and for control-rod replacement. In the event of a reactor scram, both the control rod and the extension rod are uncoupled from the actuator mechanism and allowed to fall freely.

The advantages of the core design are as follows:

1. The fuel can enables individual orificing to both fuel and blanket assemblies.
2. The top and bottom radial locations of the fuel assemblies permit malalignment between grids while giving pitch control and maximum stability to the assemblies.
3. Temperature effects between control and fuel element effects are reduced.
4. Vented fuel improves power-generation cost and reduces fuel-handling and shipping problems.
5. Solid fuel cans reduce the probability of sodium-void propagation.

3.4. Shielding

The primary shielding system comprises axial and radial neutron and biological shields. Combination radial neutron-thermal shields are placed around the core to protect the primary equipment and the intermediate sodium from neutron activation. These

shields are placed in hexagonal removable cans supported by the core tank structure in the vertical position. The neutron-shield material is boronated graphite. Axial biological shielding is provided by the cover structure.

3.5. Components of Primary System

Located in the space between the core and reactor vessel, which contains the 800°F pool sodium, and supported from the cover structure are the six primary sodium pumps, six IHXs, two cold traps, and two emergency cooling systems. This equipment, which may be removed for maintenance, is divided into two groups. Each group is offset from the geometric centerline of the reactor so as to leave a clear and unobstructed path for the refueling machine and gantry mounting.

The primary pumps are free-surface, impeller-diffuser, single-stage, double-suction units; the flow capacity of each is about 37,000 gpm at a pressure rise of 70 psi and an inlet temperature of 800°F. Each pump is driven by a 1200-hp electric motor which is connected to its impeller shaft by an eddy-current coupling. The pumps are mounted in the cold leg of the circuit, and a pony motor is connected to each pump to supply coolant to the core during emergency conditions.

Each IHX receives hot primary sodium directly from the reactor core via the upper plenum and transfers the heat to the secondary sodium. The IHXs are vertically mounted, straight-through, shell-and-tube, single-pass units. The sodium flows counter-current, and the primary sodium flows through the shell by gravity owing to a differential head of approximately 13 ft (at full power) between sodium levels in the core vessel and the reactor vessel. The advantages of this layout are as follows:

1. The tank contains a single gas blanket pressure as a result of the open-loop configuration.
2. The internal design of the vessel is compact and simple.
3. The pressure-drop requirement for the primary system is reduced.
4. Leakage from the high-pressure primary system will only affect the operational characteristics of the plant.
5. The layout complements the advantages of the refueling system.

3.6. Refueling

Refueling is accomplished by a shielded fuel-transfer machine which is temporarily mounted on the inner rotating plug. Before the refueling operation begins, the control rods are fully inserted and un-

upled, and the upper holddown structure and attachments are lifted approximately 12 in. to permit rotation of the eccentric plugs.

With the refueling mechanism installed in the smallest rotating plug, rotation of the plugs provides direct vertical access to all fuel positions as well as to both storage drums. The refueling mechanism, which has push-pull action, acts only in a vertical direction and is continuously controlled within a guide tube attached to the mechanism. Thus, the action of refueling consists of a simple grabbing action, vertical lift, and rotation with the fuel assembly remaining immersed in the sodium and removal of decay heat taking place by natural circulation. One fuel assembly is removed at a time, and the procedure is reversed to install a new fuel assembly in the vacated space.

A total of one-third of the fuel assemblies are removed from the core annually while the reactor is in a hot shutdown condition. Irradiated fuel assemblies are removed from the storage drums (after a decay period of about two months) during normal reactor on-load operations. New fuel assemblies are also installed in the storage drums during reactor on-load periods in preparation for refueling.

The irradiated fuel is removed from the storage drum and transferred out of the containment using the shielded, gas-cooled cask mounted on a gantry above the reactor. This gantry mounting offers maximum stability and safety characteristics for the machine during earthquake or accident conditions. Gas-cooling of the fuel-element assembly reduces the transfer of active sodium out of the containment building, and reduces the problems associated with interface connections between the cask and the cover structure.

The advantages of the refueling system are as follows:

1. The fuel is continuously immersed in the sodium pool during initial fuel transfer.
2. The transfer of active sodium through the containment building is reduced.
3. Fuel transfer through the cover structure and the containment interface is easier.
4. The refueling machine enables handling of other reactor components.
5. Support of the refueling machine offers maximum stability during accident conditions.
6. No heavy refueling equipment is subjected to high lifts.
7. The fuel-handling system reduces reactor outage time, enabling weekend refueling operations.

The key plant parameters and operating conditions are summarized in Table 2.

TABLE 2. SUMMARY OF KEY PLANT PARAMETERS AND OPERATING CONDITIONS—REFERENCE DESIGN

Plant	
System configuration	Primary tank
Plant life, yr	30
Refueling scheme	Under-rotary plug then through-plug shield cask
Storage location for fuel decay	Primary tank
Primary loops	6—open
Intermediate loops	3
Control—shim and regulating-safety rods	Mechanical
Diameter of cylindrical containment building, ft	100
Diameter of cylindrical reactor vessel, ft	52
Core	
Geometry	Cylindrical
L/D ratio	0.29
No. fuel assemblies	
Core	288
Radial blanket	138
Method of core holddown	Bottom and top locations
Fuel-assembly-support method	Solid can
Type of pin	Vented to sodium
Operating Conditions	
Primary circuit	
Inlet temperature, °F	800
Bulk outlet temperature, °F	1100
Nominal temperature of hot-channel cladding, °F	1160
Nominal hot-channel fuel central BOL/EOC, °F	3896/3800
Plenum-to-plenum pressure drop, psi	34.4
Flow rate, lb/hr $\times 10^6$	
Core and axial blankets	83.78
Control rods	3.40
Radial blanket	3.97
Thermal shields	0.55
Leakage	1.00
	Total 92.70
Intermediate circuit	
Inlet temperature, °F	700
Outlet temperature, °F	1000
Flow rate, lb/hr $\times 10^6$	
Steam generator	66.69
Reheater	25.41
Energy conversion	
Steam cycle, psig/°F/°F	2400/950/950
Reactor thermal power, MW	2450
Electrical power, MWe gross	1034
Electrical power, MWe net	1008
Net plant efficiency, %	41.2

4. Nuclear Design and Safety

4.1. Reference Design Criteria

The design considerations and criteria leading to the development of the reference design are discussed in this section. The criteria that determined the reference design are as follows:

1. *General*
 - a. The core must produce power safely, economically, and reliably.
 - b. Economy has precedence over doubling time.
2. *Safety Oriented*
 - a. Total voiding is prevented by engineered safeguards.
 - b. Simultaneous flow blockage of more than one fuel assembly is not credible.
 - c. The prompt power coefficient is negative in all parts of the core.
 - d. No credible reactivity insertion other than flow blockage can cause core damage. Core damage due to flow blockage is limited to the voided assembly.

4.2. Core Description

The active core geometry approximates a right circular cylinder with an L/D of 0.288 and a volume of 6516 liters. Power flattening in the radial direction is accomplished by three zones of fissile enrichment of approximately equal volume. Zone boundaries are shown in Fig. 5. Core dimensions and volume fractions are summarized in Table 3.

4.3. Design Evolution

A trade-off study comparing spoiled and unspoiled geometries was completed in Task II; it indicated that the penalties associated with extreme spoiling are severe enough to discourage any attempt to

achieve a zero, or negative, sodium-void coefficient. It was largely for this reason that the annular geometry chosen for the preliminary reference design was dropped in favor of cylindrical geometry. With this change in philosophy, the important criteria therefore became the voided worth of a specified portion of the core and the prompt power coefficient.

Let us consider the conditions that can lead to voiding and the design features that are intended to prevent their occurrence. The potential causes of voiding are draining, total flow blockage, and boiling.

The pot arrangement prevents draining of the core. Even if the reactor vessel is ruptured, the core will remain covered. Furthermore, the vessel cannot be siphoned dry. Total flow blockage is equally unlikely, since there are six independent primary pumps. In the event that a single pump is blocked, the Doppler and radial expansion reactivity feedbacks terminate the excursion.

Boiling during an abnormal power transient is precluded by preventing the transient. Reactivity insertions large enough to tax the safety system are prevented. The safety instrumentation, e.g., a thermocouple at the outlet of each fuel assembly, and neutron detectors provide means for the safety system to prevent boiling.

Although the design provides means to prevent the blockage of flow to an assembly by a flat object, the possibility of at least partial blockage by a properly shaped object is acknowledged. Solid cans surrounding each fuel assembly prevent propagation of the void that may follow flow blockage.

The total length of fuel pin is dictated by the fuel's maximum linear heat rate, power peaking factors, the overpower to melting, and an allowance for calculational uncertainty. We used the following values for these parameters:

TABLE 3. PHYSICAL PARAMETERS OF CORE

	Core	Axial Blanket	Radial Blanket
Active length, in.	34.73	14	42.73
Effective diameter, in.	120.74	120.74	144.93
L/D ratio	0.288	—	—
Volume, liters	6516	2873	5253
Pin diameter/cladding thickness	0.280/0.010	0.280/0.010	0.608/0.017
Fuel density, % theoretical	85	85	90
Volume fraction			
Fuel	0.442	0.442	0.643
Steel	0.169	0.169	0.163
Sodium	0.354	0.354	0.195
Control	0.035	0.035	—

Estimated maximum peak/average power	1.6
Overpower to control melting, % of full power	111
Uncertainties from manufacturing tolerances	1.10
Total reactor power, MWt	2450
Fraction of power in core, end-of-cycle	0.92
Maximum linear heat rate at estimated peak burnup of 150,000 MWd/T, kW/ft	16

The resulting total length of pin is 276,000 ft.

Analyses of preliminary cores had indicated that the control requirements would range from 18 to 22 dollars. For safety and power distribution, it is desirable to have a maximum control-rod worth of less than 1 dollar. Unfortunately, it is not possible to vary the total number of control rods continuously and still maintain a uniform pattern. The magic numbers

the range of interest are 19, 25, and 31. Based on the projected control requirements, 25 rods were chosen.

Similarly, the number of assemblies making up a core cannot vary continuously when subject to the constraints that the total length of fuel pin be constant, that the core be symmetric, and that the control-rod positions be accommodated. Therefore, the core was required to have at least 10 rows of fuel assemblies, or a total of 246 assemblies and 25 control rods. With this arrangement, the active core length is more than 40 in. If another row were added, then the number of assemblies would increase to 306 and the active core length would decrease by 8 in. In addition, the extra assemblies would increase the vessel diameter by more than 1 ft. As a compromise, three assemblies were removed from each corner of the hexagonal core to provide a total of 288 assemblies. The diameter of the vessel was not appreciably larger than that required for the 246-assembly core, yet the active length was nearly 6 in. shorter. The shorter length results in improved voiding characteristics, and improved thermal and hydraulic performance. A shorter control-rod stroke, coupled with the shorter core, allows the use of a shorter reactor vessel.

The diameter of the reference pin is 0.280 in. and the pitch is 0.337 in. This combination was chosen with the aid of the parametric code NAPS.⁽¹⁾ Figure 6 shows relative fuel costs for different combinations of pin diameter and pitch as indicated by the code. The reference pin is larger than those usually encountered in LMFBR designs. We feel that this is primarily due to the unit fabrication cost (\$/kg) variation with pin diameter used in B&W's fuel-cost methods. The effect of a constant unit fabrication cost, with all other economic factors unchanged, is also given in Fig. 6. The combination of 0.250-in. pin diameter and 0.300-in pitch is clearly better than others. The way that the out-of-pile fraction is defined in the fuel-cost component for plutonium carrying charge can also affect the choice of fuel-pin diameter.

4.4. Physics Design

The following core physics results were generated with B&W's DHRG-SIZLE fast reactor design code package.⁽²⁾ Fuel-management studies were performed with PDQ-06.⁽³⁾

Important cross-section sources may be summarized as follows:

1. ^{239}Pu —ENDF/B evaluation by Greebler;⁽⁴⁾
2. ^{238}U —ENDF/B evaluation by Wittkopf, Roy, and Livolsi;⁽⁵⁾

FIG. 6. Optimization of Fuel-pin Diameter.

3. ^{240}Pu —ENDF/B evaluation by Pitterle;⁽⁶⁾
4. ^{241}Pu —Evaluation by Drake;⁽⁷⁾
5. Fe, Ni, Cr—ANL MC² calculation—spectrum corresponds to that of sodium-cooled, oxide commercial breeder.
6. Na, O —ORNL GAM-I Library Listing (1965).

Cross sections for ^{238}U and ^{239}Pu in the resolved and unresolved resonance regions were prepared by the ERIC-2 code.⁽⁸⁾ The inelastic matrix for ^{238}U was prepared by using the ENDF/B discrete level and continuum data;⁽⁹⁾ matrices for the remaining materials were taken from an ORNL GAM-I library listing (1965), normalized to more recent total inelastic cross-section data. Using these methods, key parameters of Assembly 48 of ZPR-3 agree very well with the values given by Greebler.⁽¹⁰⁾

The isotopic composition of feed plutonium was as follows:

^{239}Pu	67%
^{240}Pu	26%
^{241}Pu	5%
^{242}Pu	2%

The feed uranium was depleted to 0.3 wt % ^{235}U .

Key results are summarized as follows:

FIG. 7. Assembly Burnup at End of Cycle IV (MWd/T $\times 10^3$).

Avg. fissile enrichment of feed, wt % ^{239}Pu , ^{241}Pu	11.6
Midcycle fissile loading, kg ^{239}Pu , ^{241}Pu	3315
Integrated breeding ratio	1.36
Core breeding ratio	0.93
Doppler effect, $-T(dk/dT)$	0.0047
Sodium-void coefficient, $\rho(dk/d\rho)$	0.0268
Effective delay fraction, β_{eff}	0.0036
Lifetime (l^*), μsec	0.290

4.5. Fuel Management

The reference equilibrium scheme for fuel management calls for the removal of one-third of the core and near radial blanket assemblies and one-sixth of the far radial blanket assemblies at the end of each cycle. Thus, a total of 130 core and blanket assemblies are removed on an approximately annual basis with an average core burnup of 100,000 MWd/T. Each assembly remains in its initial position throughout its residence time in the core. The three fuel batches that are in the core at any given time are located so that no two assemblies in the same

batch are touching. As a result, there is no concentration of assemblies at any one burnup. The refueling scheme is shown schematically in the following sketch:

In-place refueling requires movement of fewer assemblies and reduced refueling downtime accordingly. Additional advantages include the reduced handling and an accompanying reduced probability fuel-assembly damage, and a reduced likelihood misloading a fuel assembly.

Alternative refueling schemes, such as shuffling and

the rotation of fuel assemblies within zones, may possibly allow more uniform burnup but require more downtime for refueling, since in the limit all 426 core and blanket assemblies might be moved at the end of each cycle. In the reference cycle only 130 assemblies are moved. Thus, the benefits gained from more uniform burnup must be weighed against the reduction in plant-capacity factor that might result from the increased downtime for refueling.

Typical assembly burnups at the end of an equilibrium cycle are shown in Fig. 7. For the discharge batch shown here, the burnups range from 117.8 MWd/T in the inner zone to 67.5 MWd/T in the outer zone; thus, the maximum is 75% greater than the minimum. The maximum-to-average burnup ratio, including the axial peak, is 1.4. Because of the steep flux gradient in the outer zone, the peak burnup in that zone is nearly as great as the maximum burnup, even though the average burnup is considerably lower. Hence, the average burnup in the

outer zone cannot be raised simply by raising the zone enrichment and shifting power.

Power peaking factors at the beginning and end of the same cycle are shown in Figures 8 and 9. In Fig. 8 the beginning-of-cycle peaking factors with three rods in position 53 fully inserted are shown to be 1.17 in zone 1, 1.18 in zone 2, and 1.21 in zone 3, including local peaking. At the end of the cycle with all rods out (see Fig. 9) the corresponding peaks are 1.30, 1.13, and 1.11, including local peaking. Because of the power shift to the axial and radial blankets during the cycle, the kW/ft indicated by the peaking factor of 1.30 is actually lower than that experienced at the beginning of the cycle.

The effect of an alternative control-rod pattern (six control rods in position 56 and one in position 52) at the beginning of the reference cycle is shown in Fig. 10. Despite the increased worth of the seven rods, the peak is slightly lower than that shown in Fig. 8 for three rods. The seven rods would be par-

FIG. 8. Assembly Peaking at Beginning of Cycle IV ($k_{eff} = 0.995$).

FIG. 9. Assembly Peaking at End of Cycle IV ($k_{eff} = 0.992$).

tially inserted because their total worth exceeds the required amount. Partial rod insertion has relatively little effect on the axial power-peaking factor. A group of rods with a combined worth of 0.016 in Δk increases the axial peak less than 1%.

4.6. Fuel-cycle Costs and Doubling Time

Fuel-cycle costs and the compound-interest doubling time based on the reference scheme of fuel management at equilibrium are presented in Table 4. Economic and operational parameters which influence the costs are given in Table 5.

4.7. Effect of ^{239}Pu α on Reference Design

A study to determine the effects of the uncertainty in ^{239}Pu α was undertaken using GE-BA503 data,⁽¹¹⁾ which are approximately midway between the ENDF/B recommendations and the United Kingdom's preliminary liquid-scintillator data. The results compared with the values obtained using the ENDF/B α are as follows:

Parameter	ENDF/B	GE-BA503 ¹¹	% Change
Initial fissile loading, kg/batch	923	954	3.4
Discharge, kg/batch	1240	1251	0.9
Initial breeding ratio	1.44	1.39	3.5
Final breeding ratio	1.26	1.26	0
Integrated breeding ratio	1.36	1.33	-2.2
Doppler effect, $-T(dk/dT)^{(a)}$	0.0058	0.0053	-8.6
Sodium-void coefficient, $\rho(dk/d\rho)$	0.022	0.025	13.6
Fuel cost, mills/kWh	0.755	0.789	4.7
Doubling time, yr	10.4	11.5	10.0

(a) The proper relationship is $-T^{1.13}(dk/dT) = 0.016$.

Predictably, the higher values of α degrade the performance of the reference core in all respects. In this study the increase in α was accomplished by increasing σ_c and decreasing σ_f so that σ_a remained the same. If the same change in α is accommodated by changing only σ_c , i.e., by increasing σ_a , then the

FIG. 10. Assembly Peaking at Beginning of Cycle IV (Rods 52 and 56 Inserted).

penalty in fissile loading is reduced to 1.4% while the penalty in integrated breeding ratio increases to 3%.

4.8. Analyses of Credible Accident

The accident studies for an LMFBR safety analysis generally fall into two categories. The first includes accidents that are considered credible although unlikely. A variety of such accidents were analyzed, including the voiding of a subassembly. In this calculation the reactivity insertion was increased by a factor of seven for added conservatism. The reactivity of 0.0027 δk was assumed to be inserted in 0.01 sec. The fuel maximum temperature transient following the accident is shown in Fig. 11. At no time does the fuel temperature come within 800°F of melting. The safety-system delay is assumed to be 100 msec, at which time the rods worth 0.03 in δk move in at 50 cm/sec.

The effect of a double-ended break in the pipe between a primary pump and the core vessel was

also studied. This is thought to be the most serious accident involving reactor flow. Even without a scram, the power is very quickly reduced by the Doppler and radial expansion reactivity insertions. At 6 sec the power is 90% of the original value. The fuel surface and sodium temperature transients are shown in Fig. 12. Once again, the temperatures are within acceptable limits. With protective action, the maximum temperatures would, of course, be much smaller.

The effect of an arbitrary \$2/sec ramp was also investigated using the scram rate used in the analysis of partial voiding (see Fig. 13). Again, the safety system was capable of shutting down the reactor long before fuel melting or sodium boiling occurred.

4.9. Design Basis Accident Analysis

The design for most of the plant is based on a chosen set of normal operating conditions, along with the conditions existing during shutdown and the conditions existing along the chosen path between shutdown and normal operating conditions. However, for

certain aspects of the design the design basis is the desire to provide for the safety of the public and the plant personnel during and following severe accidents. For this reason, the design requirements for the reactor cavity, including the cover structure and the reactor building, are set by the requirement that the containment withstand any energy released as a result of a nuclear burst, energy transfer from fuel to coolant, and the decay of fission and activation products. The design requirements must contain significant margins for conservatism.

We shall discuss in some detail the nature of several severe accidents and their effects on the plant and the environment. It is seldom possible to define satisfactorily a series of initiating events for accidents of this nature. In the discussions that follow, no attempt is made to establish the events leading to the nuclear excursion; however, in some instances it will be pointed out rather clearly that the normally considered design basis accident for fast oxide reactors cannot be attained. It is included here to establish that the public and the plant personnel are protected even for this accident, since it is realized that the safety requirements are often legislated by regulatory bodies and need not be possible in nature.

The discussion considers the analysis of the energy-release mechanisms that establish the design of the reactor cavity, whose boundary forms the energy-absorbing barrier and the sodium barrier. Since there are a number of seals for components and plugs in the reactor cover structure, the reactor-cavity barrier must be considered as the energy barrier rather than the plutonium, fission product, and activation product barrier. Thus, the reactor building becomes the containment barrier for these radioactive materials.

TABLE 4. FUEL COST FOR EQUILIBRIUM CYCLE

Component of Fuel Cost	Fuel Cost, mills/kWh
Fabrication	0.334
Reprocessing and reversion	0.166
Shipping	0.038
Plutonium carrying charge	
In-pile	0.432
Out-of-pile	0.114
Total	0.546
Fabrication carrying charge	0.061
Reprocessing carrying charge	-0.042
Plutonium credit	-0.348
Total	0.755
Doubling time, yr	10.4

TABLE 5. FUEL-COST PARAMETERS

Economic Parameters	
Plutonium value, \$/g	10
Plutonium carrying charge, %/yr	10
Reprocessing plant, \$/day	30,000
Unit conversion, \$/kg	5.60
Unit shipping costs, \$/kg	
Core + axial blanket	20
Radial blanket	10
Unit fabrication costs, \$/kg	
Core	270
Axial + radial blankets	67
Interest during fabrication, %/yr	6
In-pile Times	
Cycle time, EFPD	350
Fuel-batch residence time, EFPD	1050
Plant-availability factor	0.90
Downtime for refueling and maintenance, days/cycle	14
Plant-capacity factor	0.86
Cycle time, calendar yr	1.11
Fuel-batch residence, calendar yr	3.33
Out-of-pile Times/Batch	
Fabrication, yr	0.250
On-site storage fresh fuel, yr	0.083
Spent-fuel storage, yr	0.192
Spent-fuel shipping, yr	0.219
Reprocessing time, yr	0.119
Total, yr	0.863
Miscellaneous	
Fabrication losses, %	1
Reprocessing losses, %	1
Feed uranium to core and blankets, wt % ²³⁵ U	0.3

4.9.1. Energy Production for Large Reactivity Insertions

The energy release to the coolant following the melting of a significant segment of the core is believed to be the most realistic evaluation for the requirements of the energy and radioactivity barriers. Since no source for a large, positive reactivity insertion could be determined analytically, a ramp of approximately \$60/sec was assumed as the reactivity driving function, starting from steady state at full power. The FORE II code⁽¹²⁾ was used as the calculational tool for the resulting nuclear excursion and for the analysis of the core thermal condition at the time of the release of molten fuel to the coolant. The clad failure was assumed to occur

when the clad was contacted with molten fuel. During the nuclear excursion before failure of the cladding, the stored energy of the core increased approximately 15,000 MW-sec. At this point in time, approximately 14% of the fuel in the core was molten and was released to the coolant associated with this pin length.

The nuclear excursion was stopped when the cladding failed, and a hydrodynamic calculation was made, assuming that all of the molten fuel was released instantaneously to the sodium in the coolant channels associated with the molten fuel. The pressure generated by the sodium coolant owing to the release of the molten fuel was relieved upward and caused the sodium above the core to be propelled toward the cover structure. The maximum pressure on the cover structure was approximately 100 psig.

The \$60/sec ramp was used to establish an upper limit for the fraction of the core that could be molten and released to the coolant. At \$60/sec, the time response of the power level is so fast that the thermal-energy transfer from the fuel through the clad to the coolant is very small. Thus, it would appear to form an upper limit to any processes of reactivity insertion for a core filled with sodium. If the rate of reactivity insertion is decreased significantly, then the percentage of the core that is molten when pin failure occurs is reduced as the heat transfer becomes more significant. At about \$4/sec to \$5/sec, the protection system will be able to maintain the integrity of the fuel, but some central

FIG. 11. Maximum Fuel Temperature vs Time for Void Accident Involving Seven Assemblies.

melting of the peak pins will occur. It should be emphasized that no credible accident has been found that even approaches this rate for a period of time long enough for the total reactivity insertion to ap-

FIG. 12. Temperature Transient for Double-ended Break.

FIG. 13. Maximum Fuel and Coolant Temperatures vs Time for \$2/sec Ramp and Normal Scram.

proach \$1. Hence, if one considered only those accidents that are credible but of low probability, the design requirements of our reference concept would be fixed by the normal requirements for reactor vessel design. However, the design requirements of the cover structure for the reference design concept have been conservatively set to contain the limiting energy release.

4.9.2. Nuclear Excursion for Drained Core

In the analysis of fast reactors for licensing, one type of accident has always been considered when establishing the design requirements for containment structures. This accident has its origin in the following facts: (1) the core fuel is not assembled in its most reactive arrangement; (2) criticality can be achieved without any coolant in the core; (3) the reactor core has a negative coolant-density coefficient. The design of the energy barrier requires a prediction of the magnitude of the energy released in an accident during which the core is disassembled owing to the vapor pressure of the fuel. In the following analyses and discussions of this type of accident, it is assumed that the coolant has been permanently removed from the core while the reactor is at full power.

A simple, one-dimensional, diffusion-theory calculation was performed to determine the criticality status of the reference core before and after collapse. For this calculation, it was assumed that the fuel began melting and ran into the cooler lower axial blanket where, after completely filling the vacant sodium spaces, it froze. The remainder of the molten fuel collected in a pool above the lower axial blanket. When melting was complete, the upper axial blanket collapsed as a unit onto the surface of the pool of molten fuel. It was further assumed that core melting was complete and that the radial blanket remained intact. To obtain an upper limit for the multiplication of the final configuration, zero radial buckling was used in the axial calculation. All molten fuel was compacted to 100% of theoretical density, and no control material was present in the final configuration. The results of the calculation are as follows:

Initial k	1.025
Final k , max	1.054
δk	0.029

Approximately \$8 of reactivity is added as a result of the collapse of the drained core. If it is assumed that the core materials accelerate at 1 g to final configuration, then the rate of reactivity insertion is about \$40/sec. The insertion rate does not take into account the effect of any resisting force on the spatial dependency of the melting processes; hence, it is very conservative.

The probability of this type of accident occurring in the reference design is extremely small owing to the nature of the design, i.e., the fuel pins are supported from the top. Should failure of the cladding occur because of melting, the lower part of the core would fall away from the upper part.

4.9.3. Energy Release from Disassembly

In a reactor meltdown accident involving the formation of a secondary critical assembly, we can postulate a sequence of events that leads to explosive disassembly of the core. The disassembly of the core is caused by high pressures developed in the fuel during the course of the power excursion initiated when the fuel moves from its original configuration to one of higher reactivity. It is usually assumed, in the analysis of such accidents, that the reactivity rises above prompt critical and causes a power excursion to develop. The disassembly pressures remain small until the temperature becomes high enough to produce high saturated vapor pressures in the fuel. By the time the disassembly pressures be-

Some significant, the reactor is usually on an extremely short exponential period, and disassembly occurs with potentially large energy releases. In order to predict the magnitude of the energy generated as a result of a meltdown accident, modified Bethe-Tait calculations were performed using the WEAK EXPLOSIONS code.⁽¹³⁾ Since the only factor (other than fuel movement) operating to limit the power at which disassembly occurs is the Doppler coefficient, this coefficient is of central importance in the meltdown analysis. The reactor was assumed to be operating at full power when the excursion was initiated; Doppler feedback began at that point. The sensitivity of the energy release to the magnitude of the Doppler coefficient was investigated for a broad range of values. The energy release for both the full-dense and void Doppler coefficient is in the range of 3 to 3.5×10^3 MW-sec.

4.9.4. Sodium Fires

In reactors that use sodium as a coolant, it is necessary to examine the influence of sodium on the design of containment structures. Although the energy boundary is designed to prevent primary sodium release to the reactor building, and all intermediate loops are doubly contained within the reactor building, we must examine the effects of sodium fires to determine whether the containment structure could be jeopardized and radioactive material could be released to the environment. Such fires constitute the greatest threat to personnel, plant, and environment.

Two types of fires are considered: reactor vessel fires and containment fires. Reactor vessel fires are confined to the reactor vessel; oxygen is admitted to the vessel through breaches in the cover structure. Heat transfer to the atmosphere and structure is primarily by convection. The fire continues until the containment oxygen is consumed. If the entire movable plug is removed, the maximum containment temperature and pressure are 550°F and 12 psig.

The model used in the reactor building fire assumed that the entire cover structure was covered with a pool of burning sodium and that 500 lb of sodium were sprayed into the air. Under these conditions the maximum temperature and pressure are 553°F and 14 psig.

These analyses show that, even though some small sodium fires may be permitted in the reactor building, the design requirement to retain the plugs was a good choice. The loss of even the smallest plug in the cover structure raised the containment temperature approximately 100 degrees.

5. Plant Thermal and Hydraulic System Design

5.1. General

In arriving at the reference design previously described, a great many aspects were examined that influenced the choice of the final reference plant. The most influential of these were safety and economics.

This portion of the presentation describes the final configuration of the heat-transport and energy-conversion systems, and highlights the technical and economic factors that led to this design. We shall cover the important features of, and the interactions between, the reactor coolant, intermediate coolant, steam-generating, and energy-conversion systems.

The major portion of the effort required to arrive at an economically optimum plant was directed toward determining the plant's functional parameters and configuration. Both efforts were carried out simultaneously.

5.2. Plant Configuration

The extensive matrix of technically feasible reactor coolant and intermediate loop configurations dictated a trade-off study to provide supplementary data for design decisions. National LMFBR program ground rules, economic experience with power plants, and projected test-facility capabilities narrowed the number of possible configurations. Because the cost of power generation is a major parameter in the design of power plants, and is the most tangible of all the factors, a great deal of emphasis was placed on this parameter in anticipation that it would decisively guide us in the right direction. Unfortunately, we were only partially successful and were forced to make decisions based on qualitative reasoning.

A survey of the number of possible loop combinations indicated that only a limited number could be examined in detail. For reasons previously mentioned, for relevancy to the desired objective and as a matter of practical expenditure of effort, we fixed this number at six, tabulated as follows:

Configuration	No. of Reactor Coolant Loops	No. of Inter-coolant Loops	No. of IHXs	No. of Inner Pumps	No. of SGs	No. of RH
1	3	3	3	3	3	3
2	3	3	6	3	3	3
3	3	3	6	6	3	3
4	6	3	6	3	3	3
5	6	3	6	6	3	3
6	6	6	6	6	6	6

All configurations have reheaters, since the inclusion

or exclusion of reheaters does not affect the number of loops. Configuration 1 was chosen to determine the effect on the reactor vessel system of including three pumps and three heat exchangers as opposed to configurations 4, 5, and 6, which contain six pumps and six heat exchangers. Configuration 2 is a modification of 1 relative to the reactor vessel system. Configuration 3 differs from 2 in that the intermediate system contains two intermediate pumps per loop instead of one. Configurations 4, 5, and 6 vary in the number of intermediate loops, the number of intermediate pumps per loop, and the number of steam generators and reheaters per plant. These configurations were evaluated on an economic basis to determine whether a major advantage existed among the candidates.

Configuration 6 was eliminated from further consideration because it was significantly more expensive than the others. The process of selecting configuration 4 for the reference design became one of qualitative elimination of the remaining five configurations.

The advantages attributed to the reference design for the heat-transport system are as follows:

1. The reactor-vessel diameter is reduced because the diameter of the intermediate heat exchanger is less for the six-IHX design than for the three-IHX design. The diameter of the vessel is important because it affects the diameter and thickness of the cover structure.
2. The inventory of reactor coolant sodium is reduced because of the smaller diameter of the reactor vessel made possible by the six-IHX configuration.
3. The partial output of the plant will be larger for the case in which one reactor coolant pump fails.
4. Intermediate piping to the heat exchangers is smaller for the six-IHX combination than for the three-IHX combination.
5. The smaller number of pumps and loops simplifies control of the intermediate system.
6. The capability for full plant capacity is improved with only one pump per intermediate loop because the control scheme does not permit load variations between on-line loops. One large pump is more reliable than are two half-capacity pumps.

5.3. Plant Functional Parameters

The selection of plant functional parameters depends on evaluating a complex series of interacting systems, starting with the heat source and terminating with the energy output of the plant. It depends not only on plant size but on the type of plant, i.e., base loaded or peaking, on the economics of the systems and components, on the expected reliability,

on the expected lifetime, and on the fuel cost. These are but a few of the major considerations.

Since the purpose of the reactor and its accompanying systems is to produce power at an economically competitive cost, it is only logical to initiate the design with the requirements of the energy-conversion systems and to work backwards to determine the requirements of the reactor. At the outset of this study, we chose to examine the seven steam cycles that were produced by the United Engineers and Constructors study of 1964 and 1966. They produced complete cost estimates and heat balances for the following operating conditions:

3500 psig/1000°F/1000°F
 2400 psig/1000°F/1000°F
 2400 psig/950°F/950°F
 2400 psig/950°F/NRH
 1800 psig/900°F/900°F
 1450 psig/1000°F/NRH
 1000 psig/Dry and Saturated

The important variables, such as flow, pressure, temperature, and economics, were programmed into a computer code, together with the mathematical models depicting the economic and performance characteristics of all the major components in the plant, including the core. By introducing independent variables, such as reactor outlet temperatures, reactor temperature differences, IHX temperature differences, and steam cycles, the program was able to compute fuel costs, fixed charges, OM&I costs, and total costs of power generation.

Of major concern during this portion of the study were the effects of temperature on the material of construction for the steam generators and reheaters. In the initial stages of the study, a once-through, helical-coil steam generator was modeled as a bimetallic unit, i.e., it contained a stainless steel superheater and a Croloy 2¼ boiler bundle. This design permitted a considerably higher inlet sodium temperature than would an all-Croloy bundle. However, subsequent economic evaluations indicated that this design was considerably more expensive than the single material bundle. This conclusion led to the adoption of an all-Croloy steam generator, which limited the sodium inlet temperature (on the basis of economics) to approximately 1000 to 1025°F.

The sodium-heated reheaters considered in this study are also once-through, helical-coil units and are located in parallel with the steam generators. Because of the parallel arrangement of the once-through reheaters with the once-through steam generators and the comparatively small overall heat-transfer coefficient for the reheaters, equal temperature differences

on the sodium side of both units would have resulted in economically unacceptable reheaters. It was therefore decided to raise the sodium outlet temperature of the reheater at the expense of the steam generator (thereby increasing the LMTD of the reheater) until the sum of the costs of the reheater and the steam generator reached a minimum. The minimum cost summation of the steam generator and the reheater directly influences the fixed charges; therefore, holding everything else constant, the minimum cost will be reflected in the sum of the fixed charges and the fuel cost. Figure 14 is a typical plot.

The determination of the operating conditions for the intermediate heat exchangers involved a trade-off that affected the reactor vessel geometry, the temperature differences at hot and cold ends, and the total costs of power generation. These intermediate heat exchangers were Fermi-type, Type 304 stainless steel units, and were sized and costed on this basis. Although it is recognized that there are design differences between this type of heat exchanger and one that would eventually be used in a pot design, it is believed that such differences, as related to cost, would cancel each other.

In this design, sodium is on the shell side and intermediate sodium is on the tube side. The physical size of the reactor vessel is dependent upon the total IHX length, the bundle OD, and the head of primary sodium required to drive the sodium through the shell side of the heat exchanger.

In an effort to determine the relationship between the OD, pitch, bundle diameter, sum of the bundle height and static head, and costs, we made an independent study of 54 heat exchangers at constant heat load, constant reactor coolant and intermediate flow, and constant LMTD. The results indicated that a configuration with a $\frac{5}{8}$ -in.-OD tube on a $\frac{7}{8}$ -in. pitch did not cost too much and did not penalize the height of the vessel.

Generally, the diameter of the heat exchanger is directly related to the diameter of the reactor vessel, and hence to the reactor cover plug. Therefore, we should be able to reduce the diameter of the cover plug without unduly penalizing the height of the vessel. As a means of arriving at a final reference configuration for the heat exchanger, we elected to reasonably minimize the diameter of the IHXs using engineering judgment to determine the accompanying penalties to the reactor vessel system.

Figure 15 is a plot of the IHX shroud diameter for the reference plant conditions, showing its relation to the sum of the primary-sodium pressure drop and the effective length of the heat-exchanger tubes. This summation is labeled "minimum reactor pot height."

FIG. 14. Reheater Optimization.

The design point has a minimum reactor pot height of 29.7 ft and a shroud diameter of 72 in.

The variation in shroud diameter occurs when the IHX is designed to varying allowable tube-side pressure drops. As the pressure drop on the tube side is allowed to rise (for a constant pitch), the overall heat-transfer coefficient increases, the number of tubes decreases, and consequently the shroud diameter decreases.

To determine the minimum costs of power generation as a function of the IHX LMTD, many cases were run and analyzed, varying the ΔT_M for both equal and unequal hot- and cold-end temperature differences. Figure 16 is a typical plot of the results for the reference steam cycle, reactor outlet temperature, and reactor ΔT . From the plot it can be seen that the minimum cost of power generation appears at a ΔT_M of approximately 100°F. Also, the total costs of power generation are more sensitive to the LMTD than they are to the hot- and cold-end temperature differences.

5.4. Selection of Steam Cycle

As stated earlier, seven steam cycles were investigated using the computer code developed for the LMFBR Follow-on Study. With each steam cycle, reactor outlet temperatures ranging from 1200 down to 800°F were inserted into the code together with reactor temperature differentials ranging from 500 down to 200°F. The combinations of temperatures were arranged so as to be compatible with the steam and feedwater conditions. The hot- and cold-end temperature differences for the intermediate heat exchanger were kept constant at 75 and 130°F, respectively. For each reheat cycle the outlet sodium tem-

FIG. 15. Effect of IHX Tube Pitch on Shroud ID and Minimum Height of Reactor Pot.

perature was optimized for each combination of the reactor temperature conditions. For all combinations of reactor temperature, fuel costs, fixed charges, and total costs of power generation were calculated. For each set of conditions the maximum cladding temperature in the core was examined.

A maximum clad temperature of 1300°F was set as reasonably attainable for the stainless steel-clad fuel pins. A sodium temperature limit of 1025°F maximum was also set for the steam generators and reheaters. With these limits, power-generation costs appeared to favor the 2400/950/950 steam cycle when accompanied by an 1100°F reactor outlet temperature and an 800°F reactor inlet temperature. The choice of a reheat cycle was made solely on the basis of economics, and did not include such factors as plant complexity and reliability, because of their elusive nature. By going to a reheat cycle, it was estimated that 0.034 mill/kWh would be gained in fixed charges and 0.043 mill/kWh would be gained in fuel costs. Neither of these values by themselves would be significant, but together they merit atten-

tion. In any event, further evaluation of the reheat cycle is required to substantiate this choice.

Subsequent studies on the IHX already discussed set the hot- and cold-end temperature differences at 100°F. Figure 17 is a plot of the reference plant conditions.

5.5. Economic Performance

Throughout our discussion, power-generation costs have been emphasized as an item of major importance. However, it is subject to fairly large uncertainties of this stage of LMFBR development.

The bases and procedures used in establishing the cost estimate for energy generation are, to a large extent, those proposed by ANL as economic ground rules. The principal economic factors excluding fuel-cost factors can be summarized as

Fixed capital charges on plant capital, %/year	13
Present worth discount rate, %/year	6
Indirect cost, %	40
Annual plant factor	0.86

The energy-generation cost for the reference 1000-MWe LMFBR central-station plant is summarized below:

ENERGY-GENERATION COST	
	Mills/kWh
Fixed charges	2.658
Fuel cost	0.755
Operation and maintenance cost	0.302
Total	3.715

FIG. 16. Effect of IHX ΔT_M on Total Cost of Power Generation.

This cost of 3.715 mills/kWh reflects the 86% annual plant-capacity factor and the 1000-MWe net electrical-generating capacity established for the reference design.

The largest portion of the total cost of power generation is the fixed charges on the plant investment, which constitute 2.658 mills/kWh, or approximately three-fourths of the total. This portion reflects the total direct construction cost of \$110.652 million (exclusive of land) and an indirect cost of 40%.

This cost is based on a plant erected on the standard AEC site on the east bank of the North River in western Massachusetts, as described in TID-7025, *Guide to Nuclear Power Cost Evaluation*. The cost covers all material and equipment purchased, including delivery, and the labor for installation of the material and equipment. The material and equipment costs are based on manufacturers' quotations and the delivery costs of equipment purchased for plants now under construction. All prices for equipment and material are based on June 1967 data, and no allowances were made for research and development or engineering above those normally associated with the purchase of fully developed pieces of equipment.

Labor costs are based on the wage scale of June 1967, and the labor efficiency and manufacturing productivity experienced on current nuclear power-plant projects. All labor is for a 40-hr week with no premium paid for overtime or transportation.

The direct costs do not contain such items as architect-engineer's services, insurance, interest, purchaser's administrative and general overhead, property taxes, personnel training, erection of contractor's offices or miscellaneous site construction, contingency on plant equipment and labor, nuclear-engineering services, startup costs, or land and land rights. To incorporate these costs, the indirect cost was estimated by taking 40% of the direct costs, excluding land. After including the indirect cost and \$360,000 for land and land rights, the total capital cost estimate is \$155.28 million, or \$154/kW net.

The annual fixed charge rate, which includes depreciation, taxes, insurance, and return on investment, is set at 13%. With this rate and a reference plant-capacity factor of 0.86, the unit capital cost is 2.658 mills/kWh.

The staff requirement for routine operation of the reactor has been assessed at 85 persons with a total annual salary of \$706,300. Allowing for a 20% fringe benefit and cost factor, the payroll cost is estimated at \$847,500 per year.

Material and maintenance costs are estimated to be \$1.1 million. The insurance premiums are \$0.260 million for nuclear liability, and a fee of 30 times

FIG. 17. Effect of ΔT_R on Total Cost of Power Generation.

the reactor thermal power (2450 MWt) for government indemnification, giving a total of \$0.335 million per year. The total operating, maintenance, and insurance cost is therefore \$2.283 million, and the unit cost is 0.302 mill/kWh.

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Discussion

Mr. Wyckoff (ComEd): Your plant cost is lower than the numbers we have been hearing. Did you reference that against the water-reactor costs that you are now charging? (Laughter)

Mr. Croft: We didn't reference it against our own PWR cost. However, we did use the information available from our PWR plants in judging what the costs should be for the components in this plant. But this estimate was a complete estimate to arrive at the cost for the reference design.

Mr. Hutter (ANL): My questions pertain to the fuel-handling system. What is the size of the large rotating plug, and does your arrangement allow the removal of the storage basket? Secondly, you mentioned that the vented fuel makes the fuel-unloading system simpler; I presume you meant because of the shorter subassembly. How about the contamination provisions which have to be included in the fuel-unloading system because of the vented fuel?

Mr. Goulding: The largest plug size is approximately 35 to 37 ft. You asked then a question about whether one could take out the storage basket; the answer is effectively no. We have built this unit in. However, the mechanisms which we think are more prone to damage and more prone to maintenance are readily removable from the top cover structure. The vented fuel certainly gives problems regarding the contamination features and the sealing problems associated with the cover structure. However, in our preliminary work we found very little difference between the activation level due to continually venting fuel with a certain hold-up residence time in the fuel itself, compared to the sudden increase or the burst that one could get with a postulated failure condition. The short vent reduces our problems associated with the fuel handling and with the transference of fuel material off the site.

Mr. Harde (Interatom): My first question is: what evidence made you exclude the propagation of sodium ejection out of one channel as being incredible? As is well-known, there are, perhaps, quite high recondensation pressure surges. Secondly, did I understand correctly that you designed for a maximum design

basis accident involving a \$60/sec ramp? If so, what is the thermal-energy release and the loading of the top shield?

Mr. Hostetler: The fuel-assembly can is designed to withstand the normal pressure drop in the core. Admittedly, we have not looked at the chugging; this is probably what you are referring to. We do not believe that the initial expulsion is a problem because it is necessary to have an extremely high degree of superheat to get a large enough pressure pulse. The chugging is something I'll admit we have not looked at carefully.

Mr. Harde: There is evidence of recondensation pressure surges as high up as 30 atm, for instance.

Mr. Hostetler: This is certainly a point that we were aware of but had not really had time to evaluate.

Mr. Harde: My second question was related to the pressure loading of the top shield in the design basis accident.

Mr. Hostetler: We believe that the maximum pressure loading is due to the sodium, i.e., energy partition between the sodium and the fuel rather than the Bethe-Tait accident. This results from an extremely conservative calculation: all of the molten fuel at the time of clad failure is mixed intimately and instantaneously with the sodium. Using this model we calculated an approximate 100 psi loading on the top shield. That is rather high, but, as I say, we believe that this is an extremely conservative calculation and we don't know how to really calculate what it actually is.

Mr. Harde: How have you provided for transport of fuel elements from the core to the storage dump? It wasn't indicated on your drawing.

Mr. Goulding: I take it that you mean the transfer of the fuel from the core into the storage drums. This is done with our transfer machine, which is inserted in the smallest of the three eccentric plugs by the refueling machine; the machine is about 10 in. in diameter. In operation the machine grapples and lifts a fuel assembly clear of the core, the transfer is done by actuating the rotary plugs, and the fuel assembly is lowered into the storage drum. It is a push-pull type of action.

Mr. Wirtz (Karlsruhe): My question is also with respect to the vented fuel. Have you a model, and have you designed the vented fuel or not? In case you have, is it possible that the sodium enters the fuel and remains there for some time? May I remark that we have given much thought to venting fuel in a sodium-cooled reactor; so far we have found it to be very difficult. I might prophesy that any plan you have will be very difficult to perform.

Mr. Croft: We agree that major R&D will be

Required to develop the venting devices and to prove out the vented fuel. Our economic evaluation indicated at least a 0.15-mil/kWh economic incentive for the vented fuel. We are in the process of defining R&D programs which we believe would be necessary to prove out the vented fuel. We have no detailed design.

Mr. Golan (AI): I would like to pursue further this vented-fuel question, if I may. Would you elaborate on the basis for this 0.15-mil/kWh improvement? What are the factors that lead to that improvement?

Mr. Croft: Two of the major effects on the fuel-

cycle end of the cost are: (1) the thinner clad reduces the metal in the core (we have selected 10-mil clad), and (2) with the reduction of the plenum length, there is a lower pressure drop in the core; this reduces the required can thickness and again reduces the steel in the core. These two items are the major reasons for the fuel-cycle cost saving. There are other benefits from the fuel handling, particularly in the fuel shipment, because fuel can be shipped without the fuel pins pressurized. There are also some capital-cost savings in the projected size of the reactor vessel. Of course, there are some penalties also, due to the equipment necessary to handle the vented fuel.

SESSIONS ON LARGE FAST REACTOR DESIGN

SESSION LFRD-IV

Friday Afternoon, November 8, 1968

Chairman: K. P. Cohen
General Electric Co.

Secretary: J. P. Burelbach
Argonne National Laboratory

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Phenix: Status of the Design before Construction

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ABSTRACT

The construction of Phenix is beginning now. This paper relates the construction organization and schedule. The main performances of the reactor plant are briefly recalled, and the design modifications since last publications are reviewed.

Introduction

In the French program aimed at the development of the fast reactors, Phenix is a step between the experimental reactor Rapsodie and future large power reactors.

Rapsodie has yielded much valuable information in the field of sodium technology relating to realistic conditions of reactor operation. But its power, 24 MW(th), is low; the characteristics and size of its components are not of the scale of a power reactor, and there is no steam and electricity production.

As a transition to large reactor plants, which require heavy investments, the construction of Phenix, as a prototype of a 250-MW(e) reactor, was decided.

Phenix has been described previously.⁽¹⁻³⁾ This paper briefly recalls its objectives and main characteristics, and relates the most important recent modifications.

The design of the Phenix reactor plant was made from 1965 to 1968 under the responsibility of the Commissariat à l'Energie Atomique with the assistance of Electricité de France and some participation of private companies. The main and detailed characteristics were established during this stage.

Fabrication studies, construction, and commissioning of the plant are undertaken by a new staff specially formed for this work, including personnel from the Commissariat à l'Energie Atomique (CEA), Electricité de France (EDF), and Groupement Atomique Atlantique Alsacienne (GAAA). The Project Manager is from the CEA and his assistant from EDF. The staff is located at Saclay, and all personnel are integrated in this unique organization.

This new collaboration between two governmental organizations and a private company is intended to gather the knowledge gained by the members of the three organizations, to permit each to obtain experience with a fast-neutron reactor plant, and to assure a good continuity between design and construction. Also, several people who participated in the design work are now on the construction staff.

It is hoped that this flexible organization will be a useful example for solving the management problems of the future development of the fast breeder reactors.

Construction Schedule

The construction of Phenix is beginning now on a site situated close to the Marcoule Production Center. The end of this year will be devoted to protection of the site against the floods of the Rhône river. The reactor groundwork will start on the beginning of the next year.

During 1969, the fabrication of the main components will start, and during this time the buildings will be erected on the site.

The assembling of the reactor and loops, and their tests, will take place in 1970 and 1971.

The major part of the construction must be achieved in 1972 so that very important tests of the main parts of the plant can be run during about one year. The criticality and the power rise are envisaged for 1973.

Objectives

The objectives of the Phenix reactor have been given,^(2, 3) but will be briefly recalled. The main objective is to demonstrate that the present status of technology allows the construction and operation of power reactors without important difficulty. To that end, the operation of Phenix has to be safe and reliable.

But any type of power reactor is not worth developing if the energy produced is not competitive. This objective will not be fulfilled by Phenix, whose power is too small. However, data necessary to show the competitiveness of power reactors should be obtained from experience with Phenix. Therefore, financial investments for this reactor are to be as small as possible.

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TABLE 1. DESIGN PERFORMANCES

Heat output	563 MW	Pellet density	85% TD
Electrical power	250 MW	Fuel	95% TD
Fuel material	UO ₂ -PuO ₂	Blanket	430 W/cm
Blanket material	UO ₂	Fuel maximum specific power	193 W/oxide g
Total breeding gain	0.17		1811 W/oxide cm ³
Steam pressure (at turbine stop valve)	163 bars		
Steam temperature (at turbine stop valve)	510°C		
Core and Blanket		Reactor	
Fuel	UO ₂ -PuO ₂ sintered pellets	Tank material	SS-76 CND 17.13 (AISI 316)
Blanket	UO ₂ sintered pellets	Main tank	
Cladding material	Type AISI 316 SS	Height	11.2 m
Subassembly cross section	hexagonal	Diameter	12.8 m
Number of enrichment zones		Leakage tank	
Core	2	Height	11.4 m
Blanket	1	Diameter	13.0 m
Core volume	1156 l	Instrumentation	2 thermocouples/core subassembly and sodium boiling detection
Internal zone/total core volume	0.505		
Core length	850 mm	Upper neutron shielding	
Core diameter	1356 mm	Thickness	900 mm
Axial blanket thickness	300 mm	Materials	SS, graphite, B ₄ C
Radial blanket thickness	250 mm	Lateral neutron shielding	
Core composition, vol %		Thickness	1590 mm
Fuel	36.8	Materials	SS, graphite, SS and borated graphite
Circulating sodium	29.7		3.44 m
Thermal Performances		Rotating-plug diameter	
Core average power density (at equilibrium)	422 kW/l	Fuel Loading	
Core maximum power density (at equilibrium)	668 kW/l	Reactor temperature during fuel loading	250°C
Sodium temperature		Minimum expected burnup of the fuel	50,000 MWd/t every 2 months
Core inlet	400°C	Frequency	
Core outlet	560°C	Subassembly number/loading cycle	
Nominal pressure drop	5.4 bars	Core	15 to 17
		Blanket	8 to 11
Physical Performance		Number of subassembly locations in storages	
Critical mass (at equilibrium)	700 kg equivalent Pu-239	Inside reactor	41
		Outside reactor	120
Enrichment, %		Primary Sodium Cooling	
Internal zone	20.3	Pump number	3
External zone	27.4	Pump type	centrifugal mechanical pump
Theoretical doubling time	17 yr		
Breeding ratio		IHX	
Internal	0.54	Number	6
Total	1.16	Type	vertical straight-tube bundle
Reactivity worth of the control rods	$6,840 \times 10^{-5}$		950 t
Prompt-neutron lifetime	3.8×10^{-7} s	Sodium mass	
Doppler reactivity coefficient (at 1500°C)	0.33×10^{-5} (k/k)/°C	Secondary Sodium Cooling	
Total temperature coefficient	4.30×10^{-5} (L/k)/°C	Loop number	3
	1.30×10^{-5} (k/k)/°C	Pump number	one per loop
Total power coefficient		Pump type	centrifugal mechanical pump
Maximum neutron flux		Number of steam generators	3
Total	6.7×10^{15} n/cm ² -sec	Type of steam generator	modular and once-through
>0, 1 MeV	4.4×10^5 n/cm ² -sec	Module number	12/steam generator
		Steam-generator material	Austenitic and ferritic steel
Subassemblies and Control Rods		Sodium temperature	
Number of control rods	6	Inlet	350°C
Control-rod material	B ₄ C	Outlet	550°C
Control-rod-cladding material	AISI Type 316 SS		
Fuel pin		Turbine Plant	
Fuel diameter	5.5 mm	Number of feedwater pumps	3
Pin outside diameter	6.6 mm	Water inlet temperature	246°C
Gas-plenum length	420 mm	Steam temperature	
Axial blanket pin		Superheater outlet	512°C
Blanket diameter	13.0 mm	Reheater outlet	512°C
Pin outside diameter	14.3 mm	Steam pressure	
Radial blanket pin		Superheater outlet	167 bars
Blanket diameter	12.6 mm	Reheater outlet	33.5 bars
Pin outside diameter	13.9 mm		

At least in the field of the fuel, Phenix must demonstrate that high burnup can be obtained under flux conditions similar to that of power reactors. In Table 1, 50,000 MWd/t is assigned for the burnup. This is the minimum expected burnup, but the fuel pins are designed to support a higher burnup, up to 100,000 MWd/t, if the clad-rupture rate remains low enough.

Main Characteristics

The following options⁽¹⁻³⁾ were chosen during the Phenix design:

The nominal electrical power is 250 MW(e). A standard turbine will be used, but steam temperature will be somewhat smaller than the standard temperature, 510 to 540°C instead of 565°C.

Sodium is used for the primary and secondary cooling.

Fuel is made of UO_2 - PuO_2 mixed oxide. Sintered pellets are enclosed in stainless steel clads.

The core is flattened by using two zones of different enrichment.

The maximum cladding temperature is about 700°C, including hot spots.

The maximum temperature of the sodium at the inlets of the intermediate heat exchangers is 580°C, but a lower temperature, 560°C, is foreseen for the first core.

Phenix is a pool reactor.

Fuel-subassembly transfer inside the reactor tank is made by using a rotating arm eccentrically located

in a rotating plug. Sodium pots are used for subassembly loading and unloading into or from the reactor tank, through a shielded air lock. They are supported by a carriage rolling on a ramp.

Single-wall modular steam generators will be used, including reheaters.

Description of the Plant

A general elevation of the plant is given in Figure 1.

In the reactor building, the reactor, with primary-sodium auxiliary loops and radioactive argon circuits, is installed.

The fuel element and repair building is located on the southward end of the reactor building. It contains the subassembly and control rods cut-up cell, and apparatus used to clean, examine, and cut all the components coming from the reactor.

On the other side of the reactor building, northward, there are successively:

the steam-generator building, connected to the reactor building by the sodium secondary loops;

a surface necessary to disassemble the steam-generator modules;

the turbine house connected to the steam-generator building by the steam loops.

As Phenix is a pool-type reactor, primary sodium pumps and intermediate heat exchangers are located in the main tank. The cold primary sodium is discharged from each pump into a single pipe and flows down to a collector surrounding the subassembly-support grid. Subassemblies are fed by nozzles

Fuel handing building

Reactor building

Steam generator building

FIG. 1. General Section View of Plant.

in their lower part. The sodium leaving the subassemblies flows up through the upper part of the primary tank and enters the intermediate heat exchangers. At the outlet of these IHX it comes back into the main tank and flows to the pump.

Each of the main parts of the plant will be now briefly reviewed and the most important design changes will be noted.

Core and Blanket

The main characteristics of core and blanket are given in Table 1. Several modifications have been made since 1967. The fuel diameter is somewhat larger in order to limit the percentage of PuO_2 in the external zone, to increase the Doppler coefficient, and to reduce the reactivity change due to burnup.

A larger fuel diameter makes possible an increase in the core length without changing pressure drop. Hence, a better neutron economy, a smaller number of subassemblies, and an improvement of the control-rod efficiency were gained.

The larger fuel diameter also leads to a decrease in the number of fuel pins per subassembly by cancelling one layer of pins. In this way, the nominal subassembly flow-rate and the decay heat during core-subassembly unloading were reduced.

The oxide density and thermal performance have been slightly increased since the last irradiation tests. However, the specified linear power is somewhat conservative, because of some uncertainty concerning the behaviour of a higher-density oxide during the first power rise, in which some fuel melting may occur. From this point of view, it would be more propitious to use annular pellets with higher density in the future.

Axial lower-blanket pellets and core pellets are put in the same clad, forming a single pin. This results in a simpler subassembly design. The axial upper blanket is located in a separate pin bundle so as not to increase too greatly the length of the core pin.

In addition to the fuel-pin irradiations, complete mockup subassemblies (see Fig. 2) were fabricated and are now being tested in water and sodium. Pressure drop and lifting force are measured in water. A check is also made that no vibration of the pins or the total subassemblies occurs. Endurance tests are run in sodium at operation temperature. Other tests are performed with subassembly groups for checking the loading conditions. The subassembly nozzles are specially tested to investigate the cavitation problems.

Neutron Shielding

An attempt was made to reduce the cost of the neutron shielding. Boron carbide was envisaged for the upper neutron shielding located at the top of each subassembly. Less expensive materials were investigated; for example, a part of the boron carbide can be replaced by steel and graphite.

In the lateral neutron shielding, use of graphite and borated graphite is foreseen, but other materials are under test.

The thickness of borated steel was reduced in the lower neutron shielding after new calculations. Material tests include irradiations and sodium compatibility check at high temperature.

Control Rods and Control-rod Mechanism

As reported,⁽²⁾ any control rod may be used for control, compensation, and safety. Calculations of the control-rod efficiency showed that the rod number can be limited to six. Natural boron carbide will be used for the first set of rods, but the mechanisms are designed so that tantalum can replace boron carbide.

Safety of reactor operation is largely connected to the reliability of the control rods and the control-rod mechanisms. It was estimated that two different types of control-rod mechanisms must be developed. It is envisaged that both will be used on the reactor in order to compensate for possible defects.

The first type of mechanism has no bellows, and leak tightness is made by an upper closed box. A prototype is under fabrication. Partial tests to be sure that sodium deposit on the upper part of mechanism is not troublesome gave excellent results. But an uncertainty remains about a possible gamma activity due to eventual fission gas release from fuel pins, if this gas enters the upper part of the mechanism.

Long bellows will be used to get leaktightness of the second type of mechanism. A prototype will be fabricated in 1969, but the bellows have been already tested with very promising results.⁽⁴⁾

Reactor Tank (see Fig. 3)

Functions of the primary and main tanks were given in Ref. (3).

The primary tank contains the subassemblies, the neutron shielding, and the inner fuel storage. It separates hot from cool sodium, and its upper part is a collector feeding the intermediate heat exchangers.

The main tank contains all the primary cooling sodium. It is closed at the top by a flat horizontal cover which is extended through the upper biological

FIG. 2. Core Subassembly.

shielding by cylindrical shells around each component.

A leakage tank makes a double wall for the main tank. They are very close to each other, so that a normal cooling of the reactor could be provided in case of leakage of the main tank.

If some accidental important and fast energy release should happen, a failure of the main tank cover is supposed to be possible. Moreover, the postulated energy release may have been underestimated. Therefore, a safety tank is provided around the reactor tanks on the walls of the biological shielding. It is an ultimate barrier to contain energy and active

gases. This tank can plastically yield and be strengthened by the biological concrete. It supports emergency water cooling piping, which may be used in case of failure of normal cooling.

The primary tank is supported by a conical shell welded on the inner wall of the main tank. This tank hangs from the upper biological shield. In this way, the vertical expansion differences between components supported by the upper biological shield and the tanks are reduced. Hangers allow a free horizontal expansion of the tanks.

Inasmuch as experience concerning the behaviour of thermal insulation in argon cover gas is now

FIG. 3. Reactor.

lacking, the thermal insulation is laid on the outside surface of the tank cover, which is hot and independent of the upper biological shielding. If the components are fastened to the upper biological shield, bellows have to be used for joining the biological shield to the main tank cover. The lack of experience of many years of operation with large bellows has led us to prefer a free horizontal sliding of components on the upper biological shield.

The knowledge of temperature distribution in a pool reactor is a hard problem. If a sodium flow cooling the main tank as well as possible is not provided, a hot layer is formed in the upper part of the main tank between this tank and the primary tank. As a result, the thermal gradients developed could change with operating conditions. Tests are being

made to find means for fixing the gradients and protecting the tanks by baffles. Tank models at $\frac{1}{10}$ scale are used with liquids of different densities.

The tanks are protected against fast temperature variations by other baffles which are determined by testing $\frac{1}{4}$ -scale water models.

Pumps and Intermediate Heat Exchangers

The designs of pumps and intermediate heat exchangers were little modified.

Vertically mounted, centrifugal-type mechanical pumps are used. They are driven by a wound-rotor type of motor. The speed control is still under discussion. Rheostat control or Sherbius-type control envisaged. At the discharge of each pump, sodium flows through a single pipe which is connected to

the reactor supply collector by a sliding piece. The pipe will be equipped with a nonreturn valve if further studies show it may be useful.

Pumps will be tested in water, and sliding sleeve-joints will be independently tested in sodium.

The intermediate heat exchangers have straight tubes without any expansion bend, to avoid high stresses in bends. In case of a secondary loop disturbance or failure, flows through the two corresponding heat exchangers are stopped by manually operated mechanical valves which close the inlets of the heat exchangers.

Hydraulic tests are run to determine pressure drop and possible tube vibrations. The heat exchange factors are calculated from Rapsodie experience.

Fuel Handling (see Fig. 4)

As described in Ref. (2), subassembly transfer inside the reactor tank is performed by a transfer arm located in a rotating plug. Subassembly removal from reactor to outside storage by use of sodium pots is effected by a system rolling on inclined ramps.

The reactor transfer arm and the unloading system are under construction and will be tested next year. The arm gripper and the steel ribbon which drives the sodium pots have already been successfully tested.

It was decided to cut the subassemblies on the site in the fuel building and to transport the pin rows to the reprocessing plant. Cutting the subassembly

under water was discarded because of the possible contamination of water in case of failure of fuel clad. Moreover, a fuel bundle may become critical in a water pool if not properly disposed. The subassemblies will be cut in a nitrogen-atmosphere cell equipped with conventional master-slave manipulators.

The spent fuel subassembly is first cooled for two months while stored in the reactor. A second storage is provided outside the reactor to decrease the fission gas activity down to the reprocessing allowance. It consists of a rack supporting sodium pots in a sodium tank. Thus a good thermal inertia is provided, and oxygen contamination inside the pots is limited.

The sodium pots are inserted into this storage by the reactor loading-unloading system, using a second ramp. This storage is partially located under the subassembly cut-up cell, and the subassemblies are directly lifted up from the storage into the cell.

The reactor, outside storage, and subassembly cut-up cell are very close to one another, and the subassembly displacements are minimized as well as building dimensions.

Auxiliary Primary Sodium Loops

These loops have a double purpose: main tank filling and drainage, and sodium purification. There are two sodium storages. The first is located in the reactor building and could be used to drain the tank down to the subassembly tops if necessary, for example, in case of a loading-system failure. Then it is possible

FIG. 4. Fuel Element and Repair Building.

to see what happens by using a TV camera or a periscope. The second storage, in an auxiliary building, could be temporarily used at the beginning of the plan operation for complete drainage of the main tank when sodium is not active.

Sodium is purified by two cold traps, cooled by an organic fluid. When one of the two cold traps is in operation, the other one can be cleaned by transferring sodium oxide into a cooled auxiliary tank.

In case of fuel-cladding failure, a fission product deposit is expected on the internal walls of the loop, even after sodium dumping. Accordingly, several dispositions have been provided to assure loop maintenance; the components that may fail are located in special cells, and movable shieldings can be used to disassemble the valves.

Primary Argon Loop

The cover of the main tank cannot withstand considerable over- or underpressure. Accordingly, the argon atmosphere is connected to the sodium storage

tanks which are empty and provide an important expansion volume. At the reactor outlet, argon cover gas, possibly contaminated by fission gases, can be circulated successively through these storage tanks. If flow rate exceeds $5 \text{ m}^3/\text{hr}$, or if the gas is too contaminated, gas purification is used whereby xenon and krypton are adsorbed by active charcoal.

Secondary Sodium Loops (see Fig. 5)

As described in Ref. (2), each of the three secondary sodium loops consists of two intermediate heat exchangers, a pump located on the cold leg of the loop, a steam generator, an expansion tank, and a sodium-purification system.

The once-through steam generators have no free sodium level, and particular attention was paid to protect intermediate heat exchangers from overpressures arising from sodium-water reaction. For that purpose, the steam generator is located between two free-level sodium tanks. Between the intermediate heat exchangers and the steam generator, a buffer tank is placed, with a closed argon atmosphere; the expansion tank provides another free sodium level at the steam-generator outlet. It also contains the sodium pump. From the expansion tank, an overflow pipe goes down to the storage tank, where a small pump forces the sodium to the cold trap end back to the expansion tank.

Steam Generators (see Fig. 6)

Each steam generator includes 12 stabilized ferritic steel modular boilers, 12 modular superheaters, and 12 modular reheaters, housed in a same insulated casing. The superheaters and the reheaters are made of Type 321 stainless steel. A water separator is used only during operation startup or shutdown cooling to avoid any water introduction into the stainless steel superheater.

If sodium-water reaction occurs in a given steam generator, the failed unit is automatically isolated from the sodium and water loops. On the inlet and outlet sodium, collectors are fitted with rupture discs, for discharging the reaction products outside through a sodium separator and a hydrogen-discharge header. Fast water dumping is also provided.

A steam-generator module, including boiler, superheater, and reheater, is now under test in the Grand Quevilly Facility. It has worked without any trouble for about 2300 hr. The measured thermal performances are very close to the calculated characteristics (see Fig. 7).

Beginning in 1970, a set of three modules will be tested in the 40-MW(th) steam-generator-testing sta-

FIG. 5. Secondary Sodium Loop.

tion located in "Les Renardières." Stability and thermal shock resistance will be checked.

At least another generator is being now in design and will be tested in Grand Quevilly beginning in 1969.

Plant Operation and Control

As Phenix has to demonstrate the possibility of a reliable operation of a fast sodium-cooled power reactor, it is intended to operate the reactor at full power with a load factor as high as possible. It was specified that the plant does not have to follow the changes of the electrical-network demand, leading to the choice of a control system simpler and cheaper than in other similar plants. The main characteristics of the plant are in good accordance with this rule; there are few control rods, negligible spatial effects, and large sodium inertia. Reactor scram does not require the quick startup of many auxiliary systems, and several parts of the plant can be locally controlled.

The consequences of this initial option are particularly evident in the fields of regulation, automatization, and centralized data processing. For example, control rods and sodium pumps are manually operated, and therefore any global regulation is excluded. In the same way, for all nonconventional equipment, and specially for the reactor and its auxiliary systems, automatic locks are used when necessary for safety or if there is a hazard to an expensive component. But all automation will be avoided for plant operation.

In the same manner, the computers of the centralized data-processing system have no direct action on the plant operation, except for scrambling the reactor in case of abnormal temperature rise at the outlet of any core subassembly. The centralized data processing must only survey, record, and display the measurements and other information. The number of data points which must be processed for a prototype reactor like Phenix is very important, around 2,500, from which about 1,000 are only test measurements and about 1,000 must be put into the control data.

In opposition to the previous rules, the control systems aimed at an increase in safety will not be spared. An important effort is made to insure that fast insertion of the control rods occurs in any case, as well as continuity of the core cooling by increasing reliability and independence of the survey and control systems. The core survey is essentially made by thermocouples located at the subassembly sodium outlets as in Rapsodie, where this instrumentation works quite well after more than one year of operation.⁽²⁾

FIG. 6. Phenix Steam Generator for 250 MWe.

Safety

As reported,⁽²⁾ the main features of the Phenix safety philosophy are the following:

A power burst cannot be originated by control rods, because their rate of reactivity insertion is too low.

In case of power failure, a minimum flow rate through the core is insured by shutdown cooling. But the control rods have to be inserted into the core. In case of complete failure of the three secondary sodium loops, emergency cooling can dissipate the decay power.

A localized fuel melting does not seem to be able to spread beyond a few subassemblies. An experimental program is in development to check this assertion, which is now based on calculations.

It was believed that it is not possible to describe a maximum credible accident which would be an envelope of any possible accident without changing the design by too hard specifications. It has been preferred to envelop the reactor by several containments, insuring successive barriers against a power burst: main and leakage tank, safety tank, and biological

FIG. 7. Heat-transfer Results for Steam-generator Module.

shielding concrete supporting safety tank after plastic strain. The safety tank and biological concrete form a primary containment which was designed to support an energy release equivalent to 100 kg TNT.

Conclusion

Rapsodie experience and specific experiments connected to the Phenix design give reasonable confidence for the future of Phenix. Construction of this fast

reactor prototype is now beginning, and first criticality is expected in 1973. All along the detail design and specifications for construction, a continuous effort will be made to keep the investments as low as possible.

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Discussion

Mr. Lagowski (APDA): Would you briefly describe the core-monitoring instrumentation now planned for Phenix?

Mr. Leduc: I think we have to distinguish very carefully between what we really have and what we hope to have. We are sure to have two thermocouples above every subassembly outlet. These thermocouples have been tested in hot sodium inside the reactor, and the operation is very good. Two thermocouples are used in order to get more readout data. In regard to what we hope to have, we are developing methods of detecting sodium voiding; we have left some room in the rotating plug to introduce this type of detection, and maybe we are going to put in the rotating plug some means to sample sodium coming from the subassembly in order to be able to localize clad rupture.

Mr. Lagowski: Have you considered instrumenting the blanket subassemblies in any way, say with thermocouples?

Mr. Leduc: Some blanket subassemblies are instrumented because they are next to the core and the cover plate is larger than the core. We think it is practically impossible to put thermocouples above every blanket subassembly; it would be better but, practically, not possible.

Mr. Campise (ANL): I wonder if you could briefly

summarize your thoughts on the safety considerations for Rapsodie and Phenix.

Mr. Denielou (CEA): There has been a change in the safety philosophy. We have found that in Rapsodie we have been overdesigning for safety. For instance, one basic assumption in Rapsodie was that the total sodium content could pool-burn inside the reactor; this is not physically possible, so it appeared clearly that the containment vessel was over-designed. Besides, the calculated values of 2.3 atm and 160°C which resulted from this pool burning, for instance, could not allow us to test the containment building, so we were never sure that it could withstand that. Accordingly, we shifted from this philosophy to a more realistic one for Phenix. I must say that as regards maximum credible accident, core explosion, excursions, and so on, we are in the same position for Phenix as for Rapsodie, namely, one of ignorance; we cannot precisely define what we fear, except one thing—we will be in a better position with Phenix as regards the fast-acting Doppler coefficient. I don't know the answer. It is a very broad question.

Mr. Golan (AI): Have you considered how you would ship and process your fuel; if so, how did it influence the design to date?

Mr. Leduc: We have made several studies, taking the different possibilities of transportation as a parameter for the reactor plant. But for Phenix this is not an important concern inasmuch as we have to disassemble the subassemblies at the site. With Phenix we want to gain very much information about pin irradiations. We disassemble the subassembly next to the reactor as rapidly as possible after the removal of the subassembly from the reactor. As for future reactors, I have no answer to your question.

R

Design Studies on the Reference Fast Power Plant (PCC-2)

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1. Introduction

Within the framework of the CNEN Fast Reactor Programme, considerable attention has been devoted to the studies of a reference reactor in order to establish the main design and safety criteria, and to make the basic choices for the core and the plant.

The long-term Italian Fast Reactor Programme is orientated towards two lines of development:

- a) the development of a fuel element with a high power density and a high burnup;
- b) the development of the main sodium components, particularly of the intermediate heat exchanger and of the steam generator.

Design studies for the reference power station form an effective point of convergence and normalisation of the above-mentioned two lines; they have also the purpose of establishing the mean and long-term research and development programme.

The conceptual design of the reference power station will form the natural starting point for the design of the Demonstration Power Plant, adopting the same fuel element and the same units for the main system components.

At the present stage of the work, a first optimisation study has allowed the establishment of the main characteristics of the reference core. The plant studies are performed taking into consideration as much as possible the PEC experience.

An intense design activity has been carried out for the sodium circuit components, with specific reference to the steam generator and intermediate heat exchanger. The preferred schemes of reference concepts are presented in this work.

2. Basic Choices for the Core

2.1. General Considerations

The studies were carried out with the following basic assumptions:

- a) mixed oxide with stainless steel cladding as reference fuel;
- b) sodium as a coolant for the primary and secondary circuits;
- c) the fuel-cycle optimisation is carried out on the

basis both of the reactor itself and of a complete system of thermal and fast power stations.

In middle- and long-term evaluations of fast reactor systems, two parameters are of particular importance:

- i) the specific inventory, defined as the amount of fissile material per power unit (kg/MWe);
- ii) the annual production of fissile material, defined as the amount of fissile produced per energy unit (kg/MWe-a).

Even if the reference to the plutonium market cannot be considered as the most important criterion on which the optimisation of a fast reactor is based, this market could, in the long term, be the **most important** factor. When the commercial reliability of fast reactors is demonstrated and their capital costs are comparable with the capital costs of thermal reactors, only limitations on plutonium availability could prevent their full introduction.

A closed market for plutonium is assumed in the majority of current evaluations, mainly on the understanding that the strong plutonium demand compared with the plutonium offered will contribute to minimize the plutonium exchanges. In order to approach the problem in the most general way, our evaluations are carried out on the basis of a free market. The conditions of a self-supporting system can be superimposed as a particular aspect of the problem.

2.2. Specific Power and Burnup

The specific power is the basic choice for the definition of the reference core. The technical and economical characteristics of the reactor are directly influenced by the specific power, and its value contributes to the definition of other important parameters, such as burnup, out-of-pile times, and core geometry.

The upper limit for the specific power is established by the choice of the system, and particularly by the fuel concept. The studies performed so far indicated 5 mm as the minimum diameter for the fuel pin, and 600 W/cm as the maximum value for the rod power. This value, taking into account the overpower coefficient and the hot-point factors, gives an integral conductivity of about 60 W/cm, near to the intrinsic limit for an oxide fuel. Design calculations have

shown the possibility of designing a 3,200-l core for a total power of 2,400 MWt with a plutonium amount of 1,350 kg. The resulting specific power (1800 W/g fissile) seems to be the upper limit for an oxide-fuelled sodium-cooled fast reactor. The problem is whether or not these ultimate conditions give the economical optimum.

Three reactor concepts characterised by specific power values ranging between 1,000 and 1,800 W/g plutonium and by average burnups ranging between 50 and 150 MWD/kg have been studied (see Table 1). For each system we considered a pressure drop through the core of 8 kg/cm², a temperature difference ranging between 150 and 170°C, and a maximum coolant velocity of 10 m/s. Figure 1 shows core doubling time (CDT) versus burnup for different values of specific power. It is clear that a system with a large value of specific power can reach low CDT only for low values of burnup. For burnup values near 100 MWD/kg, the system with a mean value 1440 W/g plutonium for the specific power yields the better CDT. In this respect no advantages can be obtained from pushing the specific power to extreme limits.

The study of these three systems from the economic point of view allows more precise conclusions to be reached. The calculations were carried out considering the reactor itself, a free market for fuel materials, an out-of-pile time of 0.75 yr, and an interest rate of 8%. Table 2 shows the reprocessing and fabrication costs used in the calculations; Fig. 2 shows the results obtained. For each system studied, the fuel-cycle costs show a minimum value for a given burnup value. The profile can be thought as corresponding to a function of the following type:

$$\text{Fuel-cycle costs} = a/BU + b(BU) + c(BU),$$

where the first term represents the fraction of the cost due to shipping, reprocessing, and fabrication, the second term the fraction of the cost due to the fission

FIG. 1. Core Doubling Time vs Burnup for Different Values of Specific Power.

product poisoning, and the third one the cost due to inventory.

The following conclusions can be drawn:

a) The system with a specific power of 1440 W/g plutonium shows the lowest fuel-cycle cost if the same burnup value is considered.

b) For plutonium costs ranging between 15 and 25 \$/g, the optimum burnup can be identified between 120 and 160 MWD/kg for the system with a specific power of 1440 W/g plutonium. On these bases, a minimum value of 100 MWD/kg was fixed for the average burnup of the reference system.

c) The fuel-cycle cost depends slightly on plutonium cost. Only in the case of the 1440-W/g plutonium reactor is the annual income due to the plutonium sold greater than the inventory charges.

These considerations are of general validity. The study of a complete system, in which a fast reactor class is coupled with thermal reactors (LWR), gives slightly different cost figures but the same values for optimum burnup.

2.3. *H/D Ratio*

The studies aimed to define a reference value for the *H/D* ratio of the core were carried out without any limit imposed by consideration of sodium-void coefficient. A limit of 8 kg/cm² for the pressure drop through the core and a ΔT ranging between 150 and 170°C was considered. Figure 3 shows the total and the internal breeding vs. *H/D* for two values of specific power.

The calculations for each reactor were made with the same Δp , the same ΔT , and the same linear power

TABLE 1. CONSIDERED REACTOR SYSTEMS

Reactor	Specific Power (W/g Pu)	Burnup (MWD/kg)	In-core Inventory (kg Pu/MWe)	Pu Production (kg Pu/MWe-a)
1	975	50	2.5	0.34
2	975	100	2.5	0.28
3	975	150	2.5	0.21
4	1440	50	1.7	0.31
5 ^(a)	1440	100	1.7	0.24
6	1440	150	1.7	0.19
7	1800	50	1.35	0.23
8	1800	100	1.35	0.16
9	1800	150	1.35	0.10

^(a) PCC-2 reference concept.

for the fuel pin. Neutron economies become poorer and poorer with increasing H/D ratio, and the internal breeding ratio (IBR) shows its higher value for a value of H/D of about $1/3$. A detailed study has fixed 0.3 as the value for the reference core. Lower values seem to be inconvenient if the same value for Δp is assumed, due to the resulting low figures for sodium fractions that make the subassembly design difficult. On the other hand, higher values for H/D mean a higher amount of power to be extracted per channel and require an unacceptable increase for Δp or a penalty for neutron economy.

TABLE 2. REPROCESSING AND FABRICATION COSTS

	Fabrication Costs, \$/kg		Reprocessing Costs, \$/kg	
	200 ^(a)	135 ^(b)	—	—
Core + axial blanket	50	35	—	—
Radial blanket	140	95	75 ^(a)	50 ^(b)

(a) High Costs.

(b) Low Costs.

FIG. 2. Fuel-cycle Cost as a Function of Average Burnup.

FIG. 3. Total Breeding Ratio and Internal Breeding Ratio as a Function of H/D .

2.4. Out-of-pile Times

A parameter of great importance for the definition of the reference system is the out-of-pile time needed to cool, reprocess, and refabricate the fuel subassemblies. The out-of-pile time enters directly into the expression for the reactor doubling time (DT):

$$DT = \frac{I + XD(df + dw)}{XP} + df + dw,$$

where P is the fissile production flow rate per power unit (kg/MWe-a), I the in-core inventory per power unit (kg/MWe), X the load factor, D the plutonium-supply flow rate per power unit (kg/MWe-a), and dw and df are the respective reprocessing and fabrication times (a).

In the case of a reactor family system (see Fig. 4) the out-of-pile time plays a more important role; in fact, if a certain power $P(t)$ is produced at time t , the system requires an amount of plutonium corresponding to $P(t + df)$ and produces the plutonium corresponding to $P(t - dw)$. Figures 5, 6, and 7 show the integral plutonium balance for three cases of specific power; all the plutonium produced by thermal reactors is stockpiled up to time of its use in fast reactors. The following considerations can be made:

a) The out-of-pile times is a critical parameter that

plays a primary role on the overall plutonium inventory required by the system.

b) Making reference to a system in which an LWR is combined with a FBR characterized by a 1440-W/g specific power, an out-of-pile time of 9 months corresponds to a plutonium debit of about 10 tons. A higher amount would be required if lower or higher values for the specific power is assumed.

c) In a self-supporting system, lower and lower values for out-of-pile time are required if the specific power is of the order of 1000 W/g plutonium or less.

d) For our reference system, an out-of-pile time in the range from 9 to 4.5 months seems to be desirable. A more exact definition of this value requires firm hypotheses about the plutonium market, the type of thermal reactors, and the economical situation.

3. Reactor Core

3.1. General Considerations

The outcome of the preliminary analysis has given the basis for a first conceptual design of the reactor core. These results call for:

- specific power of about 1400 W/g plutonium;
- average burnup $\geq 100,000$ MWD/t;
- H/D ratio of about 0.3 to 0.35;

FIG. 4. Reference Strategy.

FIG. 5. Integral Balance of Fissile Plutonium for the Reference Strategy. Importance of out-of-pile times. (Specific power of 980 W/g plutonium).

FIG. 6. Integral Balance of Fissile Plutonium for the Reference Strategy. Importance of out-of-pile times. (Specific power of 1440 W/g plutonium).

FIG. 7. Integral Balance of Fissile Plutonium for the Reference Strategy. Importance of out-of-pile times. (Specific power of 1800 W/g plutonium).

fissile production of about 0.25 g plutonium per kWe-a.

The following hypotheses were adopted for core design:

- The maximum Δp available through the core is 8 kg/cm².
- The maximum sodium velocity is 10 m/s.
- The maximum (nominal) linear power is 600 W/cm.
- The average sodium temperature at reactor outlet is 560°C.
- The refueling interval is ≥ 6 months.

The choices and the criteria adopted have as a direct consequence a low value for the IBR and, therefore, a rather high reactivity decrease during reactor operation. The control problems seem to be, in fact, those most difficult to be solved.

Core design was carried out without *a priori* limitation on sodium-void coefficient and Doppler effect. More details will be given on these points during discussions on safety criteria.

3.2. Core Description

The reactor core is a two-zoned cylinder; subassemblies have hexagonal cross sections, and the fuel pins are provided with a device to discharge the fission gas to the coolant. A vented-fuel concept was adopted

in order to achieve both better fuel-cycle economies and a higher degree of reliability and safety during operation. Figure 8 shows a core cross section, and Table 3 gives its main characteristics.

The refueling is performed by changing batches of one-third of the fuel each time; with an average burnup of 100 MWD/kg, corresponding to an in-core time of 18 months; the refueling interval is 6 months. For radial blanket subassemblies an average in-core life of 36 months was chosen.

In the calculation, an equilibrium composition for plutonium was taken, under the assumption that core and blankets were reprocessed together. Figure 9 shows the variation in plutonium composition *vs.* time. Figure 10 shows the geometry used in the neutronic calculations. These were carried out considering the reactor core at the beginning of the equilibrium 6-month cycle. An optimisation study on plutonium enrichments in the inner and outer zones has given 11.9 and 15.7 a/o, respectively. Figure 11 shows the power-density profile through the core. The strong influence of control rods can be observed due to the rather large reactivity to be controlled, as shown in Table 4.

The outer ring of control rods, and eventually the central rod (see Fig. 8), is used to control reactivity during operation. The safety rods are placed in the

FIG. 8. Core Cross Section.

inner ring, and two additional rods, near the boundary with the radial blanket, are used for regulation.

A shim rod in the central portion of the core seems to be convenient from the viewpoint of power-density distribution (see Figs. 12 and 13). From a safety point of view, the problem appears to be more complicated, due to the large amount of reactivity controlled ($\sim 3\%$) with ensuing severe instability problems.

Blanket and core subassemblies are shown in Figure 14. A hydraulic holddown is adopted: sodium at high pressure enters the fuel subassembly laterally, and the low-pressure sodium is used to feed the blanket subassembly from the bottom.

A neutronic shield consisting of two rows of natural B_4C subassemblies is located outside the reflector, in order to reduce the heat produced outside the reactor vessel.

4. Safety

4.1. General Philosophy on Safety Problems

As past experience has demonstrated, some safety criteria referring to hypothetical accident conditions occurring during reactor operation have a strong effect

TABLE 3. MAIN PARAMETERS OF PCC-2 CORE

Total power, MWt	2400
Net electric output, MWe	990
Core sizes:	
Active height, cm	80
Volume, l	4000
Core subassemblies, no.	210
Radial blanket subassemblies, no.	162
Subassembly across flats, cm	16
Control rods, no.	15
Fuel-pin inner diameter, cm	0.56
Blanket-pin inner diameter, cm	0.92
Axial blanket height, cm	2×40
Amount of $^{239}\text{Pu} + ^{241}\text{Pu}$ in the core ($k \approx 1.04$), kg	1,670
Amount of Pu + U in the core, kg	12,070
Amount of Pu + U in the whole reactor, kg	50,000
Pu enrichment, a/o	$11.9 \div 15.7$
Average burnup, MWD/t	100
Volume fraction in the core, %:	
Fuel	39.5
Steel	16
Sodium	44.5
Power density (max), W/cm ³ core	950
Volumetric power (max), W/cm ³ fuel	2400
Average specific power, W/g Pu	1440
Rod power (max), W/cm	600
Heat flux to coolant (max), W/cm ²	300
ΔT through the core, °C	170
Sodium outside temperature, °C	560
Available Δp through the core, kg/cm ²	8
TBR	1.31
IBR	0.72

FIG. 9. Isotopic Composition of Plutonium during Reactor Life.

on the technical and economical features of a reactor. Because of this, the greatest attention has to be paid to their realistic definitions.

The design of first-generation fast reactors has been strongly influenced by the degree of uncertainty related to their behaviour under accident conditions. Very pessimistic assumptions were made in foreseeing consequences of such accidents on the civilian population, by taking as reference accident the Maximum Hypothetical Incident (MHI) which takes no account of automatic safeguards. As a direct consequence of such a philosophy, severe limitations for the sodium-void coefficient and for Doppler coefficient were adopted. This resulted in important restrictions on core geometry and neutronic spectrum. However, the experience available so far with the sodium-cooled fast reactor shows that only minor incidents occurred.

Future design of fast reactors has to be carried out on the basis of much more realistic safety criteria. The development of nuclear technology and the greater degree of reliability of the automatic safeguards make this approach practical.

FIG. 10. Reactor with Central Control Rod. Geometry used in neutron calculation.

FIG. 11. Power Density Profile on the Reactor Midplane in Absence of Central Control Rod.

In designing our reference power plant we did not establish any limit to the sodium-void and Doppler coefficients. Moreover, we are fully convinced that the greatest care must be taken in order to get a high

degree of plant reliability resulting in a high load factor.

This approach does not exclude the worst accidents; however, the causes have to be credible, and in the accident sequence intrinsic safeguards, such as nuclear feedbacks and special plant features (double containment, "fail-safe" mechanisms, etc.), have to be considered.

In this light a Maximum Credible Incident (MCI) is taken as a reference to design containment structures. We do believe, in fact, that for a long time to come, the psychological concept of safety will require that even a very improbable accident, such as the MCI, should be taken into account.

4.2. Approach to the MCI

Figure 15 shows schematically the sequences of typical reactor incidents. The flow diagram refers to failures of components or operation mistakes, the results of which are typified by different degrees of risk.

In the first case, we face a situation in which the main effect is contamination of the primary circuit. This has to be considered a normal operating condition for a power reactor with vented or closed pins.

In the second case we consider the melting of a considerable fraction of the core, involving a large release of plutonium and fission products to the coolant.

In the third case we consider the Maximum Credible Incident, able to produce an amount of energy high enough to disassemble the core. The containment

FIG. 12. Power Density Distribution (W/cm^2) on the Reactor Midplane, without Central Control Rod.

structures of the reactor are designed on the basis of these conditions.

The rejection of an imposed limit on the value of the sodium-void coefficient does not mean that the problem can be neglected. This approach requires that the kinetics of sodium voiding be studied in detail. In this light, an intensive experimental study is underway in the Italian Fast Reactor Programme in order to achieve a full understanding of the phenomenon involving sodium superheating and the boiling mechanism. At present, safety studies are limited to typical transient conditions, with the aim of studying reactor response to abnormal conditions.

4.3. Static Characteristics

Table 5 gives the main static characteristics for the reference core. Figure 16 gives the reactivity variation due to progressive voiding of the core as a function of reactor radius. Three cases were considered, namely,

TABLE 4. REACTIVITY CONTROLLED BY SAFETY AND SHIM RODS

From cold to full power, $\$$	4
Burnup in six months, $\$$	8
Shutdown reserve, $\$$	10

core, half the core, and core plus axial blanket voiding. As can be seen, the maximum positive reactivity is about $\$5$. Figure 17 shows the reactivity variation due to Doppler effect when the fuel temperature rises from 300 to 900°K, as a function of reactor radius.

4.4. Behaviour during Power Transients

Figure 18 shows the times required to melt the fuel ($T_f = 2760^\circ\text{C}$), to reach the pin failure limit ($T_B = 3600^\circ\text{C}$), and to reach sodium boiling ($T_{Na} = 920^\circ\text{C}$) as a function of inserted reactivity ramp. The calculations were made with reference to the hot channel

FIG. 13. Power Density Distribution (W/cm^3) on the Reactor Midplane, with Central Control Rod.

and were performed for a fuel diameter of 5.8 mm. The curve for sodium boiling is not plotted for a ramp rate higher than 5 \$/sec because after this value the fuel melts before the sodium boils. In all the cases studied, it was not possible to have sodium boiling before pin failure.

Figure 19 shows the time required to reach fuel melting in the hot channel, and the imposed and total reactivities reached at the melting point. It should be observed that after a given ramp rate, these values depend only slightly on the rate itself.

In Fig. 20, with a reactivity ramp of given height specified, the time required to reach melting is given. We can observe that, independently of the inserted ramp, an asymptotic value of approx \$0.21 is reached for the reactivity. This value differs from the value of \$0.32 shown in Table 5 because it has been calculated neglecting the negative reactivity due to fuel axial expansion.

5. Plant Studies

5.1. General Considerations

At present, plant studies are directed toward two main objectives:

1. to select ground rules and design criteria for the technological solutions characterised by the highest degree of economics, safety, and reliability;
2. to identify the research and development programme to bring economic and safe commercial fast reactor into operation in due time.

Studies of the basic choices for the plant are carried out, taking into consideration several alternative solutions for primary circuit, internals, fuel handling, and containment structures.

During these studies, choices already made in the design of the PEC reactor are considered with high priority, provided they meet the economical and reliability criteria.

The criteria for plant design are summarised briefly in the following:

5.1.1. Primary Circuit

The number of components must be selected in such a way as to insure a high load factor, a good overall stability, and a low cost. The selected configuration must allow an effective natural convection in order to extract residual power in case the primary pumps fail; as an additional safeguard an emergency cooling system must be considered. The circuit must be de-

FIG. 14. Pins and Subassemblies for Blankets.

signed in such a way as to avoid sodium drain from the core (double containment).

5.1.2. Containment

The whole primary circuit must be contained in an inert atmosphere, in order to avoid sodium fires. Moreover, such a feature should be able to limit pressure increases due to sodium-air reaction occurring during explosive incidents. The part of the plant containing radioactive material must be located in a containment building able to retain fission gases and aerosol with a maximum leak of 0.1% per day.

5.1.3. Fuel Handling

For safety reasons we do not consider a continuous refueling system. The concept of a high specific power reactor, with consequential short refueling periods, requires that load, transfer, and cooling operations have to be carried out in such way as to reduce the reactor down-time and the out-of-pile fuel storage. The plant has to be provided with positions in which the whole core can be stored under sodium.

FIG. 15. Some Typical Accident Sequences.

TABLE 5. STATICS CHARACTERISTICS OF PCC-2 REACTOR

Central-subassembly reactivity worth, \$	0.71
β_{eff}	0.0035
Neutron mean life, s	4×10^{-7}
Average fission energy in the core, keV	
Outer zone	234
Inner zone	206
Average neutron number per fission,	2.91
Fuel average temperature in the core, °C	1450
Doppler constant (A_{DOP}), $T dk/dT$	-0.0065
Fuel-expansion coefficient, $H dk/dH$	0.21
Core radial expansion coefficient, $R dk/dR$	0.15
Fuel density coefficient, $\delta_f dk/d\delta_f$	0.34
Stainless steel density coefficient, $\delta_{ss} dk/d\delta_{ss}$	-0.01
Sodium density coefficient, $\delta_{Na} dk/d\delta_{Na}$	-0.014
Power coefficients ($T_{fuel} = 1450^\circ\text{C}$)	
Only Doppler, $P dk/dP$	$-3.2 \cdot 10^{-3}$
Total $P dk/dP$	$-5.2 \cdot 10^{-3}$
Maximum sodium-void reactivity, \$	5
Reactivity difference between nominal power and fuel melting in the hot channel, \$	-0.32

5.1.4. Vented Fuel

The cover-gas system must be designed with a high degree of tightness. This can be achieved by using high-efficiency mechanical seals and adequate pressure distribution. All the gas lines and the decay tanks must be included within the containment building. The primary circuit has to be designed to accept typical contaminations due to vented-fuel operation in transient conditions (startup, shutdown, and over-power bursts).

In order to limit the potential hazards of a highly radioactive cover gas, it is important to foresee a purification plant able to reduce specific activity by a factor of 100 to 1000.

5.2. Plant Layout and Thermal Cycle

A plant layout is now under study on the basis of the main technological choices made for the PEC reactor. A simplified flow sheet for the thermal cycle is shown in Fig. 21. A semi-integrated solution is studied for the primary circuit with three primary loops. For the secondary circuit, 6 intermediate heat exchangers and 6 steam generators are considered.

The adopted intermediate heat exchanger is shown in Fig. 22. The selected configuration reduces the problems connected with thermal expansion, and allows the use of a reduced number of tubes with many advantages on size and reliability.

Steam generators (see Fig. 23) are of a "once-through" type with helicoidal tubes. A superheating unit is connected to each steam generator, and a "tandem-compound" turbine type is adopted as reference component.

FIG. 16. Reactivity Effects of Partial Sodium Voiding.

FIG. 17. Reactivity Variation for Doppler Effect in Consequence of an Uniform Change in Fuel Temperature from 300 to 900°K.

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Discussion

See Discussion following article by P. Aiello et al., THIS SESSION.

FIG. 18. Times to Reach Fuel Melting (t_f), to Reach Pin Failure (t_b), and to Reach Sodium Boiling (t_{NA}) in the Hot Channel.

FIG. 19. Time (t_f) to Reach Fuel Melting in the Hot Channel, Impressed Reactivity (R_i), and Total Reactivity (R_t) at the Melting Point.

FIG. 20. Time to Reach Fuel Melting in the Hot Channel vs Ramp Height.

FIG. 21. Flow Diagram of System.

FIG. 22. Intermediate Heat Exchanger.

FIG. 23. Steam Generator.

PEC: Status of the Design before Construction

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The PEC Reactor

The purpose of the PEC design is the construction of a fast sodium-cooled reactor, with a total maximum power of 140 MWt, for the testing of fuel elements. This is the most important experimental equipment contemplated in the Fast Reactors Program.

Design studies for the reactor began in 1964, and by the end of 1965 a definite concept had been reached. In August 1966 the general design was completed. During 1968 the contract for the construction design and actual construction will be granted.

The diagram in Figure 1 represents the program of operations. The term "design support tests" includes the test of prototypes; the term "plant tests" includes general non-nuclear tests and nuclear tests until the reactor is activated to full power; construction and assembling operations include partial testings.

If the work of the engineer-architect begins during the first few months of 1969, the plant will be operating by the second half of 1973.

Purpose and General Criteria of the Design

The idea of constructing the PEC reactor has been dictated by the necessity of having available some means for the testing of fuel elements that will be employed in fast power stations in the 1980's.

The fast reactors available nowadays have all been designed as "experimental plants" and cannot be utilised for the testing of fuel elements, especially in view of the performance expected for the future.

An analysis of the limits and difficulties connected with the use of available reactors for fuel test have provided important information to define the experimental performance of PEC.

This information may be summarised as follows:

The intensity of the maximum neutron flux at the center of the reactor should be almost 10^{16} n/cm², since the flux calculated for fast power reactors presently under study ranges between $6.5 \cdot 10^{15}$ n/cm² (reactor submitted by the General Electric) and $1.1 \cdot 10^{16}$ n/cm² (PCC-2 submitted by the Italian Fast Reactors Program). It is difficult to reach this limit, and we must be satisfied with a lower value of $3 \cdot 10^{15}$

n/cm²; the fraction of neutrons with an energy greater than 0.1 MeV must be between 60 and 75%.

The active height of test elements must be able to reach 1,000 mm since the active height of submitted reactors ranges between 400 mm (G.E. pancake reactor) and 1,000 mm (French reactor).

The area available for test elements must be such that each element can hold at least 61 pins; according to studies carried out on the border effect in fuel elements for sodium fast reactors, it may be estimated that 4 rows of pins surrounding the central pin are enough to obtain a behaviour similar to elements having a much greater number of pins; nevertheless, it is better to have the possibility of testing a whole element of the power reactor.

Test elements (some at least) must be able to be instrumented, with the possibility of continuous transmission of information out of the reactor during the whole period of the irradiation test. In fact, many phenomena must be known because the design of the fuel element cannot be quantitatively estimated and often not even qualitatively interpreted without a continuous check on some fundamental quantities while in operation.

It must be possible to have coolant flow through the test elements, even with a 15-kg/cm² pressure loss; in fact, since the pressure loss in the core of the reactors submitted ranges between 2 kg/cm² in the German reactor and 8-9 kg/cm² in the French reactor, leaving a margin of 20% for power (that is, 44% for the pressure loss), a maximum of 13 kg/cm² is reached, and it is prudent to raise it to 15 kg/cm² in consideration of a possible increase in the pressure loss which should be foreseen in the future.

The coolant flow rate and inlet temperature must be able to be regulated independently from the rest of the reactor, since it is very important to test the fuel elements under variable operating conditions or at least different from the rather rigid conditions of the reactor and its plant.

Test elements must be the "vented" type, along the lines of the Italian fuel-development program for fast reactors.

Test elements (or at least some of them) should preferably be housed in suitable channels completely

FIG. 1. Program of Plant Construction.

isolated from the rest of the reactor from the hydraulic point of view. This requirement is the necessary premise for the three above points and for the two following.

It must be possible to submit test elements to transient conditions of power and flow, since a significant guarantee for satisfactory functioning of the fuel element may be given only after it has been tested under every possible stationary, transient, and—within certain limits—accidental, conditions.

It must be possible to perform lengthy endurance tests, until the failure of the element. In fact, it is probable that in the near future the maximum burnup for reactor fuel will have to approach 200,000 MWd/t. It will, therefore, be necessary to determine at what value of the burnup a certain element fails, and to verify the difference between the specified burnup and the failure point.

The functional requirements of the plant are, naturally, the result of a compromise to satisfy the research workers, to hold the cost within acceptable limits, and to have the plant ready to begin its operations in due time, bearing in mind the technological context within which it must be realised.

To this effect, we have attempted to devise a reactor on the basis of the technological means presently available. This has, of course, been a limiting factor and is one of the reasons why some technological solutions applicable to reactors presently under consideration had to be rejected.

Basic Technical Choices

The basic choices which have given the plant its present aspect and features are related to the following items:

- the reactor core;
- the location of the test fuel;
- the fuel-handling system;
- the layout of the primary cooling circuit of the reactor;
- the dispersal of the energy produced.

The choices related to the first three items have been dictated primarily by the need to reach the best experimental performance; as far as the last two items are concerned, the choices are based on the consideration of immediate realisation, economy, and reliable operation.

The Core of the Reactor

The major difficulty that must be resolved when establishing the characteristics of a reactor for testing fuel elements is the fact that at the same power density, the flux and the total power are increasing functions of the volume of the core. This means that to obtain a flux equal to the flux of a power reactor in the first approximation (see Fig. 2), an equally sized reactor should be constructed (that is, the power

FIG. 2. Flux and Total Power vs Volume.

reactor itself!) or a smaller reactor with a higher power density. The first solution is absurd from the economic point of view, and the second is logically impossible since, although thermal-reactor fuel elements could be developed to work under more severe conditions, at the present state of fast reactor technology no fuel could be obtained, even with extensive and very costly development, which would be able to work under conditions more severe than those to which the fuel under test will be subjected.

Therefore, we must test the fuel at a flux lower than the actual working flux; the power density of the test fuel must be increased to a much higher degree than the degree of the fuel of the reactor, compounding into the former more fissile material per volume than into the latter (which in practice in fuels formed from plutonium and natural uranium may be obtained by substituting all or part of the natural uranium with ^{235}U).

Three expedients have been studied that may determine a slight increase in the flux, volume and power density being equal:

Place the test elements at the point of maximum flux, i.e., in the center, a secondary but not negligible consequence is that the significant quantity of fissile material contained in the elements, placed at the most "important" point, reduces the quantity of fissile material necessary in the core; consequently, the flux increases, the power density and the volume of the core itself being equal.

Replace the fertile blanket surrounding the core of power fast reactors with a lower-absorption reflector made of a suitable material (nickel, steel, or beryllium oxide) that would require a smaller quantity of fissile material in the core, and consequently determine an increase in the flux.

Substitute for the ^{238}U contained in the fuel of the reactor a material with a lower absorption cross-section. In this way the fissile requirement is decreased, though increasing the flux, power density and volume being equal. The following are the possible materials that can be used: alumina, steel, graphite, and beryllium.

The first and second expedients have been adopted. The third was discarded because the performance of a fuel may be sufficiently guaranteed only if it has been tested in some way. Any variation from the tested samples creates a great uncertainty about the elements in operation. At present, uranium oxide and mixed-oxide fuel are the only ones that have been tested for fast reactors, although not under severe conditions and at a low burnup.

Therefore, we have selected a uranium oxide fuel—

for safety reasons—for the first stage. A mixed uranium and plutonium oxide fuel may be used for the second stage.

Thus a compromise must be reached between the need to obtain a high flux and a limited power. Since a flux similar to that of power reactors can be obtained only by constructing the power reactor itself, it is advisable to carry out, besides the tests relative to power generation and connected phenomena, irradiation tests of materials. In the course of these tests the irradiation time will be extended, in order to reach the same integrated flux of a power reactor.

Bearing in mind that both the period required for these irradiations and the power of the reactor must be contained within reasonable limits, a 1:3 ratio between the PEC flux and the flux of a power reactor has been accepted as a good compromise. This corresponds to:

- a) a 400-liter core volume;
- b) a power of about 100 MWt, with a maximum power density of the reactor core fuel of a little over 400 kW/l;
- c) an experimental cavity of about 75-liter volume, with a 900-mm active height, containing 43 kg of fissile material.

In the construction plan for experimental channels presently being considered, the fuel occupies 45% of the volume of the experimental cavity (16.4 liters of fuel in the three test channels and 17.2 in the three instrumented elements).

Regarding the design of the reactor core, it is noted that a solution has been adopted to compensate for the consumption of fissile material during the life of a fuel charge. Because of the relatively small quantity of fertile material, the production of plutonium is low, and the reactivity decrement during the life of the fuel is considerable; therefore, it would be rather difficult to provide, when the fuel is fresh, the large required margin of positive reactivity, compensating it with control rods. Hence, the chosen solution was to change the volume of the core periodically by adding some fresh elements. In this way the control rods will be required to compensate only the reactivity margin necessary for the operation period between two additions.

This solution causes, with respect to the conventional solution, a decrease in the flux value, averaged over the life of the fuel charge, of 2%.

Location of the Test Fuel

For the location of the test fuel (see Fig. 3), two possibilities may be taken into consideration: loca-

FIG. 3. Scheme of the PEC Reactor Core.

tion of the fuel at the center of the reactor, or along a circular crown placed near the core-reflector border.

The former solution has been selected for two fundamental reasons:

- a better architecture of the reactor is allowed;
- it is better that the fuel be located in the maximum flow area.

The drawback of this solution is that eventual movements of the fissile material contained in the test elements resulting from accidental causes may determine the maximum reactivity change. In the reference layout (see Fig. 4) the central experimental area is formed of three test channels, hydraulically separated from one another and from the rest of the reactor, and of three instrumented elements cooled by the reactor coolant. This layout is reached through the following logic procedure:

The space available for the experimental area is limited by the fact that control rods may not be moved beyond the core-reflector border.

It would be hazardous to have only one test channel utilising all the available space, due to the large

quantity of fissile material which would be involved in a hypothetical accident.

Three test channels constitute a good compromise between the need of a rational exploitation of the available space and the need to have test elements each with a sufficient number of pins.

For construction reasons, on the plan the entrance is circular and, therefore, it is possible to reach from above and to instrument three additional elements which are part of the usual lattice of the reactor.

The experimental zone is conceived as a fully equipped cavity; substitution in the test channels, in fact, can be made with handling operations that can be considered as part of the normal routine of reactor operation. The reference layout is that with which the reactor will be activated to full power for the first time.

Afterwards, on the basis of experimental requirements and on the state of development of reactor technology, reference channels may be substituted with diverse channels, eventually with a coolant other than sodium, or with only one channel. It may even be possible to test a whole core completely, abolishing test channels. In fact, the core-supporting grid bears,

FIG. 4. Scheme of PEC Reactor Unit.

to in the experimental zone, the seat for elements which need to be inserted to fill it completely with the normal lattice in the absence of test channels.

Finally, every element of the core of the reactor can be substituted with a test element. Obviously these elements can be instrumented only with the "posthumous reading" systems, and their type, number, and arrangement depend on the safe and reliable operation of the reactor.

Fuel-handling System

Three general ideas on the arrangement of the fuel-handling system have been studied:

- a) a one-cell arrangement;
- b) a one-floor arrangement;
- c) an intermediate room-plus-cell arrangement.

Figure 5 describes the first two solutions. The one-cell arrangement features a large cell, perfectly sealed from the atmosphere, over the reactor. The plug of the reactor is part of the floor of this cell and is crossed only by the driving mechanisms of the control rods. Test channels terminate under the plug, and the relative sodium entrance and exit tubes cross the reactor vessel laterally, right underneath the plug itself. To handle the fuel, the control rods are released, the plug is lifted until the heads of the driving mechanisms of the control rods touch the cell ceiling, and the sodium level is lowered below the heads of the fuel-element extensions. The actual handling is performed by means of several devices operated from the outside through the lateral walls of the cell, possibly with visual controls, because shielded windows may be present.

In the one-floor arrangement the upper surface of the plug of the reactor is part of a working floor, the heads and respective seal plugs of test channels projecting from the floor, and the sodium entrance and exit tubes—properly shielded—running radially at a distance of a few decimeters from the floor. Three charging machines, crossing the plug in three points at 120° , carry out the internal movements of the fuel. By means of a shielded carriage moving on rails and equipped with a lower port that can effect a temporary sealing, the fuel of the reactor is removed through an appropriate outlet and the fuel of the test channels is removed directly through the head of each channel.

The advantage of the first arrangement is that the fuel is handled in a hot cell. Failures and accidents which may occur with the irradiated fuel have limited and controllable consequences. The great drawbacks of this arrangement are the use of closed test channels which would be extremely complicated to handle, and

FIG. 5. Fuel Handling.

two very sensitive points: the fact that many mechanisms operate in an atmosphere containing sodium vapour, and the fact that elements must be gas-cooled while being handled out of sodium.

The second arrangement, which at first sight appears simpler and more conventional, is no longer attractive when considering the great risks connected to the fact that the handling of the test fuel and of the test channels themselves is done in environments accessible to people. Furthermore, this arrangement involved two considerable construction drawbacks: the complexity, weight, and size of the discharging carriage, and the space occupied by the sodium tubes of test channels in the operation area of the reactor.

Thus, both the above arrangements were rejected, and a special arrangement (see Fig. 6) has been chosen so that the handling of the irradiated fuel and of the test channels is easier and safer. It stems from an amalgamation of the following technical solutions, which have already been utilised separately and for different purposes:

- the handling of the fuel inside the reactor, with a rotating plug and an arm-type machine;
- the use of an alpha-tight hot cell for all operations with the irradiated fuel and not for operations of the reactor.

In summary, the room containing inert atmosphere, sodium vapour, and contaminating gas is clepsydra-shaped, and the area accessible for maintenance of the control and operation gears is located around its narrow neck.

A particularly important consequence of the choice of this solution is that the whole plant is thus distributed on two working floors: the main one

FIG. 6. Scheme of Operations for Handling Fuel.

which is accessible at all times, and the secondary floor accessible only when the reactor is off. The advantage of this general arrangement is that a double shielding and sealing barrier separates the main working floors from the reactor.

In particular, the double shielding barrier avoids the need to adopt complex devices to reduce to a biological level radiation leaks from the fissures and penetrations through the plug of the reactor. The double sealing barrier is, at present, the most convenient solution for reactors employing vented fuel or designed to operate with a certain number of failed cans.

By means of proper pressure and ventilation it is possible to maintain three contamination levels, gradually decreasing from the very high level which can be found in the reactor to the level biologically accepted on the working floor. This is very advantageous for personnel safety; a careful check of the

contamination in the intermediate room permits noting possible breakdowns of the plant which might cause a contamination increase in the accessible area.

Layout of the Primary Cooling Circuit of the Reactor

A study of the solutions applied to existing fast reactors and to prototype fast reactors presently being studied indicates that there exist two opposite tendencies in the choice of a layout of the primary circuit (see Fig. 7).

The first tendency is the one realised, chronologically, in the following reactors: DFR (GB); Fermi (USA); Rapsodie (France). In these reactors the reactor vessel is at the center of the system, the upper surface of the plug being part of the working floor; laterally, below the working floor, the intermediate heat exchangers, the pumps, and the traps are located. The reactor vessel is connected to the

EBR-II SOLUTION

D.F.R., E.F.F.B.R.,
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FIG. 7. Reactor Primary Circuit.

components by means of pipes running in such a way as to accommodate elastic deformations due to thermal expansion. Every component is surrounded by shielding and safety walls, and the reactor vessel is surrounded by the neutron shield which must lower the radiation level at the intermediate heat exchangers to a value which will not activate the secondary sodium. The whole primary cooling circuit is double-walled with monitoring systems for possible sodium leaks located in the interspace which is maintained as an inert atmosphere.

The second tendency was realised in the EBR-2 reactor. It is the basis for many projects of prototype fast reactors presently being studied, especially the French and English ones. The "integrated solution" resulting from it calls for a primary vessel containing the reactor vessel surrounded by pumps, intermediate heat exchangers, and the cold traps. The space remaining in the primary vessel replaces the return pipes from the heat exchangers to the pumps; therefore it is full of sodium at the temperature of exit from the heat exchangers.

The following are the most important points in favour of the first solution:

The neutron shield surrounding the reactor, which must impede the activation of the secondary sodium in the intermediate heat exchangers, is simply designed and may be realised on the basis of the experience acquired to the present day in the construction of thermal reactors (in the integrated solution this shield must be immersed in sodium).

The size of the reactor vessel and of the other components is reduced; therefore, these parts may be factory-built and installed without overburdening the already complicated plant-assembling operations.

The reactor plug, which is the most critical point in the event of an explosion incident, is of minimum

dimensions and can be designed in a manner unlikely to fail, or at least not to cause secondary damage.

The following are the most important elements attributed to the second solution:

There are very few pipes; furthermore, they are all immersed in sodium and might not be perfectly sealed when connected to the other components.

The structure of the plant provides for a large heat sink common to all cooling circuits. If the secondary circuits should not function or in case of other damages, the temperature of the sodium during migration to the reactor through the pumps is equal for a certain time to the nominal temperature of entry into the reactor. This permits, among other things, the rejection—when designing the reactor—of the hypothesis of a thermal shock at the entrance of the reactor as a physically impossible phenomenon.

Finally, in case of an accident of any kind, the heat produced must heat all the sodium contained in the primary vessel before it becomes dangerous.

The solution we have adopted (see Fig. 8) is known as the "semi-integrated solution" and is the result of a compromise of the two above solutions. It combines the characteristics of each solution which are considered advantageous to an experimental plant such as PEC.

The main cooling circuit of PEC is thus formed of two major blocks: the reactor vessel and the com-

FIG. 8. Cutaway Diagram of Primary Circuits.

ponent tank, connected to each other by the sodium migration and return pipes.

The following are the characteristics of this compromise solution which are especially advantageous to this type of plant:

The reactor vessel is isolated from the component ensemble. The distance between the components of the main cooling circuit and the ensemble of devices located in the reactor plug is extremely useful for the operation of the plant, although the general architecture is still compact and the areas are well utilised.

The main components of the primary cooling circuit are installed in a tank, in a way similar to the one planned for prototype "integrated" reactors.

The heat sink made of the sodium mass contained in the components tank eliminates any possibility of a dangerous transient of the sodium temperature of entry and permits the improvement—when designing the reactor—of the characteristics of the core, thus improving the reactor performance, and lessens the severity of safety measures to be taken in case of a breakdown in the secondary cooling circuits.

Dispersal of the Energy Produced

Sodium has been chosen as the secondary fluid, because in the PEC a secondary circuit utilising the same fluid as for the primary circuit presents less difficulty and uncertainty.

Water, steam, or air have been considered. The major drawback for the employment of water or steam is the known fact that sodium-water heat exchangers and particularly sodium-steam generators are still confronted with technological problems which have not been solved satisfactorily. Last, but not least, is the risk of a sodium-water reaction. Furthermore, there exist particular problems and disadvantages typical of the employment of water or steam as a third fluid. The use of flowing water or water cooled by means of cooling towers (therefore not under pressure) would involve serious problems caused by the high temperature difference between sodium and water. The use of a water or steam-under-pressure third circuit, refrigerated by water not under pressure, would involve a much more complex plant and, therefore, a considerable increase in the cost of the plant.

FIG. 9. Cutaway Diagram of Secondary Cooling Circuits.

The idea of developing low-performance steam generators or special sodium-water exchangers has been discarded, there being no real interest in the development of components which can be employed only for the particular requirements of this plant and may not be utilised—even by extrapolation—for power plants.

Thus the solution of a heat loss to the air by means of sodium-air heat exchangers has been studied (see Fig. 9). Such an apparatus is rather expensive but has the advantage that it has been tested, especially at the Rapsodie reactor. This solution does not involve the risks of a sodium-water reaction and the third circuit is reduced to the minimum; the air is drawn from the atmosphere, passes through a frame of finned tubes through which secondary sodium passes, and is then sent back into the atmosphere.

Therefore the heat loss by means of sodium-air heat exchangers has been chosen as a reference solution; the sodium-air heat exchangers will be 6–20 MW each for the reactor and 3–6 MW each for the test channels.

The plant is designed to permit the installation and testing of one or more steam generators if desired. The sodium-air heat exchangers, which are always kept in a state of readiness, would be immediately started in case of breakdown of the steam generators, to permit the continuous operation of the plant.

Description of the PEC Reactor

The axonometric vertical section of the containment building (see Fig. 10) gives a general view of the reactor.

The vertically placed test channels have completely independent refrigeration circuits, cross the shielding plug which closes the upper part of the reactor vessel, and extend through a closed room located immediately above the reactor. The working floor is located over this room, and from it rises the cell into which experimental channels and extraction passages of the fuel elements terminate. A part of the plug, concentric to the central section crossed by the test channels, is able to rotate and houses the operating gears.

All operating devices, including inlets and outlets of experimental channels, are located in the upper part of the reactor.

The fuel moves through a series of communicating alpha-tight cells to the hot cells outside the containment building. The external hot cells are equipped for operations relative to the running of the reactor and for nondestructive tests on test sections.

The reactor and the channels are sodium-cooled, and the whole plant is dimensioned to a total thermal power of 140 MW generated in the core. The primary circuit heats a secondary sodium circuit, and the energy produced is then dispersed through sodium-air heat exchangers.

The whole complex is housed in a metal containment building surrounded by three main blocks.

To assure a proper containment in case of nuclear accident, a double barrier has been provided. The first is constituted of the walls and liners of the cells and rooms which surround the reactor, and the second is the actual containment building.

Reactor Unit

Core

The fuel is constituted of 33% enriched uranium oxide, sinterized, pierced pellets. The fuel elements of

FIG. 10. Cutaway Diagram of the Containment Building.

TABLE 1. CHARACTERISTICS OF REACTOR FUEL ELEMENT

Shape of the section	Hexagonal
Across flats	84.4 mm
Total length	3000.0 mm
Wrapper thickness	2.25 mm
Number of fuel pins	127
Pin outer diameter	5.84 mm
Clad thickness	0.38 mm
Pin pitch/diameter ratio	1.20
Fuel	UO ₂
Fuel shape	Pierced pellets
Pellet inner diameter	1.55 mm
Pellet outer diameter	4.995 mm
Pellet height	5.0 mm
Max theoretical linear power	226 W/cm
Max theoretical temperature of UO ₂	1100°C
Max theoretical temperature of clad	540°C

the core consist of a stainless steel hexagonal box containing 127 fuel pins, for a 90-cm length. Table 1 shows the characteristics of the PEC fuel element.

Above and below the active length, the pins include a reflecting zone of natural uranium oxide, 10 cm long, and extending 61 cm below to form the fission gas plenum. Underneath this reservoir, in the same can, a 25-cm-long stainless steel axial shield is contained, designed to protect the grids. The pins have a 5.8-mm external diameter, which is reduced to 5.3 mm in the plenum and inferior axial shield to permit lower pressure losses. Thus the total length of the pins is 186 cm. The stainless steel can is 0.38 mm thick, and the lower end is constrained by a small grid. The pins are separated by 10 "honeycomb" structures in the active and reflecting zone, and by 4 "egg box" structures in the plenum and lower axial shield zone.

A filter-containing foot and a shaped head for the handling of the fuel are welded to the hexagonal box. Above the pins a top axial shield of UO₂ is located, made with a set of 200-mm-long separate pins.

The total length of the elements is 3 m, the head being 16.2 and the foot 49 cm.

The fuel elements are surrounded by a crown of stainless steel reflecting elements and then by a crown of boron carbide shielding elements; these constitute the reflector and the radial shield, respectively. The reflector permits a fair reduction of the critical mass. The absorbent layer reduces the integrated dose on the vessel walls and on the internal structures, and permits the transfer into sodium of a high percentage of the heat produced in the shields.

The experimental zone is located at the centre of the core. The area available on the plan corresponds to the area occupied by 13 standard elements, for a total volume of 75 liters (see Fig. 11). Three inde-

pendently cooled, equally sized closed channels are located in this area; the cooling fluid runs through U-shaped pipes.

In the central area, three additional sites for instrumented elements are available.

Control rods are located at the edge of the core. They are formed by a pin structure, the pins of which contain ¹⁰B-enriched boron carbide. The guide boxes are similar to those of all the other elements and may be removed with the same devices employed for the other elements.

There are 9 safety rods, two of which are for compensation purposes. On the same peripheral crown of the core on which the control rods are located, three sites are available, one of which must be left free for charging operations and two may be used for instrumented elements or possibly additional safety rods.

Outside the core and in the reflector, two regulating rods and the reactivity excursion device are located.

The fuel elements of the core are cantilever installed on a system of grids (see Fig. 12) and are in contact with one another by means of spacers located in correspondence with the centreline of the core, for the purpose of checking distortions caused by an uneven temperature distribution. The grid system is formed of two tube-connected plates, housing the feet of the elements. The upper plate rests on a flange connected to the vessel; a convex bottom is welded to the lower plate. The system for distributing the cooling sodium consists of two zones: high pressure and low pressure. The "cool" sodium is led by migration pipes to the zone between the two grids. The tubes and the feet are fed through corresponding side holes, in order to eliminate the dynamic thrust on the elements themselves. In fact, the bottom of the tubes are in communication with the low-pressure zone, fed through a calibrated narrow passage. The feet of the reflecting and absorbent elements are without the side holes and are cooled only by low-pressure sodium. With this distribution of flow rate, for which a calibration of the holes is forecast according to an average power distribution curve, a hydraulic locking system for the element is obtained.

An additional lock system has been studied, formed by a set of 12 petal-shaped structures hinged at the base near the vessel walls, which may be lifted by means of proper driving rods.

Figure 13 shows a plan of the petal set. These structures are also the hot sodium mixers, and the thermocouples measuring the temperature of the sodium flowing out of the elements are installed on them. It will also be possible to install possible sodium-drawing pipes for the system for detection of

FIG. 11. Section of the Test Zone.

can failure. Figure 14 shows a cross section of an element of the lock system, including the driving device, made with a connecting rod and a driving rod. Each element may be completely removed with the closed petal. This is an important feature for extra or emergency servicing.

Table 2 summarises the main features of the reactor core. These values refer to cold and clean fuel, and to a standard situation when three channels containing a total of 26 kg of fissile material (4.5 kg of plutonium) are installed in the test zone.

Table 3 lists the values of the Doppler coefficient, the temperature coefficient, and the void coefficient of core sodium, reactivity values relative to an internal or peripheral fuel element and to the control rods of the reactor.

The sodium-void coefficient is always negative, and the negative reactivity produced by the control rods is completely adequate, especially if one bears in mind that, in case of particular core configurations, the number of the rods may be increased.

The limits which may be achieved, consistently with the physical and geometrical capacities of the

FIG. 12. Sodium Distribution in the PEC Reactor Core.

FIG. 13. Plan of Holddown Device.

channels and with the first safety investigations (the limits corresponding to a maximum charge of 35 kg of fissile material in the experimental area), permit ascertaining that experimental performances are amply sufficient to test fuel elements presently proposed for power plants.

An increased reactor performance as an irradiation device in the course of its life has been planned utilising a core of mixed uranium and plutonium oxides, with a 300-liter volume and an active length of 75 cm. In this case the reactor will be equipped with additional safety rods in the three central positions used for instrumented elements.

Reactor Vessel and Shields

The reactor vessel (see Fig. 4) is a vertical cylindrical tank, with a 3-m-diameter polycentric bottom, about 10 m high, featuring a concentric step-structured top, containing the shielding and sealing plug, and connected to the supporting structures. Two 350-mm-diameter pipes force the sodium between the

grids, and two 600-mm-diameter pipes direct the sodium outflow.

The shielding plug is made of a fixed peripheral crown and of a toroidal rotating part, whose function is related to the charging of the elements. The central part of the rotating section is crossed by a fixed hose housing the vertical extensions of all the test channels and instrumented element structures.

The sealing between the rotating and the fixed parts is obtained by liquid-metal seals. During operation, the sealing is assured by a surveyed-gap double gasket; during charging and discharging operations, a mercury charge migrates into the hydraulic toroidal seal. The shielding structure of the plug is also partially maintained in the part crossed by the test channels, by means of a proper design of the relative structures.

In order to reduce the neutron flux to a value $\leq 10^7$ n/cm² in correspondence to the concrete casting, a graphite shield enclosed in a plate liner has been planned to surround the vessel for a total thickness of

FIG. 14. Section through Holddown Device.

TABLE 2. CHARACTERISTICS OF REACTOR CORE

Volume	418 liters
Volume occupied by 3 test channels	57 liters
Active height	900 mm
Equivalent inner diameter	284 mm
Equivalent outer diameter	820 mm
²³⁵ U mass	422 kg
Enrichment	33%
Number of fuel elements	73
Element pitch	86 mm
Neutron flux at the center	$2.8 \cdot 10^{15}$ n/cm ² -s
Neutrons with $E > 0.1$ MeV	73%
Total power	116 MW
Blankets power	1 MW
Maximum power density	429 kW/liter
Average power density	275 kW/liter

80 cm, and extending in different materials to the upper portion and the plug. This shield is made of graphite blocks in which the cooling pipelines have been carved. Its exterior is formed of a 20-cm borated graphite layer.

Still proceeding outward, an anti-explosion thermal shield made of a porous material is found plus two steel plate envelopes, which act as a thermal shield for external structures and as a cooling hollow space. At present the problem of explosive excursions is being studied again, and it seems that the use of a highly strain-resistant steel plate, rather than a compactable material layer, is advisable for safety reasons.

Finally, outside this concentric shielding set is to be found the metal structure supporting the reactor ves-

TABLE 3. REACTIVITY COEFFICIENTS; FUEL-ELEMENT AND CONTROL-ROD WORTHS

Doppler coefficient (1000°K)	$\Delta k / ^\circ\text{C}$	$-2.5 \cdot 10^{-8}$
Sodium-void coefficient	Δk	-0.02
Sodium temperature coefficient	$\Delta k / ^\circ\text{C}$	$-5.5 \cdot 10^{-6}$
Inner element worth	$(\Delta k / k) \%$	0.8
Outer element worth	$(\Delta k / k) \%$	0.35
Worth of 7 safety rods	$(\Delta k / k) \%$	8.40
Worth of 2 shim rods	$(\Delta k / k) \%$	1.54

sel. It is made of a crown of pillars partially buried in concrete, the top of which supports the star-shaped structure on which the vessel is located (see Fig. 4).

Supporting structures have been planned outside the first shielding barrier to create a simpler design of these parts, which are in turn located in a protected area, and to reduce at the same time any irregularity in the shields themselves.

The central cylindrical structure in reinforced concrete has a shielding and load-bearing function for the slabs which vertically divide the reactor.

All the crossing pipes and structures have been located in the upper portion of the well housing the reactor vessel unit, to assure the containment of sodium in case of an explosive accident.

Operation and control devices are housed in a room immediately above the reactor.

Above this room a very thick reinforced concrete slab clearly separates the upper area, which is accessible even when the reactor is in operation, from the lower rooms, which cannot be entered while the reactor is in operation. On this floor, vertical to the reactor, a "transfer" cell is found, designed to receive the transient fuel from the core and the test channels.

Charging Machine and Control Rods

A choice of the type of charging system to be used has been limited by the presence of test channels in the central area of the reactor. A system using an arm-type machine, installed on the rotating part of the plug, has been chosen. This solution is considered good because it utilizes a mechanism—the arm-type machine—for which a wide research and development program is at present under way. In fact, it is employed on other projects (the French and English prototype reactors will use a similar machine).

The machine can move vertically and also rotate, and has a pantograph opening motion of the arm and allows movement and control of the gripper. This may be removed when the reactor is in operation.

The machine works in rows according to the mesh of the triangular lattice of the core to allow an approach to the experimental area and the functioning of the locking device which keeps contiguous elements in position during the removal of an element.

Figure 15 shows the part of the machine which crosses the plug; the mechanical control elements may be noted. The whole upper portion of the machine, containing motors and position indicators, can be removed without altering the sealing between the body of the machine and the plug.

Driving mechanisms for control rods are also installed on the rotating plug. They are all equipped with a device allowing the release of the absorbent

rod to permit the rotation of the plug. From the construction point of view, and apart from the driving-gear and position-indicator systems, equipping the connecting rod with a bellow seal that would also work during the scram has been studied. The rod-releasing system, for the rotation of the plug, is operated independently.

Transfer of the Fuel

Racks on which the elements decay are located immediately beyond the last crown of absorbent elements. We prefer the solution in which the exhausted elements decay in the vessel; this limits the requirements of the special cooling systems needed during the conveyance of the elements to the shipping pond, and provides an additional shielding between the core and the wall of the vessel. A set of thermal shields made of concentric steel ferrules protects the racks from nuclear heat. Furthermore, since routine transient conditions are foreseen, the walls of the vessel are padded with a set of special shields.

The machine-rotating-plug unit serves the whole core and rack area, and it can lift and deposit elements in two points, located near the walls of the vessel immediately above the core, designed for the fuel transfer.

The fuel is transferred from the reactor vessel and test channels to the "transfer" cell (see Fig. 4). This cell is alpha-tight to permit the handling of any kind of fuel, even if damaged. By means of a sealed cylindrical channel it is connected to a passage in correspondence with one of the two transit points located in the vessel. A mobile machine on rails underneath the ceiling of the cell lifts the elements by means of a rigid-rod device.

As far as the test fuel is concerned (see Table 4), the experimental channels are extended—through the hose located in the room above the reactor—to the cell floor level, which is furnished with equipment for the assembling or removal of test sections, of the relative structures, and of the long holddown rods. The transfer machine is equipped with a gas cooling system that is able to cool the test sections which must be removed from the reactor only a few hours after shutdown, and thus have a decay power of about 35 kW. It also includes a cutting machine for disassembling the structures, an electric handling device, and auxiliary mechanical manipulators.

Since the discharging operation of test sections is rather difficult, the possibility is being studied of transferring them by means of a special compact container in which they are sodium cooled. Some sodium-cooled decay wells for test sections are also located in the transfer cell. An argon atmosphere is maintained

FIG. 15. Arm of Fuel-handling Machine.

in the cell, which is connected to the hot cells located outside the containment building by means of a "connecting" cell. This layout will greatly reduce the possibility of contaminating the room above the reactor, permitting an easier routine servicing of the operation equipment.

Furthermore, we think it advisable to limit the performance of the cells inside the containment building to transit operations, so as not to interfere with the experiments on irradiated sections and the operation of the reactor.

Handling of the Fuel

The fuel is handled in a set of alpha-tight cells located outside the reactor.

In Figure 6 the path with arrows leading from the reactor indicates the path of irradiated fuel; the path with arrows leading to the reactor shows how fresh fuel is introduced. Each driver-zone element reaches the operation cell through the connecting cell. In the operation cell, the fuel is introduced into a washing and selective analysis station to ascertain the presence

TABLE 4. TEST-SECTION CHARACTERISTICS FOR A "PCC 2" FUEL ELEMENT

Useful volume of one test channel	6.24 liters
Active height of test channel	900 mm
Useful diameter of one test channel	94 mm
Test-section shape	hexagonal
Distance across flats	68.8 mm
Wrapper thickness	2.25 mm
Number of pins	61
Pin outer diameter	6.4 mm
Clad thickness	0.3 mm
Pitch/diameter ratio	1.26
Weight of ^{239}Pu + ^{241}Pu	2 kg
^{235}U weight	6.7 kg
Linear power	700 W/cm
Volumetric power	1170 kW/liter
Total flux at the center	$2.8 \cdot 10^{15}$ n/cm ² -s
Time to reach 100,000 MWd/t	390 d
Time to reach 10^{23} nvt	413 d

of damaged elements; successively the front and lower extremities, which are not to be reprocessed, are cut; if the element is damaged it is sent to encapsulating stations where it is closed in a container

with melted lead. Finally, the elements and the containers reach—through the decontamination cell—the storage and shipping pond.

A gas-cooled dry storage place is planned in the event it becomes necessary to remove the whole core of the reactor for an exceptional servicing operation.

The test fuel (see Fig. 16) must also be washed. Afterwards nondestructive tests are made before the fuel is sent to the laboratories for postirradiation tests. Dimensional, photographic, and radiographic tests are performed on the whole test section. The test elements can eventually return to an assembling and disassembling station for a replacement of the pins before a new irradiation period.

Usually, after the comprehensive tests, the section is disassembled in the cutting station, and each pin passes through the nondestructive-test cell where a visual, dimensional, and photographic test will be carried out, as well as the eddy-current-system measurement of the bonding, a survey for failures by

means of ultrasonic and infrared examination, an examination for leaks, measurement of fission gas release, weight and density determinations, gamma spectrometry and densitometry, autoradiography, and radiography.

Finally, all the parts of test sections are encapsulated and sent to the pond through a decontamination cell. Since the sections of the channels located in the reactor have a short life due to the integrated flux to which they are submitted, their partial removal has been foreseen in a cask supported by the main-bridge crane, as well as their disassembling by means of the cutting machine located in the transfer cell above the reactor.

Cooling Circuits of the Reactor

Cooling is performed by means of 4 separate sodium circuits: one for the core and three for the test channels.

The cooling circuit of the core is made of two pri-

FIG. 16. Scheme of Operations for Test Fuel.

Fig. 17. Scheme of Reactor Cooling Circuit.

mary parallel branches and two secondary branches that are completely independent. The secondary branches receive heat from the primary ones in correspondence to the intermediate heat exchangers and disperse it into the atmosphere by means of a sodium-air exchanger group. Figure 17 shows a scheme of one of these branches.

The primary circuits of the core are of the semi-integrated type: the components, two heat exchangers, and two pumps are immersed in a cylindrical, vertical-axis tank filled with sodium, where the delivery and return pipes for the cooling liquid metal terminate (see Fig. 8). The sodium from the reactor migrates through the shell side of the heat exchangers into the tank from which it is aspirated by means of pumps.

The primary centrifugal pumps are driven by a variable-speed motor and are equipped with an embodied check valve. Like the reactor vessel, the pipes

and the tank are double-walled to avoid the risk of coolant leaks. The gap is used for a gas preheating of the reactor vessel. Parts of the pipes crossing the shields are electrically preheated by means of resistances located between the exterior wall and the insulating layer. Leak detectors are installed in the gap.

The secondary circuits are formed of a centrifugal pump, an expansion tank, and a sodium-air heat exchanger. Low-pressure argon gas is maintained in both primary and secondary circuits, at all sodium-free levels, by means of proper circuits. In addition to cover-gas circuits, sodium-cleaning circuits are also provided. The cleaning circuit of the primary circuits of the reactor also takes care of overflow and refill tasks.

All primary circuits and their auxiliaries are located inside the safety-containment building. In both the cleaning and the cover-gas circuits it is possible to

TABLE 5. OPERATING CHARACTERISTICS OF REACTOR AND TEST-CHANNEL COOLING CIRCUITS

	Primary	Secondary
Reactor		
Power (MW) ^(a)	58	58
Volume flow rate (m ³ /hr) ^(a)	1035	1297
Mass flow rate (kg/s) ^(a)	306	303
Outlet temperature (°C)	525	475
Inlet temperature (°C)	375	325
Temperature difference (°C)	150	150
Test Channels		
Power (MW)	6	6
Volume flow rate (m ³ /hr)	138	136
Mass flow rate (kg/s)	32	32
Outlet temperature (°C)	600	530
Inlet temperature (°C)	450	400
Temperature difference (°C)	150	150

^(a) Each parallel branch.

install fission-product-cleaning equipment in case a vented fuel is used.

With reference to cooling circuits, the following safety measures have been applied.

In case of interrupted operation of a pump, dangerous temperatures must be avoided in the fuel and the sodium, even when the control system has not intervened. Electric feeding is provided by means of an emergency plant. When the reactor is shut down, a charge sufficient for the evacuation of the decay power must be assured by natural convection. Table 5 shows the operation features of the primary and secondary circuits.

The reactor circuit extracts the power of 116 MWt with a 150°C temperature jump between the entrance to and the exit from the core with a total flow rate of 2,610 m³/hr. Pressure losses will be around 5 kg/cm².

The dispersal of heat produced in test channels is assured by three independent circuits, located in the sodium building annex to the reactor building (see Fig. 9).

Table 5 shows the operation conditions of a channel in the first version that will be adopted.

A 6-MWt power can be extracted from each channel, with a 140-m³/hr flow rate and a pressure loss of about 10 kg/cm². Figure 18 shows the scheme of one of these channels. The principal components (pumps and exchangers) are immersed in a tank, as are the components of the cooling circuit of the reactor. Each couple of pipes terminates at its component tank, and each tank is located in one room of the safety-containment building, separated from the others by shielding concrete walls.

Each secondary test circuit is located in the sodium building and is composed of a circulating electro-

magnetic pump, a sodium-air heat exchanger, and expansion tank. The arrangement is the same as that of the reactor secondary circuits (see Fig. 9).

Each primary test circuit is provided with its own cleaning and oxide-control circuit, as well as a cover-gas circuit.

The cleaning secondary circuit is in common with the three test circuits and the secondary circuits of the core of the reactor.

The possibility of using the secondary circuits of the reactor as a test bed for a steam generator has been studied.

Shields and Room-cooling Circuits

Several gas circuits have been designed for cooling shields and rooms. This installation is composed of the circuits for the preheating of the reactor vessel and the shield-cooling circuits. It is equipped with a thermo-fluid secondary circuit. Other special installations extract heat from the plug of the reactor and the cells. Cold traps and fuel-decay wells are cooled by means of circuits containing NaK.

For the ventilation of the reactor building, a reduced O₂ (<5%) atmosphere is maintained in all the rooms underneath the working floor, to avoid the risk of a sodium fire.

A normal atmosphere is found on the working floor. The rooms underneath it are made accessible one after the other, when the reactor is shut down, ventilating them with air drawn from the working floor.

The atmosphere of the lower rooms is constituted of a gas circulating in a closed cycle on a refrigeration battery to extract the heat discharged in these rooms, especially through the thermal-insulation leaks.

Also on the working floor, a closed-cycle conditioning is provided, with surveyed admissions and ejections of fresh air. Air or gas from the reactor vessel or the fuel-handling building are released into the atmosphere through a set of absolute filters and an expulsion stack.

Control and Instrumentation System

The PEC control and instrumentation system is constituted of nuclear, health physics, and conventional instrumentation, the safety system, and the data-processing system.

The nuclear instrumentation includes: two impulse-type startup channels, utilised in a 1:2 ratio; three current period channels; reading and safety linear channels, three with automatic change of scale, utilised in a 2:3 ratio; and a regulation channel.

The health physics instrumentation provides control of the radiation level in the plant premises by means of zone monitors for atmospheric dust and gas

FIG. 18. Scheme of Test-channel Cooling Circuit.

activity, and by means of atmospheric dust, gas, and liquid sampling devices.

Conventional instrumentation is provided for: the measurement of temperatures, flow rates, levels, and pressures necessary for the plant operation control; the measurement of the sodium exit temperature from each fuel element; an "on-line" rapid control system for the integrity of the fuel elements, which permits the monitoring of the initial sodium boiling and of eventual local damages at the cans. A visual system through sodium is also being studied which permits the individualisation and identification of objects, the survey of charging and discharging operations of fuel elements, and other mechanical operations under sodium.

The "fail-safe" safety system processes the signals received from the plant, properly intervening when abnormal conditions are created.

The data-processing system is realised with the help of an "on-line" numerical computer which provides the periodical recording by alarm and turbography of the main variables of the plant:

- the recording of changes in the state of control devices of the plant;
- the selection of alarm sequences;
- the execution in real time of pre-established calculations (heat balances, burnup, integrated flux, thermal power, etc.).

The possibility of employing the computer for the survey and inspection of charging and discharging operations of the fuel elements is being studied.

Buildings and Conventional Plant Facilities

The buildings housing the reactor and relative plant units are located around the metal containment building. They form three main blocks, located at

FIG. 19. General Plan.

90° (see Fig. 19). On the free side is the entrance to the containment building with its large components. The metal containment building is formed of a 25-mm-thick sheet steel cylinder, with a 23-m diameter, a hemispheric top and polycenter bottom, and a 47.5-m total height.

A building block located along the axis connecting the reactor vessel and the components tank in the containment building contains the hot cells and pond unit. Perpendicularly to the axis of this building is located the block housing the control room, the laboratory board rooms, the offices, and sanitary facilities. Conventional plants are installed in a parallel wing.

Opposite is the third block, constituted of two parallel wings: one for the sodium installations, the other for gas and liquid waste disposal.

The conventional installations and services system assures the feeding and equipment necessary for operation to the reactor unit.

For the electric system, an emergency plant equipped with continuity groups and prompt-intervention diesel-operated groups is provided: the former feed the loads which cannot bear 10-s breaks, the latter feed the remaining priority loads.

The conventional plant system also includes the thermo-ventilation equipment, conditioning of buildings annexe to the reactor, the thermo-hydraulic plant, and other minor auxiliaries.

Discussion

Mr. Moreau (EDF): What is the schedule for PEC?

Mr. Aiello: The contract for the construction has

been granted. The plant will be operating at the present schedule by the second half of 1973. The actual construction will begin about the middle of 1969.

Mr. Harde (Interatom): You indicated that at least three channels will be instrumented. What kind of instrumentation are you proposing?

Mr. Aiello: I cannot answer this question because we are the designers of a reactor which must be used by experimenters to test fuels. Our task has been to provide for instrumentation. We have provided for two types of instrument lead-ins: thermocouple leads and small flexible tubes. Routing of the in-core instrumentation in the test channels is not part of the design of the reactor; it is part of the experimental work to be done.

I don't want to escape the question, but I know that the type of instrumentation is not defined at this time, or at least it is only partially defined for the thermocouples. With this type of reactor, there is also the possibility of installing other types of instrumentation. It is only necessary that this instrumentation terminate with cables that are rather flexible and of a diameter between 1 and 2 mm.

Mr. Harde: I didn't understand whether you would provide for testing the elements to destruction. In other words, could your design cope with the possibility of rather severe damage to the fuel elements with respect to radioactivity?

Mr. Aiello: The design has been, shall we say, divided in two different phases. In one phase, elements can be tested in closed channels, while in the other, tests outside closed channels may be conducted according to the severity of the tests, but without severe rupture of the element (something like an explosion) actually occurring. Further, in the space in which three test channels of the latest design are normally located, one special channel may be installed that is specially designed to accommodate a test element in which complete destruction of the element may be expected.

Mr. Golan (AI): Do you plan to do any testing of components or any R&D before you proceed with the construction of PEC?

Mr. Aiello: Yes, this reactor has been designed primarily as a test facility and not as an experimental plant. This means that if constructive solutions to design problems have some doubt because of the novelty of the solution itself, they have been discarded insofar as possible; in cases for which solutions have not been completely proven or tested, a prototype will be constructed. Prototypical tests will probably be made over the next three years. These tests comprise, primarily, operational testing of the more

complicated components and those components not already existing on the market. A good example of a component which would be tested is the arm-type transfer machine. Another example is the handling machine to be used in the alpha-tight upper cell. Special tests of smaller devices also are planned. As far as the test channels are concerned, we are thinking of having a long development program, because these test channels have not yet been studied by anyone. When they are first used in the reactor, the test channels will contain elements for which no severe experiments will be made. As experience is gained, more and more severe conditions will be studied in the test loops.

Mr. Melese (GGA): Where do you test your driver fuel? Do you test it in the reactor itself? Also, what are the costs (R&D and construction costs) of the PEC reactor and the ways of financing them?

Mr. Aiello: The first question I can answer; the second, I am afraid I cannot. We are now conducting a program in Italy which foresees the development of fuel. It is not yet definite that this development program will include the driver fuel for the PEC reactor, because the reactor is a test facility for experiments. Most probably the initial experiments for the driver charge of the core will be made in already existing sodium-cooled reactors. We have already made contacts with representatives of these existing reactors. If these reactors are not exactly equivalent radiation facilities, the test results for the driver charge can be acceptable because the driver fuel is of a low-performance design. Additional improvement of the driver fuel can be made on the basis of experiments done during the operation of the reactor itself.

Mr. Cohen (Chairman): In the calculations that you showed at the beginning, I presume that the existence of the minimum, with specific power, of your conversion ratios, and so forth, was due to the increase in the amount of steel in the core as you reduced the size of the pins; is this correct?

Mr. Bruzzi: I think it is due to the decrease of the fertile material, too, because as you increase the specific power you have to decrease a certain amount of uranium-238. May I say that there is a penalty that you are accepting in the breeding gain in this way.

Mr. Cohen: Is there any tentative schedule for this plant?

Mr. Bruzzi: We are now proceeding to end preliminary studies regarding the 1000-MW plant at the end of the next year. We are proceeding to start the design of a prototype or demonstration plant.

Mr. Okrent (ANL): I have a question on PEC. It wasn't clear to me what measures you are taking to protect against leakage from pipes in the primary system so that the core is not uncovered due to such leaks.

Mr. Aiello: Do you mean what type of precautions do we take to prevent leakage from the primary system to the environment? First, we have two envelopes for all the primary circuits. Second, as I have said, we have two zones in the reactor containment building. The primary circuit is normally under an atmosphere consisting of depleted air with an oxygen content of about 5%. This is a precaution against sodium fires. Hence, the primary circuit is separated rather well from the other zone to which men are admitted. The first zone, with proper ventilation and monitoring, is a barrier between the reactor and the personnel when the reactor is operating.

Mr. Kintner (AEC): I wonder if you can say a little more about the test capability of the reactor in terms of the number of loops, the types of loops, their size, temperatures, and so forth. For example, how many pins can you test at any one time in any one loop?

Mr. Aiello: This reactor is conceived as a reactor with an experimental cavity. A wide range of experiments is possible by changing what is inserted in the cavity. Mainly we foresee three U-shaped test channels served from the top, and three experimental elements inserted in the same region of the core and

cooled by the same core coolant. This is the reference design. The only limitation is the space available, which corresponds to thirteen core elements. Now in this design which is used as the reference, the test channels have 6.24 liters of useful volume and an active height of 900 mm. The elements of the core are 86 mm across flats and also 900 mm in active height. These elements are directly accessible from the top, which means two things: they can have direct connection to on-line instrumentation, and they can be inserted and discharged in the top cell. This cell provides an alpha-tight environment. Other experimental positions, obviously, are available in the core because every element of the core can be replaced with an experimental one; it is only a question of having the correct critical mass and sufficient cooling capacity. To provide cooling, a special type of flow distribution is used. The tubes connecting the two plates (constituting the grid system) have lateral holes. If an element which is installed in the grid has lateral holes, it will receive coolant from the high-pressure system. If the element is built with no lateral holes but only has a bottom opening in it, this element will receive coolant from the low-pressure system. This means that in every position of the core, two types of cooling can be obtained. If the experimental element has a high power rating, as the core elements do, it will be equipped with lateral holes. If a material testing element is used, it will be provided with a bottom opening for coolant flow.

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Development and Evaluation of the Combustion Engineering Advanced 1000-MWe LMFBR Design*

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1. Introduction

The U. S. Atomic Energy Commission (AEC) has established the highest priority for the development of a safe, reliable, and economic Liquid Metal Fast Breeder Reactor (LMFBR) power plant for the utility environment. As an early phase of this development, studies are currently under way at Combustion Engineering and other contractors to develop conceptual designs for liquid metal fast breeder reactors. This program is known as the "1000-MWe LMFBR Follow-on Study," since it is based upon studies performed for the AEC a few years ago.^(1, 2) The scope of the Combustion Engineering Follow-on Study is in the area of the heat-generation system of the nuclear steam-supply system. Consequently, the reference design comprises the reactor only and not the entire plant.

Dietrich, in a recent paper presented at the American Power Conference,⁽³⁾ showed that for a breeder reactor to make a significant contribution to the solution of the fuel-supply problem, it must be a great deal more than a nominal breeder—it must be a good breeder. Actually, this is also necessary to assure that economic competitiveness is achieved. Dietrich showed that the introduction of a system of breeder reactors with a 7.2-yr doubling time would show a total cumulative ore requirement for the United States of only one-fourth that of breeder reactors with a 20-yr doubling time.

Consequently, in our studies under the "Follow-on Study" contract, we have taken as our target the highest performance attainable with technology that reasonably can be expected to result by approximately 1980 from currently definable research and development programs. By performance, we mean those operating characteristics that affect power-generation cost and breeding. These two goals are basically compatible, and a high breeding performance will be an asset in terms of generation cost at any time, but this will become especially important as uranium becomes more scarce and more expensive.

This paper is primarily concerned with the three subjects which bear most directly on reactor performance: safety, the fuel system, and core optimization.

Questions that are considered in the safety area include the void effect and spoiled geometries, containment of Bethe-Tait excursions, protective systems, factors relating to the inherent stability of the core, and special effects related to sodium-bonded fuel. In the fuels area, three systems were studied in some detail: sodium-bonded carbide, gas-bonded carbide, and gas-bonded oxide. For each fuel, optimized core configurations were developed that had approximately equal safety characteristics. Optimization was done by a systematic method that involved relatively extensive thermal, hydraulic, physics, and economic calculations for a number of configurations.

The reactor design developed from these studies is a moderately pancaked cylinder fueled with sodium-bonded plutonium-uranium carbide. The void effect is moderately positive, and a small amount of BeO is present in the core to increase the Doppler effect. Poison elements are used for control. The regulating and safety functions are preformed by separate sets of rods.

With a specific inventory of the reactor system of 3.5 kg fissile MWe and a breeding ratio of 1.5, a compound doubling time of 7 yr is achieved. The fuel cycle cost is calculated as 0.38 mill/kWh.

A more detailed description of the reactor is presented in the final section of the paper, and the major performance characteristics are listed in the Appendix.

2. Safety Considerations

The AEC General Design Criteria were reviewed in the Follow-on Study⁽⁴⁾ and interpreted in terms of LMFBR design. From these, specific criteria were developed that represent practical ways of satisfying these requirements. These criteria have formed the basis for the safety guidelines and safety design features incorporated in this reactor. A summary of those guidelines most influential in the design, together with brief statements of the manner of their implementation, is presented here.

Design of Reactor Core

The design of the fuel rods and fuel subassemblies should be such that clad failures do not tend to propagate from rod to rod, and do not tend to cause loss of

* A portion of the results presented in this paper were obtained in the 1000-MWe LMFBR Follow-on Study being performed under ANL Subcontract 31-109-38-1999.

the sodium bond between clad and fuel. Furthermore, the fuel subassemblies should be designed so that total plugging of the coolant inlet of one subassembly by single objects is not possible, and coolant boiling and/or fuel damage from partial or localized plugging should be prevented from propagating to adjacent subassemblies before appropriate action by a protective system is accomplished. The reactor core should be designed to permit operation with failed fuel and allow detection of clad failures before the equivalent of more than one subassembly is involved.

Reactor Stability

The reactor system with its related controls and protection systems should be designed with appropriate margins for uncertainties to be sufficiently stable to prevent exceeding acceptable limits of fuel damage during transients. The physics and thermal design of the reactor should be such that the Doppler negative reactivity effect is several times larger than any prompt positive reactivity effects from corresponding changes of power and temperature. The magnitude of the Doppler effect should be sufficient to limit power and temperature excursions resulting from the most severe credible single-equipment failures or operational errors to values which yield temperatures less than the boiling point of the coolant or bond and the melting point of the fuel.

The internal mechanical design of core and reactor should be such that structural distortions due to temperature and/or coolant pressure or flow gradients or changes do not cause unmanageable reactivity effects. Mechanical restraint of the core and fuel should not unduly inhibit natural radial and axial thermal expansion.

Core Protective System

A protective system should be provided that is sufficiently reliable, redundant, and independent to prevent or suppress conditions arising during credible accidents that could result in exceeding acceptable limits for fuel damage. Two independent systems for reactivity control, preferably of different principles, should be provided. No credible accident should produce unmanageable reactivity changes from sodium voiding or fuel failure and/or meltdown. Localized voiding or fuel failure in a few fuel subassemblies may occur as a result of localized failure (e.g., plugging); however, such failures should not produce reactivity changes that are large with respect to the inherent negative reactivity feedbacks of the system (Doppler, structural, etc.). Credible accidents that involve the core as a whole (e.g., loss of the primary pumps, or control-rod ejection) should not result in any sodium

voiding or fuel melting. The protective system should be designed with redundant backup trips which prevent fuel melting or boiling of the bond and/or coolant should the first level of protection fail to respond properly to equipment malfunctions or operational errors.

Boundary of Reactor Primary Coolant System

An independent secondary boundary should be provided for critical portions of the reactor primary coolant system which, together with appropriate complementary systems, will prevent any primary-coolant-system rupture from resulting in loss of coolant sufficient to prevent adequate emergency core cooling. Siphon breakers should be provided in the primary system that will prevent drainage of the reactor vessel below the level of the inlet nozzles in the event of a primary-system rupture at a level below this point. A large enough volume of sodium in the primary system should be provided so that any rupture in the reactor vessel or emergency cooling system will not lower the sodium level below a safe level above the primary outlet nozzles.

System for Emergency Core Cooling

A highly reliable system for emergency core cooling should be provided. The main loops should be relied upon to handle the initial effects of a major pipe rupture or other cooling fault that occurs while the reactor is at power. After the system is shut down, operable main loops should be able to handle most requirements for emergency cooling, either by natural convection or with pumps operated by emergency power. Independent backup emergency-cooling loops should be provided unless it can be shown that one or more of the main loops will always be operable.

Reactor Containment System

A reactor containment system should be provided that is designed for various gross equipment and/or system failures that have been stipulated and justified as being reasonable limiting cases with respect to loads imposed on each component of the containment system. Failures that should be considered include, but are not limited to, a major rupture of a primary pipe, a severe power/flow mismatch in the core leading to coolant boiling, and a major system failure leading to exposure of primary coolant to the containment-building atmosphere. The consequences of such failures should be evaluated conservatively, considering each to occur independently and assuming coincident failure of a single active component of the protective system.

The specific criteria discussed above can be sum-

characterized by the two following basic safety criteria, which must be satisfied by the design of the safety system combined with the inherent safety characteristics of the reactor core:

1) No credible accidents, including those in which portions of the protective system fail to respond, should produce either gross meltdown of the fuel or gross voiding of the core.

2) Even if complete failure of the plant control and protective system occurs, any resulting accident should be safely contained.

With this in mind, a safety analysis was performed in an effort to develop a reactor core and safety system to meet these criteria. Areas that were considered include the void effect and spoiled geometries, containment of Bethe-Tait excursions, protective systems, factors relating to the inherent stability of the core, and special effects related to sodium-bonded fuel.

2.1. Core Configuration—Cylindrical vs Modular Core Geometry

Although it is generally apparent that current fast reactor designs can incorporate a safety system which will meet the above criteria, it is not obvious whether some of the designs might be inherently safer than others or might meet the requirements with much less design effort or cost penalty. The question now appears to be this: Does a core designed with a zero or negative sodium-void coefficient have a significant safety advantage over a core with a small positive coefficient? Obtaining a negative sodium-void coefficient in a practical 1000-MWe core with unspoiled geometry is not possible. Therefore, in order to answer the above question, a comparison was made of the safety characteristics during protected and hypothetical accident situations of an optimized modular core with a slightly negative void effect and a cylindrical design with a positive void effect. The optimized cores were the results of a survey made during the Task I⁽⁵⁾ portion of this follow-on study. Neither of these designs contained control rods or moderating material; thus, they were compared on a relatively equal basis.

In making such a comparison, there are always other factors that must be considered before a clear choice can be made. Therefore, this comparison actually involved a trade-off study of the two mentioned designs and considered items such as fuel-cycle economics and breeding, as well as safety. Only those portions of the reactor that were included in the follow-on study were considered in the trade-off study.

2.1.1. Economics and Breeding

Both cores, cylindrical and modular, were separately optimized in surveys similar to that described later in

TABLE 2.1. COMPARISON OF OPTIMUM MODULAR AND CYLINDRICAL CORES

	Cylindrical	Modular
Fuel-cycle cost (mill/kWh)	0.38	0.68
Compound doubling time (yr)	6.8	10.4
Total breeding ratio	1.55	1.55
Seed conversion ratio	0.996	0.712
Sodium-void coefficient ($\% \Delta k/k$)	+1.70	-0.14
(seed plus axial blanket)		
Isothermal Doppler coefficient (Tdk/dT)		
With sodium	-0.0046	-0.0054
Without sodium	-0.0031	-0.0030
Seed specific power (kWt/kg)	114.0	141.0
Seed plus axial blanket power fraction (%)	88.0	61.0
Specific inventory (kg fiss/MWe)	3.22	5.81
Core height (ft)	2.0	4.0
Fuel-slug diameter (in.)	0.35	0.30
Radial-blanket fissile plutonium composition (%)	5.0	4.5

this paper (see Sect. 4.1). The parameters varied were seed height, fuel diameter, and enrichment of the radial blanket. Thirteen modular cores and ten cylindrical cores having different combinations of these parameters were studied. Safety was considered in this portion of the survey in that the optimum modular design was required to display a zero or negative sodium-void coefficient while the cylindrical cores could be positive. The characteristics of the optimum core resulting from each survey are summarized in Table 2.1. Based on fuel-cycle costs and the breeding performance of each design, there is no doubt that the cylindrical arrangement has a substantial advantage over the modular arrangement.

2.1.2. Safety Comparison

The question that remains is: How much importance should be attributed to the difference in sodium-void effect between the two arrangements, that is, between a cylindrical core with a positive sodium-void coefficient of $\sim +1.7\% \Delta k/k$ and a modular design with a slightly negative coefficient of $\sim -0.14\% \Delta k/k$? Our accident studies indicate that only four of the many possible accidents might be affected by the change in core design. These are:

a) Credible Accidents:

- (1) Local subassembly plugging, and
- (2) Gas bubble accident;

b) Hypothetical Accidents:

- (1) Core meltdown, and
- (2) Gross core voiding

Therefore, only these accidents are discussed for comparison purposes in terms of core response for the two designs.

Local Subassembly Plugging

The two credible accidents in which the sodium-void coefficient has any effect on the nature of the malfunction are: (1) the blocking of coolant flow to one or a few subassemblies, subsequently resulting in subassembly voiding, and (2) the passage of relatively large gas bubbles transported by the flowing coolant through the core. The first type, local subassembly plugging, will most likely occur over a long period of time and would be detected by local outlet temperature thermocouples before a voiding condition is approached. However, if it is undetected, as in the Fermi incident, it would cause sodium boiling and subsequent subassembly voiding. Events preceding sodium boiling are similar for either reactor design. Only the reactivity change following voiding is dependent on the sodium-void coefficient, and this is very small for one or a few subassemblies in both core designs. The effect is so small that the voiding of a few subassemblies cannot reliably be detected immediately by any of the gross systems monitoring a core parameter. Therefore, in the event of undetected local subassembly plugging, fuel damage is unavoidable in either core, and the reactivity effect is small enough so that, even if positive, its overall effect on the core is not detrimental. The important objective of the safety system in both cores is to prevent the local failure from propagating to adjacent subassemblies. Requirements for achieving this objective are the same for either core type. Thus, there does not appear to be any significant difference from a safety aspect between the modular and cylindrical design following a local plugging accident.

Gas-bubble Accident

The gas-bubble accident is somewhat different in that no sodium boiling occurs unless the bubble is large and remains in the core long enough to elevate

FIG. 2.1 The Effect of Void Size on the Clad Temperature in a Gas-bubble Accident.

the clad temperature beyond the boiling point of the coolant. The combined effect of the reactivity change plus the temporary loss of the fuel-rod heat sink caused by the passage of the gas bubble determines the maximum clad temperature during the transient. Because the reactivity change can have a significant effect on the resulting temperatures, especially for large bubbles, it is necessary to investigate several bubble sizes. Furthermore, a positive sodium-void coefficient will produce higher temperatures for most sizes of bubbles than either a zero or negative coefficient. This would appear to be a decided safety advantage for the core with a zero or negative void coefficient. However, as already stated, the dimensions of the bubble are extremely important, and it appears that a very large bubble is necessary to create a positive reactivity addition large enough to generate conditions which are hazardous to the core. Figure 2.1 shows the results of the analysis of gas-bubble incidents and indicates that a bubble large enough in diameter to enter instantaneously at least 19 fuel subassemblies and maintain a void height of about 15 in. in the core is necessary to create possible failure conditions. The void height of 15 in. creates the worst reactivity effect axially which occurs when approximately two-thirds of the seed region is voided. This means that the actual bubble size at the core inlet must be about 2.8 ft in diameter by 0.5 ft high, with all portions passing evenly through the core. By proper reactor design, i.e., equipment for gas disentrainment, flow baffles, and gas-detection equipment, the probability of bubbles this large ever reaching the core can be expected to be eliminated for practical purposes.

Furthermore, it is also evident that gas bubbles of a similar volume to that discussed above but of a different shape can create hazardous conditions in a core with a zero or negative void coefficient. For example, a bubble with the same volume and a diameter of about 1.1 ft that covers four subassembly inlets will cause clad or fuel damage in a core with a zero or slightly negative sodium-void coefficient, as well as in the core with a positive effect. This bubble creates an ~6.0-ft-high void in these four elements and causes failure because of the prolonged loss of heat sink. In conclusion, therefore, it can be said that large gas bubbles must be eliminated from all fast reactor designs and that a means to do this must be designed into the safety system of any plant. Thus, the effect of a positive sodium-void coefficient has little significance on the safety systems required for the plant.

Core Meltdown

The usual method for calculating reactivity ramps due to fuel meltdown is to assume a homogeneous dis-

tribution of the resulting geometry and density changes over the whole of the reactor seed. This accident was simulated by increasing the fuel density while at the same time the seed volume was reduced. Accordingly, the results show a continuously increasing reactivity, the ramp of which was determined by assuming an acceleration of the core center of gravity by one g. Calculations using this model resulted in a ramp of ~ 65 $\$/\text{sec}$ for both the cylindrical and modular designs. This indicates that the consequences of a meltdown should not be significantly different in either of the configurations.

Gross Core Voiding

The effects of the sodium bond on voiding behavior are discussed in Sect. 2.2, and the results of studies of gross voiding due to sodium boiling in the cylindrical reactor are presented in Sect. 2.3. The calculations indicate that the theoretical upper limit for the reactivity effect due to voiding is about $\$20/\text{sec}$ for the cylindrical (positive void effect) core. However, gross core voiding can only occur as a consequence of a major failure of the cooling systems coincident with failures of primary and backup protective systems. Such failures would also lead to meltdown of the core, the potential consequences of which have already been discussed. The conditions required to produce gross voiding—failure of cooling and protective systems—would, therefore, regardless of the void effect, lead to destruction of the core more or less violently, depending on the reactor configuration, and on the complex and unpredictable sequence of events which would follow such failures.

Because rather special and unlikely conditions of coherence in time and space are required to produce violent core destruction due to voiding and/or meltdown, a violent Bethe-Tait type of event is not very probable in any core arrangement. Nevertheless, so long as the limiting reactivity effect of gross voiding is less than the limiting effect of meltdown, consideration of the void effect only lessens the potential consequences of these hypothetical events. Only if critical reassembly of the core during meltdown is eliminated as a safety consideration does a zero-void-effect arrangement gain a theoretical advantage in the realm of hypothetical accidents.

Conclusions

A flat, cylindrical core with a moderately positive sodium-void coefficient can be designed to meet the basic safety criteria that are required to obtain an acceptably safe and reliable reactor. So far as the void effect is concerned, this requires:

- 1) the prevention of large gas bubbles;
- 2) the detection and prevention of the propagation of local plugging;
- 3) prevention of the loss of coolant;
- 4) a provision for Bethe-Tait containment.

Furthermore, an analysis of the modular design with a slightly negative sodium-void coefficient has indicated that these same provisions are required. Therefore, since the cylindrical design is superior with respect to fuel-cycle costs and breeding, it was selected as the reference 1000-MWe reactor concept.

2.2. Sodium-bonded Fuel

One feature of our 1000-MWe LMFBR design which is unique when compared to most other designs is the sodium bonding of the ceramic fuel to the clad. This bonding medium provides a high conductance across the fuel-clad gap and therefore allows operation at a much higher linear power (i.e., other limitations being equal) when compared to a more conventional gas-bonded fuel rod. The design of the rod is such that the probability of losing this sodium bond during normal operation is extremely low, that is, the fission gas produced within the fuel is continually released to the coolant by a vent at the top of the fuel rod. This maintains the pressure inside of the fuel rod always below the coolant pressure outside, thus eliminating any pressures large enough to force the sodium bond out of the pin. Thus bond loss can only occur as a result of simultaneous clad failure and vent failure.

However, if during operation the sodium bond is somehow removed from the gap, the gap conductance would drop to a low value based on a mixture of fission gases and sodium vapor. The fuel rod will remain operating at its design power, but the fuel temperatures will increase. The safety implications of bond loss have been analyzed during both normal and abnormal operation, and have indicated some rather interesting results, particularly for the loss-of-flow type of accidents. The results of some preliminary calculations performed with FORE⁽⁶⁾ are discussed herein.

2.2.1. Loss of Bond during Normal Operation

In this analysis, it was assumed that the fuel-clad bond is changed from sodium to gas (i.e., fission product gases plus sodium vapor). Calculations were performed for a fuel rod at different total burnup or lifetime levels. This variable is introduced by changing the fuel-clad gap thickness (which changes due to fuel swelling) and therefore the heat-transfer coefficient of the fuel-clad gap. The reference fuel rod has a 0.014-in. gap at the beginning of life, which is reduced to approximately zero (or contact) at the end of life. The gap resistance, assuming gas instead of sodium, is very high at the beginning of life and remains quite

high until the fuel surface expands to within ~ 1.0 mil of the clad much later in its life. Heat-transfer calculations, assuming this behavior, have indicated the following results:

a) Gross fuel melting will occur over most of the fuel-rod lifetime ($\sim 90\%$) if bond loss occurs suddenly and the fuel-clad gap does not close rapidly.

b) If loss of sodium bond occurs relatively slowly, the swelling associated with increasing fuel temperatures would likely close the fuel-clad gap sufficiently to prevent significant melting.

c) Whether the bond loss is fast or slow, fuel-clad failure is likely, due either to a molten fuel-clad interaction or to excessive fuel swelling.

What will happen after clad failure occurs following either sequence of events is not predictable with any certainty at the present time. The only experimental evidence now available⁽⁷⁾ is a single test in which neither melting nor clad failure occurred following loss of the bond. However, eventual clad failure was expected from excessive fuel swelling if the experiment had been continued to higher burnup. With proper fuel-rod and subassembly design, such failures can be prevented from affecting adjacent rods. Direct experimental data for the molten fuel situation are not available, but there is indirect evidence⁽⁸⁾ that this type of failure could cause rapid clad penetration by fuel which could affect adjacent rods. New experiments are required to determine the extent of damage under such circumstances. It is unlikely, however, that damage would be sufficiently extensive to cause a significant public hazard.

From the operational point of view, it is obvious that the design of the sodium-bonded fuel must be such that loss of sodium bond has a very low probability of occurrence during normal operation. There is no reason to believe that the clad and vent cannot be designed with such reliability that the probability of their simultaneous failure is so low that this can be realized. Even with unvented fuel pins, presently accumulated experience in SRE, Hallam, and EBR-II indicates that bond loss is a rare occurrence.

2.2.2. Bond Boiling

Boiling of the sodium bond may occur during certain reactor excursions providing all available protective action fails. Complete failure of the protective system removes the accident from the credible to the hypothetical class, i.e., gross fuel melting would be expected; however, it is of interest to know what happens between the time the bond boils until fuel failure occurs. The model used assumes that when the boiling point of the sodium bond is reached ($\sim 1700^\circ\text{F}$ at the operating pressure), the sodium vapor pressure forces

the bond sodium out of the fuel-clad gap and into the expansion reservoir above the fuel column. This instantaneously replaces the sodium bond with a sodium-vapor bond of very low conductance. Some verification of this model is provided by experimental evidence⁽⁸⁾ that indicates that the bond is simply forced out of the gap and into available reservoirs with relatively low pressures being generated. It was concluded from these tests that the sodium-bond pressures did not cause the failure; instead, the rupture was instigated by molten fuel which some time after the pressure pulse came in contact with the clad and melted through. This test was said to be especially representative of vented fuel and thus was very similar in response to what might be expected in our reference design.

In general, accidents involving reactivity insertion such as control-rod ejection result in gross fuel melting prior to coolant voiding. This is true for sodium-bonded rods as well as gas-bonded rods, either oxide or carbide. The loss of the sodium bond due to boiling only accentuates this process. On the other hand, loss-of-flow accidents in gas-bonded designs are generally characterized by first boiling and voiding the coolant, and is then followed by fuel melting due to the loss of heat sink. This type of accident has historically been used as the possible sequence of events leading to a situation in which gross voiding of the core results. Under these circumstances, a separate reactivity insertion is created due to sodium voiding, which occurs some time before gross meltdown effects are realized.

In the loss-of-flow accident for sodium-bonded fuel, however, it appears that a different sequence of events is involved: bond boiling will always occur before coolant boiling, which then tends to insulate the clad and coolant, and temporarily prevents coolant boiling. The combination of this time delay, a high gap resistance, and the fuel-rod power, even though it is considerably reduced ($\sim 1/2$ full power), causes the fuel temperature to increase beyond the melting point (see Fig. 2.2). This will cause fuel melting prior to coolant voiding, as in the reactivity accidents.

After fuel melting begins to occur, only speculation is possible as to the subsequent source of events. It could be expected, for instance, that some fuel of relatively high burnup would expand rapidly due to the volume change of central molten fuel and the action of retained fission gas in the outer nonmolten high-temperature fuel regions. This would close the gap remaining between fuel and clad, increase the heat-transfer rate between the fuel rod and its coolant, and eventually produce conventional coolant ejection in some subassemblies. In some other subassemblies having lower exposure, molten fuel could be expected to

FIG. 2.2. Hot-channel Outlet Temperatures (Includes Reactivity-feedback Effects and No Scram) before and after Loss of Sodium Bond in Loss-of-flow Accident.

penetrate the clad, producing sodium boiling and ejection of both fuel and coolant. Sequential failures of this sort spread over a time period of the order of 10 sec appears likely.

Since both fuel and coolant would tend to be ejected, the reactivity history during this period is impossible to predict without the aid of appropriate experiments. What is clear, however, is that fuel melting and fuel movement are intimately connected with coolant voiding in severe loss-of-flow transients as well as severe power transients. As was previously indicated, this is generally true for all fuel systems; however, since it is possible to postulate transients for gas-bonded cores in which voiding occurs prior to and separate from fuel melting, the void effect has a certain definable meaning for these cores; however, for sodium-bonded systems this analysis indicates that there may be no major transients in which sodium voiding can be identified as a distinct and separate event, and therefore the reactivity effect of a void would be only meaningful in connection with such things as gas bubbles.

2.3. Hypothetical Accidents

The effect of reactivity insertion during core meltdown was described earlier (see Sect. 2.1.2) when discussing the results of safety comparisons of the cylindrical *vs* modular cores. It has been postulated that core voiding preceding meltdown could provide another mechanism for the insertion of a reactivity ramp in cores with positive sodium-void coefficients. Although our studies of the effect of the sodium bond tend to reduce the significance of the sodium-void reactivity in our reference design, separate sodium voiding was investigated for the sake of completeness. Calculations were made to determine the theoretical maximum rates

of reactivity insertion due to sudden voiding of the cylindrical core.

The most straightforward sort of accident leading to gross coolant voiding of the core is a loss of pressure in the inlet plenum without any protective action. This can be caused either by pump failure, an inlet-line breakage, or a reactor-vessel rupture. The third type is treated here as the limiting worst case.

The following assumptions were used in calculating this limiting case for the reference core:

- 1) The inlet coolant plenum is instantaneously depressurized at full power due to a sudden, large rupture of the reactor vessel.
- 2) The protective systems fail.
- 3) The sodium does not re-enter any voided channels.
- 4) The sodium bond is retained.

Two voiding models were used. In the first case, it was assumed that the sodium superheat is constant throughout the reactor at 215°F. Voiding patterns for this case were constructed using the results of calculations of sodium expulsion made with the ETNA⁽⁹⁾ code (see Fig. 2.3). The reactivity effects resulting from these nonhomogeneous voiding patterns were calculated at several time steps during voiding, using a two-dimensional synthesis procedure. Results are shown in Fig. 2.4.

However, it is virtually impossible that the physically unstable phenomenon of liquid superheating should occur in exactly the same way throughout the core. Experiments have shown that when boiling is initiated in sodium during a transient, the degree of superheat varies from zero to several hundred degrees in a more or less random manner.⁽¹⁰⁾ This would lead to voiding of individual subassemblies in a random pattern throughout the core. This condition was treated in the second model by distributing the voids homogeneously throughout the core.

To calculate the reactivity increase for this model, a special Monte Carlo code was written, using the

FIG. 2.3. Void Pattern for Constant Superheat (215°F) at 840 ms after Initiating Accident.

FIG. 2.4. Reactivity Effects from Core Voiding.

language GPSS/360. Superheats with varied probability distributions were randomly assigned to the 219 coolant channels. Inputs to the model were the rate of temperature increase of the coolant, the velocity of coolant ejection as a function of the coolant superheat, and the radial distribution of coolant temperature in the core. Output consisted of several time histograms, indicating the volume percent of core voided as a function of time, which was translated into rate of reactivity insertion.

A comparison of the reactivity insertions from the two voiding models is shown in Fig. 2.4. As indicated, the maximum ramp rate assuming the worst condition, i.e., constant superheat and nonhomogeneous void distribution, is about 20 \$/sec, which is considerably smaller than the ramps of about 65 \$/sec predicted by conservative calculations for homogeneous core meltdown (see Sect. 2.1.2).

2.3.1. Bethe-Tait Analysis

In order to establish design criteria for the containment system and to determine Doppler requirements, the maximum explosive energy release during the maximum hypothetical accident was determined. As previously discussed, the largest reactivity ramp conceivable in the reference design results from a hypothetical accident involving core meltdown. Using this reactivity insertion and the appropriate vapor-pressure relationships, the total explosive energy released during the excursion was estimated using Bethe-Tait-type calculations.

The equation of state derived for PuC is based on

the LASL vapor-pressure data as reported by Miller.⁽¹¹⁾ If the Clausius-Clapeyron equation is used, these data give an estimated heat of vaporization of 70 kcal/mole at the melting point (1920°K). These data were used to estimate the critical temperature by assuming that the molar entropy of vaporization of PuC behaves like other materials in terms of reduced temperature.⁽¹²⁾ The critical volume proposed by Kubit⁽¹³⁾ was used, together with an assumed critical compressibility of 0.27, to obtain the critical pressure. Critical constants derived in this manner are as follows, where they are compared to those proposed by Kubit:

Critical Constants for PuC	This Analysis	Kubit
Temperature T_c (°K)	4470	3100
Pressure P_c (atm)	1490	1036
Volume V_c (cm ³ /mole)	67	67

Brout's method⁽¹⁴⁾ was used to calculate pressure and temperature as a function of total energy density and to obtain an estimate of the upper limit of the mechanical work energy available from expansion of the vaporized fuel to atmospheric pressure. A value of 0.87 was used for the initial value of reduced volume, which is conservative for the reference design.

Calculations with the WEAK EXPLOSION code⁽¹⁵⁾ were made using these pressure and temperature relationships. The excursions were initiated from normal full-power conditions using an equivalent spherical representation of the core. The reactivity ramp of \$65/sec obtained from homogeneous compaction of the core at one-g acceleration was used for calculations in which the Doppler constant was the parameter. Results of these calculations are shown in Fig. 2.5.

The important role of the Doppler effect in reducing the available work energy from such an excursion is shown by these calculations. An order-of-magnitude reduction is obtained with a Doppler constant greater than about -0.002. For the reference design, the upper limit of the explosive yield is about 300 MW-sec, which is well below the level for which containment can be designed. As would be expected from the similar assumptions and models used, these results are not unlike those obtained from studies of other reactor systems of similar size.⁽¹²⁾

It can be seen from Fig. 2.5 that the Doppler constant for the voided core need not be large to obtain a satisfactory reduction in explosive yield. It is only necessary to be safely to the right of the knee in the curve, which for this system occurs in the region near $Tdk/dT = -0.002$. In view of the uncertainties in making Bethe-Tait calculations, a safety factor of two

was applied to these results. A lower limit for the allowable voided Doppler constant was therefore established at a value of $Tdk/dT = -0.004$ in order to guarantee that the worst postulated hypothetical incidents could be safely contained.

2.4. Protective System

The majority of the features of the protective system are similar to those provided on currently operating or proposed fast reactors. Furthermore, many of these features are standard items in the protective systems of water reactors. Some improvements and additions have been made in our design, and these are the areas that will be discussed here. In order to determine the design requirements for the protective system, it was first necessary to analyze the plant during all anticipated modes of operation, normal and particularly abnormal conditions. The analysis involved a detailed investigation of many specific credible accidents in an effort to determine their effect on the reactor. These effects include such things as: the inherent reactivity feedback of the reactor, power perturbations, temperature changes in the fuel, clad and coolant, and any core damage, local or gross. The results of the credible-accident analysis and how they affect the protective system design are summarized in Table 2.2.

This analysis of the reference design core has produced some interesting design requirements for the protective system when considering the safety criteria discussed earlier. Areas which deserve special mention include such items as in-core instrumentation, local protection against fuel failure, control-rod reactivity worth, double protective system, multiprotective action trips, and gas detection. Each of these areas will be briefly discussed in terms of the design requirement it places on the system and the reasons behind it.

In-core Instrumentation

This instrumentation consists of the fuel-subassembly thermocouples. Two thermocouples are placed in the outlet of each subassembly. Signals from them will provide information for two purposes: (1) for continuous monitoring of the outlet temperature of each fuel subassembly, and (2) to provide early protective action trips for the protective system.

These thermocouples will be continuously monitored by a computer system that constantly checks for any large deviation in the outlet temperature of a subassembly. If such a deviation occurs, a comparative check of the two thermocouples or a temperature-lag check by a programmed power-oscillation test should determine if the channel is becoming plugged or the thermocouple is malfunctioning. Furthermore, to pro-

FIG. 2.5. Available Work Energy.

vide for early protective action from gross core changes, several properly functioning outlet thermocouples are always connected to the trip circuits of each protective system on a coincidence of 2 out of 4 basis.

Protection against Local Fuel Failure

Three lines of protection are provided for the detection of local fuel failure and the prevention of its propagation. These are:

- 1) in-core thermocouples;
- 2) reactivity meter;
- 3) detection of fission gas and delayed neutrons.

Protection from two types of local flow blockage, i.e., complete or partial, is provided in the reference design. The partial blockage situation is monitored by the computer-controlled in-core thermocouples for detecting a flow reduction early enough to prevent it from becoming a large plug, with subsequent damage to the fuel subassembly. The computer and its data analysis are not considered an integral part of the protective system, but are used only to obtain operating information and to increase the probability of detecting local plugging before it can cause damage. With proper core and subassembly design, the most realistic and probable type of plugging will be slow and will allow plenty of time for detection. The in-core thermocouple system is tailored for that type of blockage. It is recognized that a small percentage of these outlet thermocouples may be inoperable during a year's refueling cycle (they can be replaced during

TABLE 2.2 ANALYSIS OF PROTECTED ACCIDENTS

	Signals for Protective Action								Consequences of Failure of Protective Action	
	Primary		Secondary		Tertiary		Others		Consequences	Time after Start of Accident (sec)
	Type	Time (sec)	Type	Time (sec)	Type	Time (sec)	Type	Time (sec)		
I. Loss of Flow										
a. Loss of all primary pumps	Loss of pump power	0.2	Power/flow	2.0	Subassembly outlet temp	2.5	Mixed mean outlet temp Low flow Reactivity	6.5 5.0 6.5	Sodium boiling	10.0
b. Local subassembly plugging										
1. Instant blockage										
(a) ≤ 5 Subassemblies	Reactivity (fuel melting)	6.0	Coolant fission products	20.0	Cover-gas fission products	30.0	(a)	15.0	Coolant blockage and melting in adjacent subassemblies	10-20
(b) 6 Subassemblies	High power level	0.8	Reactivity (coolant void)	0.8	Subassembly outlet temp	1.2	Mixed mean outlet temp (b)	6.0 15.0		10-20
2. Slow blockage			No damage if detected by subassembly outlet thermocouple and oscillator tests. If not detected, conditions would approach those above for instantaneous blockage.							
c. Failure of primary containment										
1. Major rupture (i.e., double-ended pipe break; check valve fails)	Subassembly outlet temp	0.1	Sodium level	0.5	Reactivity	1.0	Leak detection	1.0	Sodium boiling	2.0
2. Minor rupture	Sodium level	(c)	Leak detection	(c)	If not corrected, this accident can result in the same effects produced by accidents Ia or IIc with similar protective action.				Sodium boiling	(c)
II. Reactivity Ramps										
a. Control-rod ejection (single rod)	High power level	0.2	Subassembly outlet temp	0.4	Mixed mean outlet temp	4.0	Reactivity Power/flow	0.2 0.2	Sodium boiling and fuel melting	15.0
b. Control-rod withdrawal (all rods)										
1. At full power	High power level	4.8	Subassembly outlet temp	6.0	Mixed mean outlet temp	9.0	Power/flow Reactivity	5.0 5.5	Fuel melting	20.0
2. Startup conditions	Variable high power level	73.5	Subassembly outlet temp	77.0	Mixed mean outlet temp	81.5	Period	73.2	Sodium boiling	84.0

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c. Gas bubbles	Entrained gas level	-2.0	High power level	0.1	Subassembly outlet temp	0.4	Reactivity	0.1	Sodium boiling in affected subassemblies (if ≥ 2.3 ft ³ bubble)	0.2
d. Subassembly drop in critical core during re-fueling	Provide ample shutdown margin		Criticality checks		Fabrication checks		Pre-refueling enrichment checks at reactor plant		Accident judged incredible	

^(a) Reactivity (coolant void) plus coolant fission products or cover-gas fission products in coincidence.

^(b) Fission products—coolant and cover gas.

^(c) Depends on the rupture.

refueling). Therefore, a few subassemblies may be unprotected by this system at any given time. There appears to be no economically feasible way of avoiding this problem. Therefore, the basic philosophy is to provide highly reliable thermocouples that will insure one good measurement in a very high percentage of the subassemblies. This system is designed primarily to increase operating efficiency and to protect the plant owner's investment by reducing the risk of extended outages. It is not intended as part of the safety system required for the protection of the public; however, by reducing the probability of local fuel failures, it increases the overall safety of the system.

A complete blockage or a severe partial blockage which immediately causes subassembly voiding cannot be detected early enough to be prevented by any detection system. In this case, the function of the detection system is to react early enough to limit the damage to the blocked subassembly(s) and prevent failure propagation.

From the results of our accident analysis, it is evident that the fewer the subassemblies simultaneously blocked, the more difficult it is to make prompt detection. Thus, the blockage of a single subassembly is more difficult to detect than the blockage of several. This occurs because the positive reactivity effect from sodium voiding of one subassembly is so small that it is undetectable until fuel meltdown takes place a few seconds later. The reactivity meter⁽¹⁶⁾ might separate the reactivity change from the normal background noise and expected normal changes; however, a scram based on this signal alone is not feasible. Therefore, the signal from the reactivity change must be used in coincidence with another signal, such as delayed-neutron activity in the primary sodium or fission product activity in the reactor vessel cover gas, which

would be detected several seconds after the first gross clad failure. A more definite reactivity signal will occur after the fuel melts and begins moving out of the seed region. This would produce a decrease in reactivity of up to $-0.26\% \Delta k/k$ for one subassembly if all the fuel were removed. A reactivity change of $\pm 0.1\% \Delta k/k$ is large enough to be reliably detected with the reactivity meter following any accident situation and thus can be used as a protective-action scram signal. Therefore, if approximately 40% of the fuel of one subassembly is removed from the seed region after melting, a protective-action trip will be initiated.

The flow blockage and subsequent voiding of several subassemblies cause a positive reactivity addition large enough to be confidently detected by the reactivity meter. Furthermore, it results in power changes large enough to cause high-power and outlet-temperature level trips. In such cases, the reactor will shut down before much fuel has melted in the blocked subassemblies, preventing any failure propagation.

Reactivity Worth of Control Rod

Control requirements for the reference design were formulated from the calculated operational reactivity changes in accordance with applicable provisions (Criteria 28-32) of the seventy USAEC General Design Criteria. The total control requirement for the safety-rod system was determined assuming the core clamp is not removed and the core is in its most reactive condition. The shim-regulating rods provide sufficient negative reactivity at any time during the life of the core to be capable of making and holding the core subcritical from any hot standby or hot operating condition. The safety rods provide enough negative reactivity at any time during the life of the core to be capable of making and holding the core subcritical under any conditions including that described above for the shim-regulating rods. Sufficient excess reactivity is available in the control rods to compensate for reactivity changes with fuel burnup and startup of the reactor from cold to full power. The scram release and insertion capabilities of the control subassemblies, both shim-regulating and safety, is sufficiently fast to prevent exceeding acceptable fuel-damage limits.

A summary of the control-rod worth requirements is given in Table 2.3.

Double Protective System

The reactor protective system utilizes the control rods and is subdivided into two independent shutdown systems. Each system is capable of automatically shutting down the reactor during any credible-accident condition either before any significant damage occurs for incidents involving the reactor as a whole or before

TABLE 2.3. REACTIVITY EFFECTS AND ASSOCIATED CONTROL REQUIREMENTS FOR REFERENCE DESIGN

	Reactivity Requirement, $\% \Delta k/k$	
	Shim-regulating System	Safety Rod System
Burnup—one cycle	0.863	0.863
Full to zero power		
Expansion effect	0.080	0.080
Fuel Doppler effect	0.275	0.275
Maneuvering and error allowance	0.500	0.500
Shutdown margin	1.000	1.000
Operating to cold at zero power		
Expansion effect	—	0.247
Fuel Doppler effect	—	0.274
Total control requirement	2.718	3.239

Any damage from a local incident can propagate. Specifically, for local incidents, such as rapid flow blockage of one or more subassemblies in which some damage might be expected, each scram system is designed to prevent propagation of failure to any of the adjacent nonaffected subassemblies. The primary shutdown system uses the fully withdrawn safety rods, whereas the backup system employs the shim-regulating rods. To provide further redundancy in the shutdown system or, in fact, a third scram system, the safety rods are subdivided into two separate systems from the detectors through the instrumentation channels and to the safety rods. Either safety-rod bank is capable of accomplishing the safety and damage-limiting functions indicated above.

Multiprotective Action Trips

Protective-action trips for a minimum of three different reactor parameters is provided for every credible accident. This will reduce the probability of protective-system failure to a very low level, thus essentially guaranteeing the prevention of damage to the plant during most accidents.

Gas Detection and Protection

Since gas in the primary system in relatively large, coherent volumes can cause damaging reactivity and heat-transfer effects, it is necessary to detect its presence and provide for its removal. The protective system provides entrained gas detectors on each of the six primary loops; these detectors will determine the amount of gas present at any time. This will allow an indication of any gas buildup and provide time for protective measures, i.e., either gas removal or plant shutdown. However, the primary protection from gas accumulation and entrainment is either to prevent it from entering the primary system, or if it does enter the sodium to provide for its separation and disentrainment in the design features of the primary system. Proper primary-system designs should prevent gas entrainment or accumulation at such locations as free pump surfaces, upper plenum of the reactor vessel, pipe bends, and minimize entrainment of fission gas. Separation and disentrainment of gas can be permanently designed into the system by such techniques as regions of low sodium velocity, centrifuge equipment, or baffling and flow directional changes which force the gas into sodium which passes through core regions of low importance, i.e., the radial blanket.

2.5. Summary of Safety Considerations

The results of the safety studies which were described in this section can be summarized in the following general conclusions.

Sodium-bonded Fuel

The effect of the sodium bond on safety considerations was examined for normal operation and for reactor transients. Because the bond boils before the coolant, the presence of the bond was found to alter the usual sequence of events for transients involving coolant boiling. Because sudden bond loss during normal operation can produce substantial fuel melting in the affected fuel rod, high reliability of the bond is required. This can be achieved by venting fission gases properly to keep the internal pressure always lower than the outside sodium pressure.

Protective System

A protective system which achieves high reliability through redundancy of shutdown systems and redundancy of detection signals has been evolved through study of a number of postulated accidents. Each accident can be terminated safely by any one of three independent shutdown systems, with shutdown action being initiated by any one of three signals of different kinds. The most useful such signals are flux level, coolant outlet temperature measured near the core, and indications of abnormal reactivity changes obtained from a reactivity meter. The combination of an inherently stable core and such a protective system makes the probability of substantial fuel melting or coolant boiling extremely low.

Hypothetical Accidents

Generalized core voiding and melting were studied in order to establish theoretical upper limits for rates of reactivity insertion. Based on idealized models and conservative assumptions, the limiting values due to meltdown and voiding were found to be 65 \$/sec and 20 \$/sec, respectively. Studies indicated that cores with sodium-bonded fuel are less likely than gas-bonded cores to experience voiding due to coolant boiling as an independently identifiable occurrence. Bethe-Tait analysis of the consequences of reactivity ramps of 65 \$/sec showed that the energy yield of such incidents can be reduced to satisfactory levels with a substantial margin of safety by the addition of a small amount of moderator to the core.

Reactor Configuration

A modular core configuration with a near-zero void effect was examined and found to have a substantial disadvantage in economic and breeding performance. Safety requirements for the modular arrangement were developed and compared to those for the moderately pancaked design, and they were found to be substantially the same and equally achievable. Con-

sequently, the short cylindrical core was chosen as the reference configuration.

3. Fuel Selection

3.1. Requirements of Fast Reactor Fuels

3.1.1. Material Properties

The heat rating and the breeding ratio which can be realized with a fast breeder design are influenced quite strongly by the choice of the fuel material. High thermal conductivity of the fuel, preferably in combination with high allowable temperatures, is essential to achieve high heat ratings. High fissile-atom and low light-atom densities are favorable for the neutron economy of the reactor and permit good breeding performance. In addition, of crucial importance for the economics of the fuel cycle is the irradiation stability of the fuel material, which is a most significant factor in determining the amount of exposure an actual fuel element can tolerate in the reactor.

3.1.2. Development Status

Besides these intrinsic properties of a fuel material, the status of its technological development with respect to processes for preparation of fuel material as well as fabrication and reprocessing procedures is significant because of influence on the cost of the fuel cycle. Finally, of utmost practical importance are, obviously, the extent and the results of irradiation tests with fuel-material specimens and prototype fuel elements under LMFBR conditions.

3.1.3. LMFBR Fuel Materials

Mixed dioxides, carbides, and metal alloys of uranium and plutonium are generally considered to be the choice of fuel materials for large LMFBR's. The relatively poor thermal conductivity of the (U, Pu) dioxide results in comparatively low heat ratings in spite of the high allowable fuel temperature, whereas the low heavy-atom density and high light-atom (O_2) density lead to a comparatively poor breeding performance. However, these intrinsic shortcomings are to some extent counterbalanced by the advanced development status of the oxide and the relatively large extent of irradiation testing. The (U, Pu) carbide has the potential of achieving higher heat ratings and better breeding because of its better thermal conductivity and density parameters. The carbide-fuel technology, however, and the irradiation testing are less advanced. The (U, Pu) metal alloys basically have good thermal conductivity and heavy-atom density, but also the comparatively lowest melting temperatures. Low-density fuel-rod designs which compromise

the favorable material properties will be required, on all probability, because of the higher irradiation-induced swelling rate of the metal alloys at exposures comparable to oxides and carbides.

3.2. Comparison of Fuel Materials

3.2.1. Scope of Comparison

In the present work, two fuel materials were selected for closer comparison: mixed (U, Pu) dioxide and mixed (U, Pu) carbide. The metal alloy was excluded because the irradiation-swelling problem associated with that fuel material is considered severe enough to require considerably more development work than either the mixed oxide or mixed carbide.

The comparison of the oxide and carbide was to show the differences in the performance of large LMFBR cores which are caused by the choice of the fuel and to substantiate the selection of the fuel for the C-E LMFBR. The primary criteria for comparison were fuel-cycle cost and doubling time. In the comparison, the mixed oxide was assumed to be incorporated in gas-bonded, stainless steel-clad fuel rods; the carbide fuel was studied in two design variations: gas-bonded and sodium-bonded, with stainless steel cladding in both cases. Thus, three different fuel systems were compared. The core comparison was performed using the same method (which is described in more detail in Sect. 4.0) for all three fuel systems.

A main objective of the comparison was to establish an optimum core configuration for each of the three fuel systems and to compare the performance of these optimized cores. For this purpose, fuel-cycle costs and doubling times were calculated for a variety of core configurations which were defined by a parametric variation of the two main independent variables, namely, seed height (2, 4, and 6 ft) and fuel-pellet diameter (0.2, 0.3, and 0.4 in.). As a result of this parameter study, an optimum core configuration was selected for each fuel system which, after modifications due to safety and control considerations, finally evolved into the reference core for that particular fuel system.

3.2.2. Ground Rules of Comparison

The fuel comparison was performed for large LMFBR cores capable of generating 1000 MW net electrical power. The mixed oxide as well as the mixed carbide (gas-bonded and sodium-bonded) were assumed to be able to achieve an average seed exposure of 100,000 MWd/MT. The fuel-rod design of the oxide and the gas-bonded carbide was consequently based on the "strong" cladding concept, employing a stain-

ss steel cladding with a ratio of thickness to mean diameter of 0.065. The smeared density of the oxide was taken as 87% of theoretical; the higher swelling rate of the carbide was accounted for by reducing the smeared density of the gas-bonded carbide to 80% of theoretical. The sodium-bonded carbide-fuel rods were designed with a thin stainless steel cladding of 11-mil wall thickness. At the end of the exposure, the expanded fuel is just in contact with the cladding without exerting any strain on it. The sodium gap was sized to accommodate a swelling rate of 1.8% $\Delta v/v$ per 10,000 MWd/MT of the hyperstoichiometric fuel with an initial fuel pellet density of 99% of theoretical. The design of the gas-bonded as well as the sodium-bonded fuel rods incorporated a vent device for venting fission gases to the coolant.

The thermal performance of the fuel rods was determined on the basis of not exceeding two temperature limitations: maximum allowable fuel and outside cladding temperature. In the case of the oxide, the maximum allowable fuel temperature was taken as the melting temperature, which was not to be exceeded at 10% overpower. For the carbide, the maximum allowable fuel temperature is usually considerably lower than the melting temperature and was chosen with respect to avoiding excessive fuel swelling. The temperature limit of the cladding was chosen in consideration of the corrosion resistance of the cladding material. In the following tabulation, the limiting temperatures along with other main thermal design values are listed:

	Oxide	Carbide	
		Gas-bonded	Sodium-bonded
Maximum allowable fuel temperature (°F)	4900	2500	
Maximum allowable cladding temperature (°F)	1200	1200	
Temperature rise through seed region (°F)	300	300	
Fuel-cladding heat-transfer coefficient (Btu/hr-ft ² -°F)	1500	1500	Sodium conductivity
Thermal conductivity of fuel (Btu/hr-ft-°F)	melting $\int_0 k dT$ = 86W/cm	8.5	10.5
Maximum sodium velocity (ft/sec)	30		30

The fuel-cycle costs were determined for equilibrium midcycle conditions. They are based on the following economic constants:

Price of plutonium (\$/kg)	10 ⁴
Annual interest rate	0.10
Plant factor	0.80
Overall plant efficiency	0.40
Average seed exposure (MWd/MT)	10 ⁵
Capacity of fabrication plant (MT/week)	1
Reprocessing plant charge (\$/day)	3×10^4
Shipping costs:	
seed and axial blanket (\$/kg seed metal)	20
radial blanket (\$/kg)	10

3.2.3. Results of Parameter Studies

The results of the parametric studies of cores fueled with oxide, gas-bonded carbide, and sodium-bonded carbide are shown in Figures 3.1, 3.2, and 3.3. Total fuel-cycle costs and compound-system doubling times are plotted in these diagrams as functions of the two independent parameters, seed height and fuel diameter. In addition, the total fuel-cycle costs are shown broken down into three components: inventory charges, charges for fuel operations (fabrication, shipping, reprocessing, and working capital), and plutonium credit.

Although the total fuel-cycle costs are the complex result of the various individual cost contributions with

FIG. 3.1. Comparison of Fuel-cycle Costs (Seed Height = 4 Ft).

FIG. 3.2. Comparison of Fuel-cycle Costs (Fuel Diameter = 0.3 In.).

FIG. 3.3. Comparison of Fuel Doubling Time.

often counteracting trends, the relative performance of the three fuel systems is determined primarily by the combined effect of inventory charges and plutonium credit. The inventory charges are largely an inverse function of the heat ratings which are lowest for the oxide, intermediate for the gas-bonded carbide, and highest for the sodium-bonded carbide. The plutonium credit, being essentially a measure of the breeding ratio, reflects the advantage of the better material densities of the carbides in general and of the thin stainless steel cladding of the sodium-bonded carbide in particular.

Fissile inventory and breeding performance are also the factors that determine the doubling time. Consequently, the smaller inventories and higher breeding ratios of the sodium-bonded carbide result in doubling times that are shorter than for the gas-bonded carbide or oxide.

The parameter study established a framework from which it was possible to select a seed height and a fuel diameter for each fuel system that would result in a core configuration with an optimum combination of fuel-cycle cost and doubling time:

	Oxide	Gas-bonded Carbide	Sodium-bonded Carbide
Seed height (ft)	2.75	2.5	2.0
Fuel diameter (in.)	0.25	0.3	0.35

Safety aspects were factored into the core selection by considering the sodium-void effect and the voided Doppler coefficient. Cores with a less positive void effect and a more negative voided Doppler coefficient were generally favored if the differences in economic performance were slight. In order approximately to equalize safety characteristics, the selected cores had nearly equal void effects (about $+1.5\% \Delta k/k$). Moderator, if required, was added later to the selected cores to achieve equal voided Doppler coefficients of -0.004 .

These optimum cores were also analyzed in more detail to establish the requirements for reactivity-control systems and eventually became the reference core concepts for the three fuel systems.

3.2.4. Comparison of Reference Core Concepts

In Table 3.1, fuel-cycle cost and doubling time of the three reference designs are listed together with other performance characteristics. The relative ranking of the fuel systems, in terms of fuel-cycle cost and doubling time, was maintained: the sodium-bonded carbide is most favorable, and is followed by the gas-bonded carbide and the oxide. The superior performance of the sodium-bonded carbide is primarily a con-

sequence of the high heat rating and better breeding ratio. It shows an advantage of about 0.35 mill/kWh in fuel-cycle cost and 5.5 yr in doubling time over the gas-bonded carbide and of about 0.52 mill/kWh and 8 yr over the oxide.

In view of the significance of the heat rating and the breeding ratio for the performance of the cores, an estimate was made of the improvement obtainable for a gas-bonded carbide and an oxide core if the linear heat-generation rate were increased and the cladding thickness decreased. From the cores which were analyzed during the parameter study, one core fueled with gas-bonded carbide and one with oxide were selected and compared with a core of same seed height and fuel diameter, but improved heat rating and thinner clad:

	Gas-bonded Carbide		Oxide	
	Standard	High-performance	Standard	High-performance
Seed height (ft)	4	4	2	2
Fuel diameter (in.)	0.3	0.3	0.25	0.25
Maximum linear heat rate (kW/ft)	21.9	27.3	15.7	19.5
Clad thickness (in.)	0.021	0.016	0.0175	0.013

The analysis of these standard and high-performance cores indicated that for an increase in heat rating of about 25% and a decrease of the cladding thickness by about 25%, the performance of a gas-bonded carbide core can be expected to be improved slightly, by better than 0.1 mill/kWh in fuel-cycle cost and a little over 2 yr in doubling time. The corresponding improvements of an oxide core are about 0.2 mill/kWh and about 3.5 yr.

3.2.5. Conclusion from Fuel Comparison

In the comparison which was described above, cores fueled with oxide, gas-bonded carbide, and sodium-bonded carbide were optimized with respect to fuel-cycle cost and doubling time, using a common set of ground rules and the same method of optimization. Thus, the comparison shows the influence of the three investigated fuel systems on the economic performance of a core. It substantiates the well-known qualitative ranking between mixed-oxide and mixed-carbide fuel by a quantitative determination of the difference in performance. Without doubt, it will be possible to improve the performance of each of the fuel systems to some degree by further "tuning" of promising core configurations. The present comparison, however, is indicative of the basic difference in performance potential, which is caused by the material characteristics

TABLE 3.1. COMPARISON OF REFERENCE CORES

	Oxide	Carbide	
		Gas-bonded	Sodium-bonded
Fuel diameter (in.)	0.25	0.30	0.35
Seed height (ft)	2.75	2.5	2.0
Seed diameter (ft)	11.526	11.470	8.87
Seed volume fractions			
Fuel	0.404	0.397	0.386
Na	0.358	0.363	0.434
Steel	0.235	0.192	0.131
BeO	—	0.045	0.046
Ta	0.003	0.003	0.003
Fissile specific power (MW/kg _{fiss})	0.714	0.713	1.104
Specific inventory ^(a) (kg _{fiss} /MWe)	4.96	5.02	3.46
Net electrical power, MW	1052	1079	1000
Sodium-void effect (% $\Delta k/k$)	+2.22	+1.80	+1.395
Voided Doppler coefficient	-0.004	-0.004	-0.0042
Breeding ratio	1.321	1.396	1.503
Compound doubling time ^(a) (yr)	14.96	12.36	6.99
Fuel-cycle cost, total (mill/kWh)	0.895	0.736	0.377
Inventory ^(a) (mill/kWh)	0.709	0.717	0.493
Fuel operations (mill/kWh)	0.514	0.421	0.369
Plutonium credit (mill/kWh)	0.328	0.402	0.485

^(a) Includes fissile material in blankets and out-of-pile.

and design requirements of the three fuel systems. The conclusion of the core comparison is that a sodium-bonded carbide fuel outperforms significantly the gas-bonded carbide and the oxide fuel because of the high thermal conductivity of the fuel, the low resistance to heat transport in the sodium-filled gap, the cladding which can be conveniently thin, and the high metal density of the fuel. The advantage in fuel-cycle cost is between 0.25 and 0.35 mill/kWh with respect to the gas-bonded carbide fuel system, and between 0.35 and 0.50 mill/kWh with respect to the mixed oxide. The doubling time of the sodium-bonded carbide core is between 3.5 and 5.5 yr shorter than for the gas-bonded carbide and between 4.5 and 8 yr shorter than for the oxide.

3.3. Selection of Reference Fuel for C-E LMFBR

Among the three fuel systems which were studied, the sodium-bonded mixed-carbide fuel has the best potential to fulfill the basic requirements for a "good" breeder reactor: lowest fuel-cycle cost and short doubling time. In addition, a fuel design with a sufficient gap between fuel and cladding for fuel expansion without mechanical fuel-clad interaction is considered a concept with the highest probability of achieving exposures in excess of 100,000 MWd/MT with a suffi-

cient degree of reliability. Therefore, the sodium-bonded mixed carbide was selected as fuel for the C-E LMFBFR. This choice was made in full realization that this fuel concept still requires a major effort in development of fuel technology and in irradiation testing. The economic advantages which derive from the superiority of the sodium-bonded carbide fuel over the gas-bonded carbide and oxide, however, are significant enough to justify its selection in spite of the less advanced state of fuel development.

3.4. Design Bases and Performance Limits of Sodium-bonded Carbide Reference Fuel

3.4.1. Fuel Swelling and Microstructure

The design of fuel rods which are to operate for long exposures in fast fluxes has to solve the combined problem of irradiation-induced fuel swelling and clad embrittlement. The embrittlement of the clad reduces drastically its capability to react to the pressure of the expanding fuel by yielding enough to maintain the mechanical integrity of the fuel rod. Our fuel-rod design is based on not subjecting the cladding to any strain from fuel expansion at the end-of-life. This is accomplished by providing a gap between fuel and clad which is large enough to accommodate the fuel swelling. In order to reduce the resistance to heat transport through the gap, it is filled with sodium.

The microstructure of the fuel material was selected as hyperstoichiometric because of its better irradiation stability as compared to a hypostoichiometric structure. The hypostoichiometric structure contains a free metal phase and, at temperatures above the melting point of the metal ($\sim 1830^\circ\text{F}$), it exhibits a higher swelling and gas release rate.^(17, 18) An evaluation of irradiation data of hyperstoichiometric carbide in thermal and fast fluxes suggests a swelling rate between 1.6 and 1.8 vol % per 10,000 MWd/MT^(19, 20) as well as a fission gas release rate of up to 50% beyond 40,000-MWd/MT exposure. The dimension of the fuel-cladding gap is based on a design value of 1.8% $\Delta v/v$ per 10,000 MWd/MT and is sized to accommodate (under the assumption of isotropic swelling) a fuel-diameter increase commensurate with a peak exposure of about 133,000 MWd/MT.

3.4.2. Fuel Temperature

The maximum allowable fuel temperature is not well established. Irradiation results indicate that the fuel temperature influences the fuel-swelling rate.^(18, 19) Thus, a balance has to be struck between desired burnup, acceptable fuel swelling, and allowable fuel temperature. The available evidence suggests that the allowable fuel temperature for hyperstoichiometric

mixed carbide should not exceed 2600°F if the fuel rod design is based on the swelling rate mentioned above.⁽²¹⁾

3.4.3. Thermal Conductivity

An evaluation of the thermal conductivity of mixed carbide indicates a value of 11.5 Btu/hr-ft- $^\circ\text{F}$ for 99% dense material containing 15 w/o Pu.⁽¹⁷⁾ Assuming a nonlinear reduction of thermal conductivity with decreasing fuel density, this value drops to about 9.5 Btu/hr-ft- $^\circ\text{F}$ at the end-of-life. This degradation of the conductivity, however, is accompanied by a shift of power from the seed region to the axial blanket, due to breeding and a burnup of plutonium in the outer zone of seed enrichment. An examination of the fuel temperatures at several points during the exposure of an assembly indicated that the use of a constant design value of 10.5 Btu/hr-ft- $^\circ\text{F}$ for the effective fuel conductivity allows a sufficiently accurate prediction of fuel temperatures.

3.4.4. Cladding Material

Austenitic stainless steels have the best current potential for use as cladding in LMFBFR systems. The commercial availability and the industrial manufacturing experience are far greater for stainless steels than for other candidate cladding materials (e.g., nickel, niobium, or vanadium alloys). There is also more pertinent information available for stainless steels with respect to sodium compatibility, irradiation performance, and fuel compatibility. In addition, the concentrated AEC development program in support of FTR will contribute considerably to the advancement of the technology of stainless steels for service in LMFBFRs.

Based on these considerations, stainless steel has been selected as cladding material. The choice between Type 316 and Type 304 has been made in favor of Type 316 in view of the availability of a greater amount of sodium compatibility data as compared to 304 stainless steel. It is recognized, however, that both materials will be likely to suffer similar losses of ductility at elevated temperatures and under fluences of the order of 10^{23} nvt.

3.4.5. Fuel-Cladding Compatibility

The selection of the hyperstoichiometric fuel microstructure for a sodium-bonded fuel-rod design with stainless steel cladding has been considered problematic because of carbon transport between fuel and stainless steel cladding. The carbon of the dicarbide phase of the hyperstoichiometric fuel has a tendency to be transferred by the sodium from the fuel to the cladding, causing its embrittlement and subsequent

failure. Recently reported out-of pile tests at up to 4000 hr and 1470°F, however, show that carbon transport from the fuel to the cladding does not occur if the dicarbide is eliminated as the second phase of hyperstoichiometric fuel and replaced by sesquicarbide.⁽²²⁾ The carbide fuel in the tests contained 10 vol % of $(U, Pu)_2C_3$ as second phase. It was prepared by carbothermic reduction of the oxide powder and contained 0.1 wt % of nickel as sintering aid. The results of these tests give a very positive indication that the carbon-transport problem can be solved by controlling the second phase of the hyperstoichiometric fuel.

3.4.6. Corrosion

The adoption of a fuel-rod design that greatly reduces fuel-clad mechanical interactions by providing a sodium gap and which minimizes internal pressure stresses by venting the fission gases permits the use of a thin cladding. The cladding thickness was set at 0.011 in. and reflects primarily considerations of practical materials handling during the fabrication processes and of maintaining structural integrity after exposure to corrosion in the reactor. In order to establish a corrosion allowance, a corrosion rate of 1.0 mil/yr was considered a reasonable upper limit. For a coolant velocity of 32 ft/sec, an oxygen concentration in the coolant of 10 ppm and limiting, under the worst condition, the cladding surface temperature to 1200°F, an average corrosion rate of about 0.3 mil/yr was calculated using a correlation developed by General Electric.⁽²¹⁾ Even if it is assumed that a maximum local corrosion rate could be higher than the average by a factor of 2.5 (to account for adverse local conditions in the reactor and uncertainties of predicting the corrosion rate) the calculated value is well below the design corrosion rate.

4. Optimization and Parametric Studies

4.1. Optimization Method

In the development of all core designs, a two-step procedure was used in which core configurations were first optimized with respect to their principal dimensions and composition without considering the effects of control and moderating materials. In the second step, modifications were made to accommodate appropriate amounts of control and moderating materials, specific blanket configurations were selected, and the effects of parameters such as coolant velocity, reloading interval, and plutonium price were evaluated. Except for the modular sodium-bonded carbide core which was not carried much beyond the first step, the procedure was essentially the same for each core with differences only in some details associated with the particular system. Parametric studies for the cylin-

dric sodium-bonded carbide design were the most extensive, and significant results of these are presented below together with illustrative examples of optimization calculations.

Although core optimization may be approached in several ways, all approaches follow the same basic procedure. Starting from the definition of a basic reactor type, several specific geometries are established, their performances are evaluated, and the arrangement with the best overall performance is selected.

If the effect of control and moderating materials is neglected, the core may be characterized for the purpose of physics calculations by the following parameters: seed height and diameter, fuel fraction, sodium fraction, and steel fraction. Specification of four of these five parameters is sufficient since the volume fractions must sum to 1.0. Arbitrary selection of values from this set of parameters presents some difficulties for optimization calculations because quantities such as fuel power rating, fuel diameter, and outlet temperature cannot be related explicitly to volume fractions and seed diameter. The approach selected for this study avoids this difficulty by adopting seed height, coolant velocity, fuel diameter, and seed temperature rise as the four basic free parameters.

Applying the usual methods of thermal, hydraulic, and structural analysis, and taking into consideration the engineering and performance limitations of the core materials, we can determine a unique set of seed parameters (diameter and volume fractions) for each set of the above four parameters. Additionally, the fuel power rating and the inlet and outlet temperatures can be determined, and much of the basic data for cost analysis can be generated. The process is illustrated schematically in Table 4.1.

One of the basic ideas behind this is the concept that the most interesting arrangements are those which are neither infeasible because they go beyond the engineering limitations of the system nor are arbitrarily under-rated with respect to those limits. More important, however, is the establishment of a consistent interrelationship among core and fuel dimensions and the material fractions. This interrelationship is illustrated in Fig. 4.1, which shows the results of thermal, hydraulic, and structural calculations for sodium-bonded carbide cores with hexagonal subassemblies having fuel and clad characteristics and limitations indicated in Sect. 3.0. The subassembly shroud thickness was calculated for each case to limit the peak shroud stress from internal coolant pressure to 16,000 psi.

4.2. Seed Height and Fuel Diameter

As indicated in Fig. 4.1, the strongest variations in material fractions are associated with variations in

TABLE 4.1. PROCEDURE FOR CORE OPTIMIZATION

FBR Type	Free Parameters	Material and Engineering Limitations	Reactor Characterization Analysis	Reactor Configuration	Reactor Performance Analysis	Reactor Performance
Fuel Clad	Seed height Fuel diameter	Fuel conductivity Density, max temperature, burnup	Thermal Hydraulic	Fuel power rating Seed diameter	Physics Economic	Fuel-cycle cost Doppler
Coolant	Coolant velocity	Clad conductivity, max temperature, corrosion	Mechanical	Volume fractions (fuel, steel, coolant) Inlet and outlet temperature	Thermodynamic	Void effect
Power	Core temperature rise	Fuel assembly stress, corrosion, vibration, pressure drop	Power distribution			Plutonium doubling time
Core arrangement	Blanket residence time and thickness	Hot-channel factors				Specific inventory

seed height and fuel (pellet) diameter. Consequently, the initial optimization calculations considered primarily the effect of these two variables. For these calculations, the peak coolant velocity was fixed at 30 ft/sec and the seed ΔT (excluding axial blankets) was fixed at 300°F.

Figure 4.2 shows fuel-cycle costs for the cylindrical sodium-bonded carbide core as a function of seed height and fuel diameter with fabrication costs as the

FIG. 4.1. Relationship between Free Parameters and Material Volume Fractions for Sodium-bonded Carbide Cores.

FIG. 4.2. Effect of Fabrication Cost on Fuel-cycle Cost (Survey Cores).

parameter. These results for the sodium-bonded carbide illustrate several important and basic trends which were found in the preliminary optimization studies.* These are:

1) There is a rather broad but definite minimum fuel-cycle costs as a function of fuel diameter between

* These trends are qualitatively similar for oxide and gas-bonded carbide as is illustrated in Figures 3.1 and 3.2.

TABLE 4.2. PARAMETRIC TRENDS FOR SODIUM-BONDED CARBIDE

	Fuel diameter = 0.3 in.			Seed height = 4.0 ft		
	Seed Height			Fuel Diameter		
	6 ft	4 ft	2 ft	0.2 in.	0.3 in.	0.4 in.
Breeding ratio	1.44	1.47	1.53	1.29	1.47	1.58
Specific inventory (kg fiss/MWe)	3.84	3.33	2.98	2.46	3.33	4.53
Seed + axial (blanket fabrication cost) (\$/kg seed metal) (Nominal)	129	153	236	234	153	119

diameters of 0.3 and 0.4 in. This optimum is somewhat larger than would be obtained if the interrelationship between fuel diameter and materials fractions were not considered.

2) Over a broad range, from about 2 to 5 ft, fuel-cycle costs are not strongly dependent on seed height.

3) The optimum fuel diameter is not significantly affected by variations in projected economic parameters (fabrication and reprocessing costs, interest rates, and plutonium prices). Although the fuel-cycle cost minimum as a function of seed height may be shifted considerably by variations in economic parameters, in the range indicated, the relatively weak dependence of costs on height remains.

The reasons for these trends are relatively straightforward. Table 4.2 below shows estimated fabrication costs and typical results of calculations of inventory and breeding ratio as a function of seed height at constant fuel diameter and fuel diameter at constant seed height.

When the seed height is reduced, there is an improvement in neutron economy because the reduction in sodium fraction is more than enough to compensate for the increase in steel fraction and the effect of the non-ideal core shape. For the same reason, neutron economy also improves when the fuel diameter is increased. Thus, the increase of inventory is almost linear with fuel diameter rather than increasing as the square of diameter. Although other factors are involved to a lesser extent, the main trends in fuel-cycle costs can be satisfactorily explained by the balancing effect of physics and fabrication costs when the fuel diameter is constant and the additional effect of heat transfer and power density when the fuel diameter is changed. This additional effect is the reason that a more pronounced minimum is found with respect to fuel diameter than with respect to seed height.

The costs of interest in this study are those asso-

ciated with a plant which will begin operation in the 1980's and whose life will extend beyond the year 2000. To calculate these costs, economic parameters must be projected over a long term, and these projections are unavoidably uncertain and variable. In order to find whether or not the optimum core configuration is a strong function of these parameters, they were varied over a wide range for the initial survey cores. The results of the most important of these calculations are shown in Figures 4.2, 4.3, and 4.4. The three economic parameters shown are those associated with the three factors listed in Table 4.2.

With respect to plutonium prices, one must consider the fact that this price will vary from time to time over the life of the plant. Viewed in this light, no seed height has a particular advantage, i.e., when plutonium prices are high, the short core has the advantage and the reverse is true when plutonium prices are low. In evaluating the effect of interest rates, it is not so important that the true optimum fuel diameter is different for different interest rates; rather, the significant result is that the optimum for 10% interest (0.35 in.) is within 0.01 mill/kWh in fuel-cycle cost of the true optimum for 5% and 15% interest. Thus, variations in interest rate will not significantly penalize a design with 0.35-in.-diameter fuel.

FIG. 4.3. Effect of Plutonium Price on Fuel-cycle Costs (Survey Cores).

FIG. 4.4. Effect of Interest Rate on Fuel-cycle Cost (Survey Cores).

FIG. 4.5. Effect of Seed Height on Void Effect and Doubling Time (Survey Cores).

The selection of the fuel diameter for the reference design (0.35 in.) was based on the results of the economic studies just described. However, as has been indicated, the economic studies did not lead to a positive conclusion as to the best seed height. Additional factors were considered in selecting the seed height. The variations of void effect (100% seed plus axial blanket) and doubling time with height are shown in Fig. 4.5. These clearly indicate that the shorter cores are to be preferred. Heights of 2.0 and 1.5 ft were examined in some detail, and the 2.0-ft seed height was selected because the void effect and doubling time advantages of the 1.5-ft seed height were outweighed by its other disadvantages. These include a significant disadvantage for fuel-cycle cost at discharge burnups less than 100,000 MWd/MT (see Fig. 4.6) and for higher than nominal fabrication costs (see Fig. 4.2). Additionally, the 1.5-ft. seed region is very tightly packed; the fuel-pin pitch to diameter ratio is less than 1.10. The practicality of such an arrangement is in doubt; because of this, a reduction of fuel diameter and/or sodium velocity could be required for a 1.5-ft.-high seed which would increase both doubling time and the fuel-cycle cost.

4.3. Blankets

As part of the preliminary studies, an investigation was made of the effect of residence time in the radial blanket on core performance. For the sodium-bonded carbide cores, a slight advantage in fuel-cycle cost was generally found for cores which retained radial blankets to relatively high discharge burnup ($\sim 50,000$ MWd/MT avg). This advantage was produced by a combination of factors including higher power produc-

FIG. 4.6. Effect of Average Discharge Burnup on Fuel-cycle Cost (Survey Cores).

on from blankets, lower blanket-fabrication costs, and lower core-region power peaking and enrichment. Economic factors which favor discharging the radial blanket at low burnup ($\sim 10,000$ MWd/MT avg) are primarily a lower blanket-inventory charge and a higher overall breeding ratio. Primarily because the radial blanket has a weaker influence on the core for the larger gas-bonded carbide and oxide cores, the economics balance favored a low-burnup radial blanket for these systems. Thus, average fissile plutonium content for a radial blanket was established at $\sim 5\%$ for the sodium-bonded survey calculations and at 2.0% for the gas-bonded calculations. After completion of these parametric studies, this question was reexamined for the reference design, and the residence time of the radial blanket was made approximately equal to the average seed-residence time. This did not change fuel-cycle costs significantly; however, overall breeding performance was improved.

Results of calculations to determine the best thickness of radial and axial blankets for the reference core are shown in Table 4.3. (initial survey calculations were made with an 18-in. axial blanket and a 3-row radial blanket.) On the basis of these calculations, an 18-in. axial blanket was chosen. Although the calculations indicated a slight advantage for the 2-row radial blanket, a single-row radial blanket with a single-row stainless reflector was chosen as the simpler arrangement.

4.4. Control Rods

As indicated in the preceding safety discussion, control requirements were derived from interpretation of the AEC General Design Criteria. This led to two separate systems: regulating rods with a required total worth of $2.72\% \Delta k/k$ and safety rods with a required total of $3.24\% \Delta k/k$. Studies of rod-ejection accidents indicated that the peak worth of single control assemblies should be limited to about $0.4\% \Delta k/k$. Control-rod studies were primarily limited to examination of the most straightforward and simplest scheme, i.e., poison assemblies which occupy a full core position. For reference, it was estimated that replacement of poison with movable fuel control could reduce the fuel-cycle costs by 0.5 mill/kWh and the doubling time by 1 yr.

Tantalum and natural and enriched boron carbide were investigated as candidate poison materials. In representative configurations, the reactivity worth of natural B_4C relative to tantalum was found to be 1.6 in the unmoderated reference core and 1.4 in the moderated reference core. Boron carbide was therefore selected for safety rods, and these were designed so that all have the maximum allowable worth ($0.4\% \Delta k/k$).

TABLE 4.3. CALCULATIONS^(a) FOR BLANKET THICKNESS

	Breeding Ratio	Fuel-cycle Cost (mill/KWH)
Axial blanket thickness		
12 in.	1.47	0.459
18 in.	1.55	0.414
24 in.	1.58	0.437
Radial blanket		
1 Row	1.56	0.406
2 Rows	1.59	0.374
3 Rows	1.61	0.380
1 Row + 1 row stainless reflector	1.58	0.376

^(a) Reference core configuration (2-ft-high seed 0.35 in.-dia fuel) without control rods or moderator.

Three natural B_4C assemblies are provided in the inner core zone and six enriched (40% boron-10) assemblies are placed in the outer zone for a total safety rod worth of $3.6\% \Delta k/k$. The rod pattern is shown in Fig. 4.7.

The regulating-rod pattern was selected such that one ring of rods would be stationed at the boundary between the two zones where the maximum in power occurs, and that a second set of seven rods would be placed in the inner zone. Physics studies showed a relative power shift from the outer to the inner zone over the cycle and a net increase over life of plutonium concentration in the inner zone fuel. The regulating-rod pattern allows some compensation for these effects by shifting regulation from the outer ring at the beginning of a cycle to the inner zone at the end of a cycle. Tantalum was selected for regulating rods because it has a longer useful life, it provided sufficient reactivity worth, it operates at lower temperature than B_4C for comparable rod sizes, and it does not produce gas from n, α reactions. The total worth of the 13 regulating-rod assemblies is $2.9\% \Delta k/k$.

4.5. Core Moderator

The voided Doppler "constant" for the reference core with 22 empty control positions was calculated to be $T dk/dT = -0.0021$ in the range from 300 to $1500^\circ K$ without moderator. These calculations also indicated that $T dk/dT$ decreases gradually as temperature increases. As described above, Bethe-Tait studies indicated a somewhat higher value would be prudent from the safety point of view. It was therefore decided to add moderator to the core to raise the voided Doppler constant to $T dk/dT = -0.004$.

The initial studies indicated that this could be accomplished by the addition of either 5 v/o BeO or 10 v/o graphite (both 70% dense). On this basis, BeO was selected as the moderator, and studies were made

LEGEND

- CONTROL - 13
- ⊗—SAFETY - 9
- ▨—INNER SEED - 111
- OUTER SEED - 108
- BLANKET - 60
- ▨—REFLECTOR - 66
- * EQUIVALENT DIA. 8.87'

FIG. 4.7. Arrangement of Control Assemblies.

which evaluated several alternative methods for moderator placement. The following three alternates received the most attention:

- a) Replace fuel rods with moderator rods.
- b) Replace hexagonal fuel subassembly with a round subassembly and place the moderator in the corners between subassemblies.
- c) Replace the 0.193-in.-thick solid hexagonal subassembly shroud with a honeycomb structure with the voids in the honeycomb filled with moderator. (The proposed structure is a conventional arrangement with honeycomb webbing sandwiched between and attached to two flat plates.)

The second two alternates were attractive because they tend to have the net effect of substituting moderator for steel rather than for fuel. Volume fractions for these alternates are shown in Table 4.4 together with relative fuel-cycle costs and doubling times. The fuel-cycle performance numbers indicated that the advan-

tages of the more complex alternates were substantial enough to justify serious consideration. For the reference design, the circular arrangement was selected because it is less complex than the honeycomb shroud arrangement.

4.6. Parametric Studies on Reference Reactor

The configuration of the reference core was initially established by the process just described. Subsequent parametric studies of this core were made to determine if refinement of the blankets, provisions for control rods, and addition of moderator affected the validity of initial choices, to investigate the effect of free parameters not studied in the initial survey, and to find the effect of variation of engineering limitations, such as allowable fuel and coolant temperatures and burnup. As was done in the initial survey, thermal, hydraulic, and structural calculations were made for each case to determine volume fractions. Because pressure stresses in the circular subassembly shroud were relatively low for all cases, a constant shroud thickness was maintained (0.050 in.). Thus, for these parametric studies, the steel fraction remained relatively constant, and since the moderator fraction was held constant, the main variations are in the sodium and fuel fractions.

4.7. Sodium Velocity and Seed ΔT

The principal free parameters not studied in the initial survey were seed ΔT and sodium velocity. For the survey, these were fixed at nominal values of 300°F (excluding axial blanket) and 30 ft/sec. So far as the physics and fuel-cycle characteristics of the core are

TABLE 4.4. COMPARISON OF ALTERNATIVE MODERATOR ARRANGEMENTS

	Hexagonal Subassembly Arrangement		Reference Circular Subassembly
	Solid Shroud with 19 Moderator Rods	Honeycomb Shroud with Moderator in Shroud	
Volume fractions			
Fuel	0.355	0.400	0.386
Sodium	0.407	0.407	0.432
Steel	0.189	0.138	0.137
BeO moderator (% density)	0.049 (77)	0.055 (60)	0.045 (77)
Voided Doppler (T dk/dT)	-0.004	-0.004	-0.004
Change ^(a) of fuel-cycle cost (mill/kWh)	+0.15	+0.04	+0.06
Change ^(a) of doubling time (years)	+1.4	+0.4	+0.6

^(a) Relative to unmoderated reference core.

Concerned, variation of each of these parameters has exactly the same effect: an increase in either reduces the sodium fraction. The effects on reactor performance of variations of either velocity or ΔT are shown

FIG. 4.8. Effect of Peak Design Velocity and Core ΔT on Characteristics of Reference Reactor.

in Fig. 4.8. Minimum costs for these parameters can be found only by consideration of capital costs in addition to fuel-cycle costs. Detailed studies of capital cost were outside the scope of this work; however, estimates based in part on published studies⁽²³⁾ indicated relatively flat minima with respect to ΔT between 300 and 400°F, and between 45 and 55 ft/sec with respect to velocity. The estimated difference between power costs at the design point and at the combined minimum point was 0.05 mill/kWh. However, neither ΔT nor velocity were increased, because in order to take advantage of the lower cost potential, it would be necessary to reduce the fuel-rod pitch to diameter ratio to a value less than 1.1.

4.8. Other Parameters

Table 4.5 summarizes the effects calculated for variations of fuel diameter, seed height, fuel temperature limit, outlet temperature, and average discharge burnup of the fuel. These results confirm the trends found in the initial survey calculations with respect to fuel diameter and seed height, and show that moderator and control additions as well as radial blanket changes did not produce performance changes that would indicate any changes in the basic geometry of the core.

Variation of the limiting fuel temperature in the range from 2000 to 3000°F with corresponding changes of peak heat rating from 27.4 to 55.2 kW/ft has a surprisingly small effect on overall reactor performance. There is practically no economic incentive to increase the peak heat rating above the design value of 43.5 kW/ft, and only a relatively modest penalty is asso-

TABLE 4.5. VARIATION OF REFERENCE CORE PARAMETERS

Parameter	Sodium Fraction (%)	Avg Specific Power of Seed (kWt/kg metal)	Breeding Ratio	Void Effect (% $\Delta k/k$)	Compound Doubling Time (yr)	Fuel-cycle Cost (mill/kWh)
Fuel diameter, in.						
0.35 (ref)	43.2	118	1.45	+1.36	7.79	0.424
0.25	50.0	221	1.24	+0.52	11.7	0.641
0.45	37.5	73	1.54	+1.48	8.75	0.486
Seed height, ft						
1.5 (Ref = 2.0)	40.3	120	1.51	+0.88	7.11	0.437
Fuel temp limit, °F						
2000	37.8	76	1.52	+1.46	8.84	0.519
3000	46.1	149	1.41	+1.19	7.71	0.412
(Ref = 2575)						
Outlet temperature, °F						
818 (Ref = 1095)	44.7	138	1.42	+1.25	7.59	0.407
Avg discharge burnup, MWd/MT						
5,000	43.2	118	1.57	—	10.2	1.64
10,000	43.2	118	1.52	—	8.00	0.782
200,000	43.2	118	1.31	—	10.5	0.353
(Ref = 100,000)

FIG. 4.9. Relationship between Specific Power and Characteristics of Reference Reactor.

ciated with substantial decreases in heat rating. This is an indication of a general characteristic of this reactor, namely, that its performance is, over a broad range, insensitive to the specific power rating of the fuel.

Specific power depends upon fuel diameter, and the product of fuel conductivity and the sodium-to-fuel temperature difference. Six cases of different specific power were examined in these parametric studies: the reference case, two fuel-diameter cases at constant fuel and coolant temperatures, two fuel-temperature cases at constant coolant temperature, and one case in which the average coolant temperature was lowered by 270°F. Examination of these six cases from the viewpoint of fuel power density allows some generalization about the characteristics of this system. Performance parameters are plotted in Fig. 4.9 in terms of seed specific power. From this plot, it is apparent that the economic and breeding performance of the design is insensitive to specific power over a rather wide range between about 60 and 180 kWt/kg total metal. The reason for this is fairly straightforward. Higher specific powers yield lower inventories, but also lower the breeding gain because more sodium is required to remove the heat. These opposite acting effects maintain

a fairly good balance in the ranges indicated. Eventually, of course, either very high or very low specific power significantly penalizes the performance of the system.

Calculations were also made to determine the economic effects of changes in average fuel-discharge burnup, and the results of these are shown in Table 4.5. The incentive to go beyond an average of 100,000 MWd/MT is not significant. Even though the reference reactor configuration is not optimum for 50,000 MWd/MT (see Fig. 4.6), it is still competitive with oxide FBR's operating at burnups of 100,000 MWd/MT.

4.9. The Reference Reactor

The essential features and characteristics of the reference reactor have been developed and explained in the preceding sections. A further description of the reactor and some of its details are presented here together with performance numbers to complete the picture. For further amplification, a relatively complete tabulation of design and performance data is given in the appendix.

The reactor core is composed of 219 seed subassemblies, 60 blanket subassemblies, 22 control subassemblies, and 66 reflector subassemblies forming a relatively flat circular cylinder, approximately 10 ft in diameter and 5 ft in height, as shown in Fig. 4.10. The seed region is 2 ft high and has a mean diameter of about 9 ft. It is surrounded by radial and axial blankets. The fuel subassemblies of the seed region contain 163 fuel rods that are arranged on a triangular lattice within a 6.5-in.-diameter circular stainless steel shroud, as shown in Fig. 4.11. The outside diameter of the fuel clad is 0.4 in. Spiral spacer wires are used as spacer elements between the fuel rods. The fuel rods contain both the seed fuel material and the axial blanket fuel material in the same stainless steel fuel tube. In order to accommodate the irradiation-induced swelling of the mixed carbide fuel without straining the cladding, a sodium-filled gap is provided between the fuel and the cladding. The fuel rods are vented to the coolant by means of a diving bell-type fission gas vent. The BeO moderator is contained in compartments which are attached to the outside of the fuel-assembly shroud wall. The fuel assemblies are held down in the double core-support plate by hydraulic holddown forces. The subassemblies of the radial blanket consist of 91 depleted uranium carbide fuel rods of 0.6-in. clad OD within a hexagonal stainless steel shroud, as shown in Fig. 4.12. The fuel rods are nonvented and collect released fission gas in a gas plenum at the top end of each fuel rod.

The control-rod system consists of two types of as-

FIG. 4.10. Core Arrangement.

semblies: 13 regulating assemblies and 9 safety assemblies. The regulating assemblies are composed of 10 tantalum-bearing rods of 1.25-in. OD within a circular shroud, as shown in Fig. 4.13. The safety assemblies are shown in Fig. 4.14 and consist of 18 boron carbide-bearing rods of 1.210-in. OD inside of a circular shroud.

In Fig. 4.15 the arrangement of the core in the reactor vessel is shown. The fuel and reflector assemblies that form the reactor core are supported by the upper and lower core-support plates. Bowing of the assemblies in the radial direction is controlled by the radial core retainer. The stainless steel reactor vessel itself is suspended from the top end flange on 24 tie rods allowing unrestrained expansion. The thermal shields reduce the fast-neutron and gamma flux at the vessel wall to acceptable levels. Six 30-in. sodium inlet and six 36-in. sodium outlet nozzles are provided together with two sets of emergency cooling nozzles. All nozzles penetrate the vessel wall at elevations higher than the core to preclude loss of coolant from the core in the case of a primary-system leak. The control assemblies are actuated by control-rod drives that are mounted on top of the closure head. The flow direction of the incoming coolant is down in the annular space between

vessel wall and core barrel, upward through the core, and out through the exit nozzles.

The components of the refueling equipment are shown in Fig. 4.16. The refueling procedure incorporates the "wet" transfer of the fuel subassemblies from the core to the decay storage. It is based on an under-the-plug refueling concept employing a plug-exchange scheme. After the central portion (control-drive plug; see Fig. 4.15) of the reactor vessel closure is exchanged with the handling-machine plug and its integral handling machine No. 1, the fuel assemblies are transferred under sodium into the sodium-filled transfer pot. This pot is picked up by the transfer machine and transferred to the decay-storage tank, where the subassemblies are removed from the pot and arranged in their storage positions by handling machine No. 2. The transfer machine is used, again, to transport the cooled-down subassemblies from the decay-storage tank to the cask cart. The reversal of this procedure is used for moving the fresh fuel subassemblies into the core.

5. Conclusion

The studies described in this paper examined some of the fundamental design questions for fast reactors: safety, fuel system, and core configuration. A rela-

FIG. 4.11. Seed Region of Fuel Subassembly.

FIG. 4.13. Shim-regulating Control Subassembly.

FIG. 4.14. Safety Control Subassembly.

FIG. 4.15. Arrangement in Reactor Vessel.

FIG. 4.16. Refueling Equipment.

tively long-range viewpoint was adopted; however, the design was based on technology which is in hand or can confidently be expected to be developed within the next few years. The principal conclusions were:

1) The principal requirements for LMFBR safety are a stable core with high-reliability protective and cooling systems. A sufficiently negative Doppler effect has to be achieved to provide time for protective action and to limit the energy yield from hypothetical accidents to containable levels. A moderately positive sodium-void effect does not significantly compromise safety.

2) The preferred fuel system for LMFBR's is hyperstoichiometric sodium-bonded, mixed plutonium-uranium carbide. Although the present state of development of this fuel is less advanced than gas-bonded oxide or carbide, there is sufficient information available today to assure that it will be developed sufficiently to compete favorably in the 1980's.

3) The preferred core configuration for large (1000 MWe) LMFBR's is a moderately pancaked cylinder of height-to-diameter ratio of about 1 to 4. This configuration is near optimum from an economic and breeding point of view, and also has important safety advantages over other arrangements.

4) The characteristics and performance that can be expected in the 1980's for a reactor based on the above considerations can be summarized as follows:

Fuel-cycle cost	0.38 mill/kWh
Compound doubling time	7.0 yr
Specific inventory	3.5 kg fissile/MWe
Void effect	+1.4% $\Delta k/k$
Doppler effect	
With sodium	-0.006 $T' dk/dT$
Voided	-0.004 $T' dk/dT$

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APPENDIX

DESIGN AND PERFORMANCE DATA OF REFERENCE REACTOR

Description	Unit	Quantity or Material	Remarks
Overall Reactor Performance			
Total thermal output	MWt	2465	
Net electrical output	MWe	1000	
Net efficiency	%	40.6	
Steam pressure	psi	2400	
Steam temperatures	°F	900/900	
Geometric and Mechanical Data			
Reactor Size			
Seed height	ft	2.00	
Seed diameter	ft	8.87	Equivalent
Axial blanket length	ft	1.50	On each end
Axial blanket diameter	ft	8.87	Equivalent
Radial blanket height	ft	3.00	
Radial blanket outside diameter	ft	9.88	Equivalent
Radial reflector outside diameter	ft	10.89	Equivalent
Reactor Composition			
Seed and Axial Blanket Region			
Fuel	vol %	38.63	
Sodium	vol %	39.98	
Stainless steel	vol %	13.10	
Moderator	vol %	4.61	BeO—77% T.D.
Shim poison positions	vol %	1.55	Occupied by sodium unless tantalum rods are inserted to maintain criticality (0.3% Ta at midcycle).
Safety Poison Positions	vol %	2.13	During operation, all positions are occupied by sodium.
Radial Blanket Region			
Fuel	vol %	52.66	
Sodium	vol %	32.33	
Stainless steel	vol %	15.01	
Fuel Assemblies			
Seed Region			
Composition		PuC-UC	
Average enrichment, equilibrium			Pu-239 + Pu-241
Zone I	wt %	10.13	
Zone II	wt %	12.05	
Number of subassemblies		219	
Number of rods per subassembly		163	
Shroud thickness	in.	0.050	
Fuel-rod pitch	in.	0.451	
Clad material		Type 316 SS	
Clad OD	in.	0.400	
Clad thickness	in.	0.011	
Sodium-bond thickness	in.	0.014	At beginning of life
Pellet diameter	in.	0.350	
Pellet density	gm/cm ³	13.46	99% T.D.
Axial Blanket			
Composition		Depleted UC	
Average fissile plutonium concentration	wt %	1.65	
Other data		Same as seed	

DESIGN AND PERFORMANCE DATA OF REFERENCE REACTOR—Continued

Description	Unit	Quantity or Material	Remarks
Fuel Assemblies Cont.			
Radial Blanket			
Composition		Depleted UC	
Average fissile plutonium concentration	wt %	2.0	
Number of subassemblies		60	
Number of rods per subassembly		91	
Hexagonal can thickness	in.	0.011	
Fuel-rod pitch	in.	0.652	
Clad material		Type 316 SS	
Clad OD	in.	0.600	
Clad thickness	in.	0.020	At beginning of life
Pellet diameter	in.	0.520	
Pellet density	gm/cm ³	13.46	99% T.D.
Physics Data			
Breeding ratio		1.503	
Compound doubling time	yr	6.99	Includes Pu in blankets and out-of-pile
Internal conversion ratio		0.979	
Reactivity change for 1 cycle	% $\Delta k/k$	0.863	
Seed Peaking Factors			
Radial (peak/avg)		1.196	
Axial (peak/avg)		1.191	
New assembly		1.05	
Isothermal Doppler Coefficient			
With sodium	$T dk/dT$	-0.0064	Not including effect of axial blanket
Sodium removed	$T dk/dT$	-0.0042	
Sodium-void coefficient	% $\Delta k/k$	+1.395	100% seed and axial blanket
Control Assemblies			
Assembly Worth			
All assemblies	% $\Delta k/k$	6.50	
Maximum—Any one assembly	% $\Delta k/k$	0.40	
Shim-regulating Assemblies			
Number of assemblies		13	
Material		Tantalum	
Poison-rod OD	in.	1.25	
Total worth, fully inserted	% $\Delta k/k$	2.90	
Safety Assemblies			
Number of assemblies		9	
Material		B ₄ C	70% T.D. Outer 6 assemblies 40% enriched in ¹⁰ B
Poison-rod OD	in.	1.21	
Total worth, fully inserted	% $\Delta k/k$	3.60	
Thermal Power Data			
Seed power, midcycle	MWt	2128	
Axial blanket power, midcycle	MWt	286	
Radial blanket power, midcycle	MWt	51	
Fuel Power Density ^(a)			
Seed			
Average	kW/kg	121.5	
Peak	kW/kg	170.5	
Linear Power ^(a)			
Seed			
Average	kW/ft	29.5	
Peak	kW/ft	43.5	

DESIGN AND PERFORMANCE DATA OF REFERENCE REACTOR—Continued

Description	Unit	Quantity or Material	Remarks
Temperature Data			
Seed Fuel Temperatures ^(a)			
Hottest element peak	°F	2310	
Statistical uncertainty of peak fuel temperature	°F	265	
Seed Clad Temperatures			
Hottest rod peak outside temperature ^(a)	°F	1169	
Statistical uncertainty of outside temperature	°F	44	
Sodium Temperatures			
Reactor mixed mean inlet temp	°F	797	
Reactor mixed mean outlet temp	°F	1095	
Hydraulic Data			
Total reactor flow	lb/hr	9.40×10^7	
	gpm	2.33×10^5 at 1100°F	
Peak velocity in highest powered seed subassembly	ft/sec	32.7	
Pressure drop, total	psi	75.0	
Fuel Cycle			
Average discharge burnup of seed region	MWd/Mt	100,000	
Specific inventory (total system) ^(b)	kg fissile per MWe	3.46	
Plant factor		0.80	
Fuel-cycle out-of-pile time for decay, reprocessing, and refabrication	days	410	
Plutonium price	\$/gm	10.0	
Interest rate	%	10.0	
Shipping cost			
Seed + axial blanket	\$/kg	20.0	kg of seed metal
Radial blanket	\$/kg	10.0	kg contained
Reprocessing-plant daily charge	\$/day	30,000	
Fuel-fabrication cost			
Seed subassemblies	\$/kg	195.0	kg of seed metal
Radial blanket subassemblies	\$/kg	60.0	kg contained
Fuel-cycle cost			
Fabrication	mill/kWh	0.193	
Inventory ^(b)	mill/kWh	0.493	
Shipping	mill/kWh	0.048	
Reprocessing	mill/kWh	0.099	
Interest on fabrication	mill/kWh	0.029	
Plutonium credit	mill/kWh	0.485	
Total fuel-cycle cost	mill/kWh	0.377	

^(a) Full power at beginning of cycle without operating allowances and statistical uncertainties; peak values include all predictable peaking factors.

^(b) Includes plutonium in blankets and out-of-pile.

Discussion

Mr. Smidt (Karlsruhe): In your sodium-bonded carbide fuel, part of the fuel is at rather low temperatures, so that probably the swelling rate should be different from that experienced with gas-bonded fuel. How do you take that into account?

Mr. Menzel: We did not take it into account, primarily because we do not have sufficient data to know exactly what the difference in swelling rate is between sodium-bonded and gas-bonded carbide. The temperature dependence of the fuel-swelling rate is not clear.

Mr. Smidt: Yes, but you did perform an economic comparison.

Mr. Menzel: In an effort to establish the relative difference between a gas-bonded and a sodium-bonded carbide fuel element, we made the assumption that the swelling rate for both cases would be equal. The estimate of fuel-cycle cost is based on this assumption and is as good as this assumption.

Mr. Siegel (AI): As regards compatibility of the carbide and the stainless steel, you place great emphasis on the presence of the sesquicarbide phase. Can you

count on that phase remaining there as fission products develop during burnup? Also, is it costly to produce the fuel with the second-phase sesquicarbide?

Mr. Menzel: Let me answer the second question first. From work that has been done at United Nuclear, we get the impression that it is not significantly more expensive to make the hyperstoichiometric microstructure. I understand that the United Nuclear carbide fuel material is fabricated by carbothermic induction, and something like 0.1 wt % nickel is used as a sintering aid. As of now, we do not have experiments to show how stable the sesquicarbide will be.

Mr. Gast (Karlsruhe): My question is related to the sodium bond in the fuel. In the first part of the paper that was concerned with safety, it was quoted as a potential advantage of the sodium bond that in a loss-of-flow accident, the sudden vaporization of the sodium bond would effectively reduce the heat transfer to the cladding and to the coolant, so that no gross sodium boiling would occur. Now I wonder where the sodium vapor that would be generated in the gap would go. Would it tend to expand the cladding and thereby replace the rest of the sodium in the channels, which might lead to a rather large reactivity insertion, or would it tend to replace fuel in the axial direction?

Mr. Gilbertson: First of all, this was a highly hypothetical calculation. We based the model on a judgment of one experiment which does exist on how the sodium bond acts during the transient. This was done by AI in a TREAT transient which indicated that the bond did indeed boil and the sodium was forced out. The heat-transfer coefficient did change in that experiment. I don't know whether it is known that the fuel was displaced axially during this test or not. The experiment did go on to complete failure, because the fuel became molten and penetrated the clad; there was quite a violent reaction in the coolant. Thus, the model was based simply on assuming that the bond, when it came out, went up into the available reservoir space. There is room for the sodium in our design because we have to provide space in the fuel design for the end-of-life condition when the swelling has expanded the fuel out to the clad. The bond sodium will presumably go up into this space.

Mr. Kirchenmayer (AEG-Germany): In your study you considered the fuel in the form of pellets. There are a number of fuel manufacturers who believe that carbide that is made by vibratory compaction is a more favorable and more economical way of making fuel elements. How far would your conclusion be changed by this choice, by using vibratory-compacted fuel elements?

Mr. Menzel: If I understand you correctly, vibratory-compacted fuel essentially would not be much

different from our gas-bonded design because one of the assumptions in the gas-bonded carbide design was that although we have a pellet density of something like 85% theoretical initially, the carbide fuel swells very quickly out towards the cladding and then we have a smear density of 80%. I would suspect that vibratory-compacted fuel would basically have to have the same smear density.

Mr. Zebroski (GE): In your optimization on specific power, did you take into account a reduction in refueling interval as the specific power went up, or did you allow the fuel lifetime to rise as the specific power went up?

Mr. Noyes: We did account for the change in the core lifetime with respect to the out-of-pile inventory. We did not change the load factor; in other words, all of the calculations represent numbers for an 80% load factor. The high-specific-power fuel was provided with a penalty in the respect that the out-of-pile inventory was kept at a value determined by a constant time. We assumed that about 400 days were required out-of-pile, so that for those cores with higher specific inventory, the total inventory was penalized by the relative times involved in in-pile and out-of-pile.

Mr. Campise (ANL): I have two questions on safety. The first is in reference to boiling the sodium bond. Apparently you assume that the pin remains perfectly vertical, so that there is no contact with the clad. Now if there were contact with the clad, it is conceivable that the fuel temperature would not rise as fast, and that fuel melting and perhaps sodium boiling would occur. Have you considered this point of the fuel touching the clad even though you boil the bond off or lose part of the bond? The second question is in reference to your containment for handling the Bethe-Tait accident. Have you also considered molten carbide-sodium interactions?

Mr. Gilbertson: To answer the first question, this can happen. Of course, the pellets will be leaning up against the clad. We assumed that pellets leaning on the clad on one side will make the gap larger on the other. Our model simply assumes that the sodium bond is removed all around. We are only saying that this might produce melting of the fuel prior to core voiding. It looks as if it probably will. We have not gone into it in all of the detail that you are suggesting. In reply to your second question, the study didn't go that far.

Mr. Cybulskis (BMI): I believe you assumed the same swelling rate for the gas-bonded and sodium-bonded carbides. Were the smear densities in the two cases the same?

Mr. Menzel: No, the smear density in the gas-bonded carbide was 80% theoretical; for our sodium-bonded

reference design the smear density would work out to be about 85% theoretical.

Mr. Melese (GGA): It was mentioned that roughly doubling the linear heat rating from 27 to 55 kW/ft didn't make much difference in the overall performance; if I understand correctly, this probably involves the doubling time and fuel-cycling costs. I would be interested to know what you kept constant in this comparison—the maximum clad surface temperature or the maximum fuel temperature, and what other core parameters varied. I agree that if you design the reactor properly, the maximum kilowatts per foot is not too important, but it depends on what you keep constant.

Mr. Noyes: In this parametric variation, the coolant velocity, ΔT across the core, core height, and fuel-pin diameter were held constant, and the linear heat rating was varied. Resulting changes occur in the volume fractions of the coolant and the fuel. By the thermal-hydraulic processing that we go through, if we increase the linear heat rating, then (since we're holding ΔT and velocity constant) we have to provide more sodium to remove that heat. So an increase in linear heat rating increases the sodium fraction.

Of course, an increase in power rating does give you a benefit, provided you can also increase the coolant velocity in order that the sodium fraction does not increase. I think perhaps a proper conclusion from the study is that if the performance of the fuel is to be increased by increasing the heat rating as well as upgrading other parameters, you must go to higher velocity and higher ΔT across the core if you want to take advantage of high power ratings. If you can't do that, then there's much less benefit to be derived from going to high power ratings.

Mr. Hannerz (ASEA): I believe this economic optimization was made using a plutonium value of \$10 a gram as is usually the case. However, if you can produce the results that you predicted, I think the plutonium value might actually be twice as high. I would like to hear if you have any comment on how this would influence your choice of core parameters.

Mr. Noyes: Our conclusion is that, in general, the performance is relatively insensitive to plutonium price and the optimum point is not changed too much. With respect to fuel-pin diameter, again the picture is fairly clear. Variation in plutonium price does shift the true optimum, but you don't pay much penalty for staying at the design point of 0.35 in. in diameter. So far as the height of the core is concerned, the true optimum again is shifted quite a lot depending upon what the price of the plutonium is, but if you don't know in advance what the price of the plutonium is or if you expect the price of plutonium over the 30-yr life of the

plant to vary (perhaps from a high value or a low value), then you really can't find any advantage for either a short core or a tall core. In other words, when plutonium prices are low, the tall core seems to have some advantages. On the other hand, when plutonium prices are high, the short core has an advantage. Again we conclude that the price of plutonium doesn't really influence the basic configuration of the core. It might be nice, for instance, to choose that point up to about 2-1/2 ft, which is completely insensitive to the price of plutonium.

Mr. Wolfe (GE): I'd like to raise a burning issue. I think that the comments that came before Gast's and Campise's were indicative of a feeling which I share. With the high void coefficient you have in your reactor you really aren't going to be able to completely wipe out accidents. As an example, your meltdown could be the trigger which would get the power up high enough, quickly enough, to void rapidly the whole core. I think it is indicative of your study and the other ones that were presented at the conference that all of the studies casually (it's certainly not in detail) took care of these hypothetical accidents by putting in something for Bethe-Tait in the form of a black box of some sort. I would say that designing for one of these hypothetical accidents is not going to be handled that easily, that there are other factors besides the shockwave that enter in. One of them is the debris; another is the fuel-sodium interaction which is going to produce pressures, much higher if taken pessimistically, than those that were calculated this morning by B&W. To finish, I think that the situation is not the same as Mr. Harde mentioned yesterday with regard to his solution of the core-debris problem. The situation in the water reactors is that they are designed so that they don't get the core debris. They don't first accept it, and then say that they can't get away with it; they go to a lot of trouble to avoid it. The basic question, the burning issue, is whether the fast reactors can afford to take a casual approach by saying we will put it in at this stage and then find out really what it is going to involve; or whether, in fact, we will have to do what the water-reactor designers do, namely, somehow design so that it doesn't happen.

Mr. Noyes: I will make one comment on that. I think I generally agree with you that we perhaps ought to talk less about these things. I think there is one difference between the water reactor and the fast breeder, and that is that there is the possibility of the Bethe-Tait explosion in between the time that the core melts down and the time that it melts through the bottom of the vessel. In other words, there is an extra phenomenon which is different from a water reactor. Even though I think in both cases we agree that for the wa-

ter reactor or for the fast reactor, once you have a molten fuel, if you don't have the Bethe-Tait (which is certainly the likely situation), the core probably will melt through or is equally uncontainable. This must be prevented. I think that the No. 1 criterion is the real one, namely, to prevent those sorts of things. On the other hand, you do have the fact that there is that extra phenomenon. If there is some provision

made for it, perhaps it is only a psychological equalization, it does seem to equalize the water reactor and the fast breeder.

Mr. Wolfe: I don't know that there is that big a difference, because in both cases the real thing you have to do is to prevent clad melting. If you can't prevent clad melting, in both cases there is a lot of energy available from molten fuel hitting a liquid.

Transient Response of Heat Transfer and Thermal Stresses in Thick Walls of Sodium Components

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ABSTRACT

Thermal transients in a solid wall caused by changes of sodium temperature are measured with a thick tube as a test section. Responses of heat transfer and of thermal stresses are calculated from the thermal data through analyzing heat conduction in the test section.

Temperature changes of sodium flow exert serious influence upon heat flux on the surface and upon thermal stresses in the test section, but exert little influence upon the heat-transfer coefficient of the surface. This coefficient responds almost instantaneously to a new thermal condition and is affected by the thermal conductivity of the test section. The largest thermal stress appears at the inner surface of the test section, together with the largest temperature difference between the inner and outer surfaces.

1. Introduction

Thermal shock becomes a subject of discussion in the design of sodium components. This is attributable to the properties of sodium flow, that is, its high thermal conductivity yields high heat transfer, thus transferring temperature variations which are brought by reactor scrams and other operations immediately to solid walls contacted with sodium.

For solving such a problem, an orthodox method is first to obtain the thermal response of sodium flow such as variations of temperature and flow under a given condition, then to analyze transient conduction of heat and thermal stresses in the solid structure, as well as transient heat transfer to sodium, and to check the thermal stresses with strength of the material. Endurance tests of structural material fabricated definitely in certain concrete forms have been performed under repeated thermal cycles with sodium as a heating or cooling medium,⁽¹⁻³⁾ whereas experimental studies of heat transfer and of thermal stresses in thermal transient are few, compared with analyses of these heat transfer and thermal stresses,⁽⁴⁻⁸⁾ the reason probably being the difficulties in measurements. Actually, it is very difficult to measure transient thermal stress at higher temperatures, because strain gauges are not useful and not stable over wide temperature ranges.⁽⁹⁾

A method of obtaining heat transfer in a transient from the temperature variation on a test section has been devised, and one of its applications has been reported.⁽¹⁰⁾ This method is also available to obtain a better understanding of thermal shock of sodium components. As a present project, the following experiment has been performed. Since a heat exchanger is recognized to be a component subject to a severe thermal shock, test sections are installed at the primary inlet of a heat exchanger and at its secondary outlet. The temperature of sodium is suddenly changed at the inlet, and the responses of heat transfer and of thermal stresses are obtained by the test sections.

The purpose of this paper is to present an experimental technique with which the transient responses of heat transfer and of thermal stresses are followed, to verify variations of heat flux, heat-transfer coefficient, and thermal stresses during the transient stage, and to estimate the peak value of thermal stress under given conditions.

2. Experimental Facility

2.1. Thermal Shock Test Loop

A thermal shock test loop comprises two loops: a sodium-circulating loop, and a NaK-circulating loop, combined by a double-pipe heat exchanger. Figure 1 is a schematic diagram of the entire facility. The structural features of the thermal shock test loop are shown in the upper part of Fig. 1.

The NaK loop consists of an electromagnetic pump EMP(3) and an electromagnetic flowmeter EMF(3), heat reservoir tanks HR(1) and HR(2), the double-pipe heat exchanger, test sections TS(1) and TS(2), cold trap CT(3), expansion tanks EXT(1) and EXT(2), valves and three-way cocks, and connecting pipes indicated by thin lines.

The capacity of HR(1) and HR(2) is 277 liters each, containing a total of 430 liters of NaK. The heat capacity of NaK contained in the tanks lowers the thermal shock of the electromagnetic pump, valves, and piping. NaK in the tanks is heated by two 10-kW immersion heaters inserted from both side walls of the tanks or cooled by fans installed under the tanks. The tanks are

FIG. 1. Schematic Diagram of Experimental Facility.

stripped of their thermal insulation when cooled by the fans. The expansion tanks EXT(1) and EXT(2), each with a 33-liter capacity, are provided with level indicators of the plug-in, simple contacting type. The cold trap CT(3) is a horizontal, air-cooled type, including a regenerative heat exchanger. The capacity of the cold trap is 35 liters. The electromagnetic pump is an AC Faraday type and the electromagnetic flowmeter utilizes a permanent magnet. The tanks are secured at their legs, and heat expansion of the piping is absorbed by bellows between the tanks and the heat exchanger, and by looping the piping.

The sodium loop directly related to the thermal shock test loop consists of an electromagnetic flowmeter EMF(4), an electromagnetic pump EMP(4), the double-pipe heat exchanger, and test sections TS(3,4,5).

2.2 Sodium-purification System

The sodium-purification system, composed of a cold-trap loop and a plugging-indicator loop, are represented in the lower part of Fig. 1. Piping carrying sodium is shown by thick lines. Including the sodium loop of the thermal shock test loop, whole loops are connected to the central expansion tank EXT, so that each flow in the loops can be controlled independently.⁽¹¹⁾

Sodium circulates in the cold-trap loop, overflowing from the expansion tank down into a storage tank ST(1), pumped up by an electromagnetic pump EMP(1), flowing through an electromagnetic flowmeter

EMF(1) and the cold trap CT(1), up into the expansion tank. The storage tank ST(1), of 2.6-m³ volume, is provided with a 100-kW immersion-type heater. Temperature of sodium in the tank is controlled by the power input to the heater. The cold trap CT(1) is also horizontal and is an air-cooled type which includes a regenerative heat exchanger. Capacity of the cold trap is 150 liters. Another cold trap CT(2) of identical type and volume as CT(1) is provided for storing impurities transferred from CT(1). When CT(1) is choked with impurities, CT(1) is connected with CT(2) to form a closed circulating loop; these impurities in CT(1) are dissolved into sodium by heating up CT(1), and they are precipitated again in CT(2) by cooling down CT(2).

Sodium circulates in the plugging-indicator loop, flowing out from the expansion tank or the outlet of the cold trap CT(1), pressurized by an electromagnetic pump EMP(2), flowing through an electromagnetic flowmeter EMF(2) and a plugging indicator PI, and returning to the expansion tank. The plugging indicator is cooled by NaK and operated pneumatically; the impurities plug in its narrow tubes. The electromagnetic pumps and flowmeters of the sodium loops are the same as those of the NaK loop.

2.3. Test Section

The test section is constructed of a thick tube, of 53-mm OD and 20-mm ID, effectively 100 mm long. Construction of the test section is shown in Fig. 2.

Thermocouples are set along the centerline of the

test section, and are also attached on the inner and outer surfaces of the test section, 13 mm from its mouth. The thermocouples inserted through the wall to the interior are sealed by brazing. The hot junction on the outer surface is fastened in a small hole, and that on the inner surface is secured in a shallow groove, with at least half the surface of the junction exposed to sodium flow. These thermocouples are chromel-alumel, 0.2 mm in diameter, and sheathed in Type 304 stainless steel tubes, 1.0 mm in diameter.

Copper, brass (70 % copper, 30 % zinc), and Type 304 stainless steel were chosen for the material of the test section to examine their response to thermal conditions. Their thermal conductivities are evaluated as 320, 85, and 15 kcal/m-hr-°C, respectively.

The copper and brass test sections are coated with nickel to prevent corrosion. These test sections are insulated on their exteriors with glass wool. Test sections TS(1,2) are connected to both primary nozzles (tube side) of the heat exchanger, and test sections TS(3,4,5) are connected to the secondary outlet nozzle (annular side) of the heat exchanger with flanges, sealed with a tongue-and-groove joint fitted with copper gaskets to permit exchanging. The response of sharp temperature changes at the NaK-side inlet is obtained from TS(1), and the gradual response of those at the sodium side outlet from TS(3,4,5). The linked test sections TS(3,4,5) are provided for checking the entrance effect.

The double-pipe heat exchanger is a U type, constructed by wrapping a spacer wire on the inner pipe, 18 mm in OD and 1 mm thick, and spacing it in the outer pipe, 30 mm in OD and 2 mm thick. The effective length of the heat exchanger is 5800 mm.

In order to measure bulk temperature of the fluid by effective mixing, mixing chambers are butt-jointed to the inlets and outlets of both primary and secondary passes (as shown in Fig. 2).

The thermocouples for measuring fluid temperature are also sheathed in Type 304 stainless steel in tubes 1.6 mm in diameter and inserted through thin pipes, sealed with Teflon bushes.

3. Experimental Procedure

In normal operation, NaK circulates through a bypass pipe in the direction shown by the arrows in Fig. 1, without passing through the heat reservoir tank HR(1). There is no heat sink in this circulation; thus, NaK circulates isothermally. When the thermal-shock test is performed, the path of flow is changed by turning the three-way cock (1). NaK stored and cooled beforehand in HR(1) starts flowing into the inner pipe of the double-pipe heat exchanger. A sudden temperature drop occurs at the inlet on the NaK side of the heat exchanger, and

FIG. 2. Construction of Test Section.

a gradual temperature response appears at its outlet on the sodium side.

Argon gas space is provided in HR(2), but not in HR(1); otherwise argon gas space in HR(1) would damp the pushing pressure from the pump and disturb the flow of NaK when the path of flow is changed. Temperature variations of the test sections TS(1) and TS(3,4,5) are recorded by multipen recorders.

A thermal transient settles gradually into a stationary state; this is shown by the convergence of all recording curves. Thereafter, the path of flow is returned again by the three-way cock (1); then temperature rises are recorded the same as mentioned before. In this case, the amount of temperature change is slightly smaller than for the above cooling case, due to thermal mixing in the NaK loop.

Although NaK is used as the colder fluid in order to obtain a large temperature difference from the hotter fluid (sodium), the temperature difference obtained at the outlet of the sodium side cannot exceed 120°C due to the allowable limitation of temperature of carbon-Teflon packings used at the three-way cocks. The experiments are conducted by keeping the sodium tem-

perature around 240°C. Packings must be renewed several times during the experimentation. Pressure loss through the bypass pipe is smaller than that through the heat reservoir tank, giving NaK-flow decreases of 3–5% when flow path changes, but not exerting any influence on the thermal transient at the test sections.

As the flow direction of NaK can be easily reversed by an exchange of source-current connections supplied to the electromagnetic pump EMP(3), data in the case of parallel flow through the heat exchanger are also obtained by the test section TS(2).

4. Analysis of Heat Transfer and Thermal Stress

4.1. Heat Conduction in Hollow Cylinder⁽¹²⁾

The equation describing heat conduction in a hollow cylinder of which initial temperature is expressed in terms of the radius, and of which surface temperatures vary with time, is

$$\frac{1}{r} \frac{\partial}{\partial r} \left(r \frac{\partial T}{\partial r} \right) - \frac{1}{\kappa} \frac{\partial T}{\partial t} = 0, \quad (1)$$

with the initial and boundary conditions

$$T = f(r) \quad \text{at} \quad t = 0; \quad (2)$$

$$T = \phi(t) \quad \text{at} \quad r = a; \quad (3)$$

$$T = \psi(t) \quad \text{at} \quad r = b. \quad (4)$$

It is assumed that some of the properties of a solid, namely, thermal conductivity, density, and heat capacity, are constant. Transient temperatures in the test sections are obtained from the solution of Eq. (1) by substituting the measured temperatures at the surfaces into Eq. (3) and (4). However, it is not practical to determine the form of the functions $\phi(t)$ and $\psi(t)$ from the measured temperature variations. Therefore, the solution should be calculated on the assumption that temperature varies linearly during a short time interval $\Delta t(m)$, namely, by letting

$$\phi(t) = f_a t + g_a \quad (5)$$

$$\text{and} \quad \psi(t) = f_b t + g_b, \quad (6)$$

where

$$f_a = \frac{T_a(m+1) - T_a(m)}{t(m+1) - t(m)}; \quad g_a = T_a(m);$$

$$f_b = \frac{T_b(m+1) - T_b(m)}{t(m+1) - t(m)}; \quad g_b = T_b(m),$$

and m is a integer which signifies the order of the time interval; the subscripts a and b indicate the inner and outer radius, respectively. The solution is expressed as

$$\begin{aligned} T = & \frac{\pi^2}{2} \sum_{n=1}^{\infty} \frac{\alpha_n^2 J_0^2(a\alpha_n)}{J_0^2(a\alpha_n) - J_0^2(b\alpha_n)} U_0(r\alpha_n) \\ & \cdot \exp(-\kappa \alpha_n^2 t) \int_a^b r f(r) U_0(r\alpha_n) dr \\ & + \frac{\ln b - \ln r}{\ln b - \ln a} (f_a t + g_a) \\ & + \pi \sum_{n=1}^{\infty} \frac{J_0(a\alpha_n) U_0(r\alpha_n)}{J_0^2(a\alpha_n) - J_0^2(b\alpha_n)} J_0(b\alpha_n) \\ & \cdot \left[\frac{f_a}{\kappa \alpha_n^2} + \left(g_a \frac{f_a}{\kappa \alpha_n^2} \right) \exp(-\kappa \alpha_n^2 t) \right] \\ & + \frac{\ln r - \ln a}{\ln b - \ln a} (f_b t + g_b) \\ & - \pi \sum_{n=1}^{\infty} \frac{J_0(a\alpha_n) U_0(r\alpha_n)}{J_0^2(a\alpha_n) - J_0^2(b\alpha_n)} J_0(a\alpha_n) \\ & \cdot \left[\frac{f_b}{\kappa \alpha_n^2} + \left(g_b \frac{f_b}{\kappa \alpha_n^2} \right) \exp(-\kappa \alpha_n^2 t) \right], \end{aligned} \quad (7)$$

where

$$U_0(r\alpha_n) = J_0(r\alpha_n) Y_0(b\alpha_n) - J_0(b\alpha_n) Y_0(r\alpha_n), \quad (8)$$

α_n is the n -th positive root of

$$J_0(a\alpha) Y_0(b\alpha) - J_0(b\alpha) Y_0(a\alpha) = 0, \quad (9)$$

and $J_0(\kappa)$ and $Y_0(\kappa)$ is the first and second kind of Bessel function, respectively.

Heat flux at the inner surface and at $t = \Delta t(m)$ is obtained from

$$q_a = -k \left[\frac{\partial T}{\partial r} \right]_{r=a}. \quad (10)$$

By the use of this heat flux, the "transient" heat-transfer coefficient is defined tentatively as

$$h = \frac{q_a}{T_s - T_a}, \quad (11)$$

where the fluid temperature T_s at the center of the test section is introduced in place of the bulk fluid temperature. Consequently, the physical meaning and value of the heat-transfer coefficient are not the same as those usually employed.

Now one can obtain the transient response of heat transfer, as well as the transient responses of thermal stresses, by repeating the substitution in turn. The calculated temperature at $t = \Delta t(m)$ is substituted for the initial one at the next time interval $\Delta t(m+1)$.

4.2. Thermal Stresses in Hollow Cylinder⁽¹³⁾

The transient thermal stress at an instant under a temperature distribution varying with time is equal to the stationary thermal stress with the same temperature distribution as long as the displacement inertia

can be neglected. The equation of displacement in the hollow cylinder, which is taken to be long compared with the outer radius, reduces to

$$\frac{d}{dr} \left[\frac{1}{r} \frac{d}{dr} (ru) \right] = \left(\frac{1+\nu}{1-\nu} \right) \beta T \frac{dT}{dr}, \quad (12)$$

It is assumed that the temperature distribution is axial symmetric, and the axial and circumferential displacements can be neglected. By using the strain-displacement relations and Hooke's Law, rewriting the general solution of Eq. (12) in the stress expressions, and determining the constants of the general solution from the boundary conditions

$$\sigma_{rr} = 0 \quad \text{at} \quad r = a \quad \text{and} \quad r = b, \quad (13)$$

one can derive thermal stresses which are expressed by

$$\sigma_{rr} = \frac{\beta E}{r^2(1-\nu)} \left[\frac{r^2 - a^2}{b^2 - a^2} \int_a^b T r \, dr - \int_0^r T r \, dr \right], \quad (14)$$

$$\sigma_{\theta\theta} = \frac{\beta E}{r^2(1-\nu)} \left[\frac{r^2 + a^2}{b^2 - a^2} \int_a^b T r \, dr + \int_0^r T r \, dr - T r^2 \right], \quad (15)$$

and

$$\sigma_{zz} = \frac{2\beta E\nu}{(1-\nu)(b^2 - a^2)} \int_a^b T r \, dr - \frac{\beta E}{1-\nu} T. \quad (16)$$

When the hollow cylinder is heated or cooled from the inside, the maximum stress rises at the inner surface in the circumferential direction and is written as

$$[\sigma_{\theta\theta}]_a = \frac{\beta E}{1-\nu} \left[\frac{2}{b^2 - a^2} \int_a^b T r \, dr - T(a) \right]. \quad (17)$$

This maximum thermal stress is normalized and expressed as

$$\sigma_* = \frac{1}{\Delta T} \left[\frac{2}{b^2 - a^2} \int_a^b T r \, dr - T(a) \right] \quad (18)$$

in terms of the temperature distributions and the radii, where ΔT is the maximum temperature difference between the fluid and the hollow cylinder.

5. Examples of Temperature Variations

Figure 3 represents the temperature variations in the test section TS(2) when the temperature of NaK is changed most rapidly, almost stepwise. The vertical lines at the left indicate the pen positions at the incipience of change, which implies no time delay between the temperatures. The temperature T_{a2} follows T_{p2} closely, and T_{b2} is far behind, because more time is needed for heat transfer through the thick wall of the test section than from NaK to the inner surface. The reason why the temperature curve for NaK begins to descend 25 sec after the incipience is that the change of temperature has passed one turn through the NaK loop. The temperature T_{p0} is not significant, but $T_{s,i}$ shows that there is little temperature change concerning sodium.

The variations of temperature represented in Figs. 4, 5, and 6 permit comparison of the thermal conductivity of the test section TS(3) when made of copper, brass, and Type 304 stainless steel, respectively. The thermal and flow conditions are similar in each case. For the copper and brass test sections, heating curves are shown as well as cooling ones, since the durations of thermal transients are short. The thermal transient in the case of copper is shortest, that of Type 304 stainless steel longest.

The time delay between the start lines of T_{a0} and $T_{p,i}$, and those of T_{b3} , T_{a3} , and T_{s3} correspond to the time elapsed from the mixing chamber to the test section TS(3).

FIG. 3. Temperature Variations at Test Section TS(2).

FIG. 4. Temperature Variations at Copper Test Section TS(3).

FIG. 5. Temperature Variations at Brass Test Section TS(3).

FIG. 6. Temperature Variations at Test Section TS(3), Made of Type 304 Stainless Steel.

6. Transient Heat Transfer

6.1. Heat Transfer in Entrance Region during Cooling and Heating

The heat flux q_a and heat-transfer coefficient h at the inner surface of the circular tube, obtained from the brass test sections TS(3) and TS(5), are represented in

Fig. 7. The results from TS(3) are indicated by the small circles connected by solid lines, and the results from TS(5) are shown by the triangles connected by dotted lines. These marks indicate the results at every interval which has proved to be small enough in previous work.⁽¹⁰⁾

The indicial response of counterflow heat exchangers

is similar to a unit lag,^(14,15) so that q_a increases gradually to a peak in 8 sec as shown in Fig. 7, then decreases as the temperature difference between sodium and the inner surface diminishes. In contrast with the variation of q_a , the value of h increases only slightly between 1 and 10 sec. In the initial time range smaller than 0.5 sec, useful data have not been obtained because the thermocouples are not capable of following such a fast temperature change. On the other hand, in the time range larger than 10 sec, although it appears that h increases, this is also insignificant because of an error originating in the poor accuracies of measurement of $(T_s - T_a)$, occurring in the denominator of the expression for evaluation of h .

It is observed that q_a and h obtained from TS(3), located nearest to the outlet mixing chamber of the heat exchanger, are slightly larger than those from TS(5), linked behind TS(3). Time taken as the abscissa is measured from the start lines of the change as shown in Figs. 3 to 6, so that a time delay does not exist between TS(3) and TS(5).

It may be concluded that the difference in the results for TS(3) and TS(5) is attributable to the entrance effect, i.e., the thermal boundary layer is thinner and its thermal resistance is smaller in TS(3).

The circular and triangular marks also distinguish between cooling and heating, as indicated respectively by the hollow and the solid marks. No remarkable difference is observed between the hollow marks and solid ones, a reasonable result, since thermal properties do not change much during the experiments.

6.2. Influence of Flow Velocity

Figures 8 and 9 represent the results obtained from copper and Type 304 stainless steel test sections TS(3), respectively, when the flow velocity of sodium is varied.

FIG. 7. Heat Transfer in Entrance Region during Cooling and Heating.

FIG. 8. Influence of Flow Velocity in Copper Test Section TS(3).

FIG. 9. Influence of Flow Velocity in Type 304 Stainless Steel Test Section TS(3).

The influence of the flow velocity is obvious, i.e., h increases with flow velocity in both figures. The heat-transfer coefficient h obtained from the copper test section is larger than that from the brass one, and that from the brass one is larger than that from the stainless steel.

It is observed that q_a obtained from the copper test section shifts to the left and the peak point of q_a rises with increasing flow velocity. These tendencies are but slight in Fig. 9 in the results of the 304 stainless steel test section. Inasmuch as these figures are expressed on a log-log scale, it can be observed that the influence of flow velocity is rather large.

6.3. Thermal Conductivity of Solid Walls

Figures 8 and 9 also represent the influence of thermal conductivity of the test section. Figure 8 indicates that not only is q_a larger, but also that it finishes the entire

FIG. 10. Influence of Thermal Conditions at Secondary Outlet.

course of change in a shorter time compared with that in Fig. 9. These differences may be ascribed to the relation of demand (heat transfer to sodium) and supply (heat conduction in the test section);^(16,17) in the case of Type 304 stainless steel, the supply cannot respond to the demand. This is also confirmed with the results obtained from TS(4) and TS(5). The results obtained from the brass test section, as shown in Fig. 7, lie between the results from the copper and those of the Type 304. The reason why h in Fig. 8 is larger than in Fig. 9 has been left unexplained and is the subject for a future study.

6.4. Influence of Thermal Conditions

The variations of q_a and h are represented with the maximum temperature difference ΔT , defined in Sect. 4.2, as the parameter in Figs. 10 and 11. The results shown in Fig. 10 are those obtained from TS(3), which is made of Type 304 stainless steel, when ΔT is between 110 and 85°C. The results from TS(2) are plotted in Fig. 11 for ΔT equal to 205, 96, and 62°C. The curves in Fig. 11 are entirely different in their contours from those in Figs. 7 to 10, indicating large initial values that descend gradually. Regardless of these differences, it is found that the variation and the value of h are considerably similar to each other. As a result, q_a becomes nearly proportional to ΔT , independent of the varying forms of q_a . In other words, it may be said that q_a depends sensitively upon the temperature variation of sodium and NaK and that, on the contrary, h is insensitive to temperature variations.

6.5. Comparison with Stationary Heat Transfer

In Fig. 12 the transient heat transfer is compared with that measured by a usual stationary method, by normalizing Peclet numbers vs Nusselt numbers.

Nusselt numbers are obtained from TS(5) where the thermal boundary layer has been well developed. Their dispersions in the middle time intervals are shown by the vertical line segments connecting open marks, and the error arising in the initial and terminal stages in the transient are eliminated. Fluid properties are evaluated by the mathematical mean temperature between the initial temperature and the minimum (or maximum) one. The equation of Lubarsky and Kaufmann,⁽¹⁸⁾

$$Nu = 0.625 Pe^{0.4}, \quad (19)$$

is indicated in Fig. 12 by a straight line. This equation represents the heat transfer to turbulent and well-developed molten metal flow in a circular tube with uniform heat flux at the wall. Since the heat-transfer coefficient in our experiments are defined by the use of

FIG. 11. Influence of Thermal Conditions at Primary Inlet.

FIG. 12. Comparison with Stationary Heat Transfer.

the fluid temperature at the center of flow instead of the bulk fluid temperature, and since the heat flux at the tube wall has not been confirmed as uniform, the comparisons in Fig. 12 are not necessarily proper in a strict sense. However, at least it can be pointed out that Nusselt numbers obtained from the copper and brass test sections agree well with the straight line predicted by Eq. (19). On the other hand, the Nusselt numbers from the test section of Type 304 stainless steel are about half the value predicted by the straight line.

It should be noted that the use of the tentative heat-transfer coefficient determined by Eq. (11) depends upon the thermal conductivity of the solid wall exposed in the thermal shock of sodium.

7. Transient Thermal Stresses

7.1. Distribution of Temperature and Thermal Stresses

The transient distribution of thermal stresses in the test section at a certain time are calculated from the temperature distribution corresponding to that time by the use of Eqs. 14 to 15. Figure 13 represents the distributions of temperature and thermal stresses in TS(2), made of Type 304 stainless steel, indicating by solid lines those of q_a maximum and by dotted lines those of $(\sigma_{\theta\theta})_a$ maximum, a circumferential thermal stress at the inner surface. The former arises just after the change, when the temperature of TS(2) descends only in the neighborhood of the cooling surface. The latter becomes a maximum of 6600 kg/cm² at 6.5 sec after the incipience of change, when the effect of temperature change reaches the insulating surface. The

FIG. 14. Distribution of Temperature and Thermal Stresses in Test Section TS(3).

radial stress σ_{rr} , its peak in the middle of the wall, represents tension, although considerably smaller compared to $\sigma_{\theta\theta}$, which becomes tension at the inner surface and, conversely, compression at the outer surface.

If the thermal stress swings from zero to 6600 kg/cm² several thousand times, it may possibly bring about thermal fatigue of the material. The interesting results are that the heat flux is, of course, proportional to the temperature gradient, but the thermal stress is not, preferentially depending upon the temperature difference between the inner and outer surfaces.

Figure 14 represents the distributions of temperature and thermal stresses in TS(3), made of Type 304 stainless steel, exposed to a milder thermal shock than in Fig. 13. In this figure, q_a and $(\sigma_{\theta\theta})_a$ attain their maxima at the same time, 10 sec after the incipience of change. Thermal stresses in the copper and brass test sections do not become a problem of discussion because the temperature distributions are flat in those test sections.

7.2. Expression by Nondimensional Numbers

The normalized thermal stress σ_* given in Eq. (18) is determined by the following variables: a normalized time F (Fourier number), a normalized heat transfer B (Biot number), the radius ratio ξ of the hollow cylinder, and a constant which determines the curve of variation of the fluid temperature. The three variables are expressed as

$$F = \frac{\kappa t}{a^2}, \quad (20)$$

FIG. 13. Distribution of Temperature and Thermal Stresses in Test Section TS(2).

FIG. 15. Expressions in Terms of Nondimensional Numbers.

$$B = \frac{2ah}{k} \times 0.01, \quad (21)$$

and $\xi = \frac{b}{a}, \quad (22)$

where k is thermal conductivity of the test section and h has been already discussed. If it is assumed that the fluid temperature varies along a unit lag, its time constant τ determines its variation. Then the other variable is

$$F_0 = \frac{\kappa\tau}{a^2}. \quad (23)$$

Figure 15 represents the comparison of the results obtained from experiments with those theoretically calculated under conditions that the initial temperature is zero, the outer surface of the hollow cylinder is insulated, and the inner surface is heated (or cooled) by fluid whose temperature varies along a unit lag. The topmost curve is fitted to the results obtained from the Type 304 stainless steel test section TS(2). The second, third, and lowest curves are fitted, respectively, to the results obtained from the copper, brass, and Type 304 stainless steel test sections TS(3).

The plotted results are limited within ranges of good accuracy. Although some corrections were necessary for F_0 to make the curves fit the corresponding results, little differences are found between the curves and the experimental data. These differences are considered to originate in the difference between the fluid-temperature variations and the approximate variations in unit lag.

Now that it has been verified that the heat-transfer coefficient can be taken to be a constant even in thermal transients, the response of thermal stress can be followed

by the theoretical calculation, provided that the fluid-temperature variation is given exactly.

8. Conclusion

An experimental technique whereby heat transfer and thermal stresses are obtained from measured temperature variations has been presented and its usefulness verified. The results obtained are summarized as follows.

(1) The heat flux varies, depending sensitively upon the temperature variation of the sodium flow; the heat flux has its highest value initially and subsequently descends gradually for a step change of sodium temperature; the heat flux rises to the peak value similarly and descends for a gradual change of sodium temperature.

(2) The heat-transfer coefficient passes its transient state almost instantaneously, and it can be estimated as a stationary one.

(3) The heat flux decreased accordingly as the rate of heat conduction in the solid wall becomes unable to respond to the rate of heat transfer to sodium. The heat-transfer coefficient derived from the copper test section is as large as that calculated from Lubarsky and Kaufmann's equation, whereas the coefficient from a stainless steel test section is about half that given by the equation, according to our definition of the heat-transfer coefficient.

(4) The thermal stress at the heating (or cooling) surface is also sensitive to variation of sodium temperature. If the variation of sodium temperature is given, the response of the thermal stress can be exactly calculated.

9. Nomenclature

Symbols

a	Inner radius of tube	cm
b	Outer radius of tube	cm
E	Modulus of tube elasticity	kg/cm ²
h	Heat-transfer coefficient	kcal/m ² -hr-°C
k	Thermal conductivity of tube	kcal/m ² -hr-°C
k_s	Thermal conductivity of fluid	kcal/m ² -hr-°C
m	Order of time interval	
n	Order of progression	
q	Heat flux	kcal/m ²
r	Radius	cm
T	Temperature	°C
ΔT	Maximum temperature difference between fluid and tube	°C
t	Time	sec
Δt	Time interval	sec
u	Radial displacement	cm
v	Flow velocity	m/sec

β	Coefficient of thermal expansion	$^{\circ}\text{C}^{-1}$
κ	Thermal diffusivity	cm^2/sec
κ_s	Thermal diffusivity of fluid	cm^2/sec
ν	Poisson's ratio	
ξ	Radius ratio	
τ	Time constant of unit lag	
σ	Stress	kg/cm^2
σ_*	Normalized stress defined by Eq. 18	

B	Biot number = $2ah/k$
F	Fourier number = $\kappa t/a^2$
Nu	Nusselt number = $2ah/k_s$
Pe	Peclet number = $2av/\kappa_s$

Subscripts

a	Inner
b	Outer
i	Inlet
o	Outlet
p	Primary or NaK
s	Secondary or sodium
rr	Radial
$\theta\theta$	Circumferential
zz	Axial
1	Location No. 1
2	Location No. 2
3	Location No. 3
4	Location No. 4
5	Location No. 5

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Discussion

Mr. Latzko (Delft): Does your second conclusion, that the "transient" heat-transfer coefficient can be estimated as a stationary one, contradict the results that the heat-transfer coefficient obtained from the Type 304 stainless steel test section are half the values of Lubarsky and Kaufmann's equation?

Mr. Sagawa: I do not agree with this; I mean that this difference concerns only the value itself, and has no relationship between "transient" and "stationary." In my paper the "transient" heat-transfer coefficient is defined with respect to time, not with respect to distance from the starting point of heat transfer.

Mr. Latzko: Why is the heat transfer at the copper test section larger than that of the Type 304 stainless steel test section?

Mr. Sagawa: The reason why the heat-transfer coefficient is larger is unexplained and will be the subject for future study. I am now carrying out a theoretical analysis of this problem, but I have not yet obtained a concrete answer. I am thinking of the following points as the reasons.

The first is the axial heat conduction in the solid wall. This may produce some effect on temperature distributions on the wall downstream. The effect of this axial conduction in the solid wall may be largest in the copper test section and smallest in the Type 304 stainless steel section.

The second is the heat-transfer coefficient which is defined "tentatively" in our experiments. Whether the temperature profile of sodium flow is or is not proportional to the heat flux on the wall surface is not clear.

The third is about temperature measurements. The measurement of surface temperature is very difficult; it is one of the biggest problems in heat-transfer experiments. We measured the inner surface temperature with thermocouples which were secured in small grooves. As I mentioned, a half-side of the hot junction

is exposed to sodium flow. The thermal conductivity of the solid wall may have little effect on the temperature measurements.

Mr. Latzko: Which results, the results from the copper test section or those from the stainless steel test section, should designers choose?

Mr. Sagawa: To be on the conservative side, the designer should choose the values from the copper test section.

Mr. Latzko: Then why did you show the results from the stainless steel in Fig. 12?

Mr. Sagawa: These results have been found by experiment. I refer you to my first three conclusions, of which the second is the most important. It could be said that the first conclusion is the consequence of the second and the third conclusion is a proposition.

SESSIONS ON LARGE FAST REACTOR DESIGN

SESSION LFRD-V

Saturday Morning, November 9, 1968

Chairman: W. J. McCarthy
Detroit Edison Co.

Secretary: J. H. Tessier
Argonne National Laboratory

The Effect of Using Different Safety Criteria and Selected Safety Studies for a 1000-MWe Sodium-cooled Fast Reactor

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1. Introduction and Conclusions

In the course of carrying out studies on the 1000-MWe LMFBR Follow-on Program for Argonne National Laboratory, General Electric has considered two contrasting approaches to LMFBR safety. The consequences of these two approaches, which lead to two different reactor designs, were considered in terms of economic effects, safety effects, and development requirements. From these we have drawn some conclusions about the type of safety approach that we believe should be pursued for the LMFBR, and about the corresponding general characteristics of the reactor. These conclusions, if implemented, would have significant consequences for certain aspects of the LMFBR development program.

The first approach to LMFBR safety is essentially that assumed in previous conceptual designs of LMFBR plants, and actually applied in the design of Fermi, EBR-II, and SEFOR. It is here called a "conservative" approach, not in the sense that it is more cautious, but simply because it follows past procedures. A distinguishing feature of this approach is that it has assumed that a core-disassembly accident (usually caused by meltdown, or possibly also by sodium voiding in some core designs) cannot be prevented and has provided containment features to accommodate it. In the conservative approach, the use of probabilistic considerations is usually avoided, if possible. Where they cannot be avoided, they appear implicitly in the form of judgments which are made generally without any formal analysis of probabilities.

It is clear, qualitatively, that the magnitude of the energy release in a core disassembly is dependent on the core design. In particular, the dependence on Doppler coefficient has been estimated.⁽¹⁾ A dependence on sodium-void reactivity is expected because of the possible contribution of sodium voiding to rates of reactivity insertion, either independently or in conjunction with reactivity changes due to fuel movement. Therefore, modifications of core design to increase the Doppler coefficient and to decrease the sodium-void reactivity should, in principle, reduce the

magnitude and/or the probability of core-disassembly accidents. Previous conceptual designs have made significant compromises in core design in order to achieve this result and, in addition, have made provision for containing energy releases up to 1000 MW-sec of available work.

Although these provisions certainly would seem to increase safety, there is as yet no way of determining the degree of improvement or of assessing whether the improvement is worth the cost. It should also be realized that given sufficiently pessimistic assumptions no absolute assurance exists that the core designs proposed are not capable of calculated energy releases exceeding the capability of the proposed containments. Consequently, the degree to which the core design is compromised, and the design-basis accident for the containment, are arbitrary to a considerable extent. With this in mind, the following criteria were assumed to define a "conservative" core design:

1. Complete voiding of the core and blankets shall result in a zero or negative reactivity effect.
2. The Doppler $T(dk/dT)$ with sodium voided from the core and blanket shall be -0.004 or greater (in magnitude).
3. Containment shall be furnished for a core-disassembly accident. In the absence of detailed studies, 2000 MW-sec of available work were assumed to be representative of the amount of energy to be contained.

The first of these criteria will avert large sodium-reactivity effects as long as voiding occurs at different times in different parts of the core. This should be the case in many circumstances that might be postulated. The second and third criteria are based on the aforementioned calculations of the effect of the Doppler coefficient on energy release.

The second approach to LMFBR safety is to make extensive and explicit use of probability analysis to optimize the plant design under the constraint of achieving a defined, acceptable level of safety. It appears likely that one could assure by design that severe core-disassembly accidents have such low prob-

ability that containment need not be provided for them. It then follows that there is no point in making compromises in core design to reduce energy releases. The core can be optimized for nuclear performance with design attention concentrated on the measures necessary to reduce the consequences of the more probable minor accidents and to provide an acceptable degree of assurance that neither gross sodium voiding nor core melting will occur. This approach will require not only use of the techniques of probability analysis, but also higher reliability requirements for components and systems, and a more elaborate instrumentation system than does the conservative approach. It also implies a need for substantial change in currently accepted procedures and attitudes, of which more will be said. The assumed result of this second approach is termed the "advanced" design.

The conclusions that we have drawn from this study are summarized as follows:

1. The fuel-cost incentive for a low-leakage core design is large, being at least 0.2 mill/kWh, and possibly as high as 1 mill/kWh. The economics of a conservative core are much more vulnerable to uncertainties in nuclear data and unit fabrication costs, and to possible revisions in the void-reactivity criterion.

2. Within the accuracy of the available estimates of capital costs, there is no difference in capital cost between the conservative and advanced plant designs. The reduced cost of containment for the advanced design is offset by increased cost for instrumentation and control.

3. In the more probable accidents the advanced and conservative cores are equally safe.

4. The only safety drawback of the advanced design is its large reactivity effect due to sodium void, which becomes important only if gross sodium voiding occurs. We expect that systematic probability analyses will eventually show that accidents involving gross sodium voiding can be made to have very low probability, so that severe compromises in the design to reduce the effect of void reactivity are not merited (given achievement of reasonable development goals).

5. A strong incentive therefore exists to support the choice of an advanced core design. Containment provisions would be added to the plant only if containment appeared to be the most economical way to achieve acceptable safety, and might therefore be relatively minimal compared to a conservative design containment. It should be possible to make the safety of the advanced design comparable to that of the conservative design.

6. The development and application of probabilistic methods for the safety analysis of fast reactors should be undertaken in the near future. For the advanced design, the need for this is obvious. Even for a conservative design, however, it is impossible to make a well-founded choice of criteria for core-safety design or of a containment design without the use of a probabilistic approach.

2. Comparison of Advanced and Conservative Core Designs

2.1. Economic Comparisons

Application of the two safety approaches resulted in two contrasting core designs. In arriving at these designs emphasis was put on the choice of parameters that would result in near-optimum costs, given the various safety and design criteria. Some of this optimization work will be described in other papers at this conference.^(2, 3)

Table 1 shows some of the principal reactor characteristics of the two designs. The two major differences that result from the different safety criteria are the differences in core geometry and composition. The active height of the conservative core is 18 in. compared with 36 in. for the advanced core. The short dimension is needed to promote leakage to satisfy the criterion for void reactivity. This then necessitates higher enrichments and requires the use of BeO to satisfy the Doppler criterion. These differences, in turn, produce many of the other variances in characteristics, such as the lower internal conversion ratio, the larger number of control rods, and the lower pressure drop in the core for the conservative design.

As indicated before, the fuel-cycle costs represent the major differences in cost between the concepts. Figure 1 shows the fuel-cycle costs for both the advanced and conservative designs as a function of plutonium value. Because of the uncertainty in particular nuclear data and the uncertainty associated with the safety criteria of the conservative design, alternative conditions were evaluated. For example, a more stringent criterion for sodium void was applied to the conservative design. The base case shown on the curve corresponds to a criterion for maximum void reactivity of approximately \$2.65 and used the existing Brookhaven Evaluated Nuclear Data File (ENDFB) nuclear data. The lower boundary of the "reduced void criterion" curve corresponds to a maximum void reactivity of \$1.00 and the upper boundary corresponds to 50¢, also with the existing ENDFB nuclear data file.

More recent British work and measurements per-

formed by Oak Ridge National Laboratory at Rensselaer Polytechnic Institute have indicated that the plutonium-alpha value utilized for these calculations is probably too low in the energy range between 1 and 10 keV. An increase in the plutonium-alpha value affects the conservative core more than the advanced core, since it not only decreases the breeding ratio and increases the required enrichment, but also increases sodium-void reactivity. Thus, the conservative core must make further compromises in design to satisfy any given void criterion. The range of costs estimated for the increased plutonium-alpha values corresponds at the upper end to the pessimistic side of the error bars obtained by Sowerby and Patrick at Harwell,⁽⁴⁾ and at the lower end to our own estimate of the most probable values.⁽⁵⁾

It should be noted that if increased alpha values are combined with more stringent criteria for sodium-void reactivity, the economic effects are not simply additive, but tend to reinforce each other.

The economic criteria of Fig. 1 are quite optimistic, and should be considered as representative of the cost targets for a large and fully matured fast breeder industry. Figure 2 shows fuel-cycle costs based on less optimistic assumptions, such as might be appropriate in the near term when fabrication and reprocessing plant throughputs are low. Again, the effects of alternative safety and nuclear data are also considered.

From examination of both Figures 1 and 2, it is concluded that under optimistic circumstances the advanced and conservative designs show a difference of only 0.2 mill/kWh. However, the potential for this difference to increase is high. Under some circumstances it is doubtful that the conservative concept could compete with other forms of power generation expected to be available in the 1980 period (the value of 1.5 mills/kWh for fuel-cycle costs for the reduced void criterion in Fig. 2 is comparable to the fuel-cycle costs of today's water reactors). The advanced design shows excellent fuel-cycle costs under all circumstances.

2.2. Safety Comparisons

The relevant comparison between the conservative and advanced designs insofar as safety is concerned is to determine whether gross voiding and/or meltdown accidents are more likely to occur in one design than in the other. This comparison was made by considering whole-core accidents involving reductions in flow and accidental reactivity insertions, and locally initiated accidents such as fuel failures and assembly blockages. In each case the response of each design was analyzed to determine whether sig-

TABLE 1. PRINCIPAL REACTOR CHARACTERISTICS OF CONSERVATIVE AND ADVANCED CONCEPTS

	Advanced	Conservative
Core height, in.	36.0	18.0
Core diameter (equivalent cylinder), in.	81.8	110.3
Axial blanket thickness (each end), in.	15.0	18.0
Fuel material	(U, Pu)O ₂	(U, Pu)O ₂
Number of control rods	31	55
Specific power (core average), kW/kg		
U + Pu	186	218
Fissile (midcycle)	1450	1210
Core fissile load, kg Pu-239, -241	1404	1562
Core conversion ratio (zone 1/zone 2)	1.01/0.75	0.71/0.55
Fissile enrichment (zone 1/zone 2), %	11.3/14.5	15.5/18.8
Breeding ratio	1.37	1.32
Doubling time, yr	7.8	10.2
Fuel-pin OD, in.	0.230	0.220
Ratio of pin pitch to diameter	1.20	1.22
Subassembly pressure drop, psi	191	57
Peak cladding temperature at full power, °F	1300	1265
Mixed mean coolant outlet temperature, °F	1150	1150
Temperature rise of core coolant, °F	295	270
Reactor vessel diameter, ft	10.5	13.9
Blast shields and meltdown trays	No	Yes
Power output at midcycle (total reactor), MWT	2345	2245
Core composition at midcycle, vol %		
Fuel	41.7	38.6
Sodium	37.7	38.9
Steel	20.5	19.0
BeO	—	3.0
Tantalum	0.1	0.5
Refueling interval, months	6	6
Average burnup, MWd/Te U + Pu	112,200	112,800
Plutonium composition, as-supplied, 239/240/241/242,	0.784/0.193/0.018/0.005	

nificant differences existed in terms of the response required of the control and instrumentation systems to prevent core damage, and therefore to prevent gross voiding or melting.

The reactivity coefficients for the two reactor concepts are shown in Table 2. All coefficients were computed by a two-dimensional calculational model. (It was found that one-dimensional analysis was inadequate for this purpose, giving errors in sodium coefficient of about 40% and predicting greatly decreased effectiveness of axial fuel and radial core growth, as compared with a two-dimensional analysis.)

FIG. 1. Fuel-cycle Costs with Optimistic Economic Criteria for Advanced and Conservative Designs.

FIG. 2. Fuel-cycle Costs with Alternative Economic Criteria for Advanced and Conservative Designs.

TABLE 2. REACTIVITY COEFFICIENTS BASED ON TWO-DIMENSIONAL ANALYSIS OF ADVANCED AND CONSERVATIVE CORES

	Advanced	Conservative
Fixed core size		
Fuel $\rho dk/d\rho$	+0.31488	+0.3835
Sodium $\rho dk/d\rho$	-0.01398	-0.0061
Steel $\rho dk/d\rho$	-0.04434	-0.0286
BeO $\rho dk/d\rho$	—	-0.005
Fixed density		
Hdk/dH	+0.15712	+0.2848
Rdk/dR	+0.107	+0.0612
Net effects		
Axial fuel growth ^(a)	-0.15776	-0.0987
Radial core growth ^(b)	-0.47644	-0.6544
Doppler coefficient ($-T dk/dT$)		
Sodium in	0.0076	0.0067
Sodium out	0.0050	0.0046
Effect of sodium-void reactivity, ^(c) \$		
Total core and blankets	2.50	-0.08
"Maximum" ^(d)	6.00	2.65
One-bundle "maximum" ^(e)	0.08	—

^(a) $Hdk/dH - \rho dk/d\rho$ (fuel).

^(b) $Rdk/dR - 2 \times \rho dk/d\rho$ (fuel, sodium, steel, BeO) + effect of added volume of sodium when core expands.

^(c) $\beta_{eff} = 0.0035$.

^(d) Based upon partial voiding outward from core center to $\frac{2}{3}$ of core height and 80% of core volume in radial direction.

^(e) Based upon one subassembly at the center of the reactor.

In order to make the analyses it was necessary to identify the point at which significant fuel damage would be considered to occur. In the context of this study, it was felt that to have a sufficient degree of assurance that any large-scale core damage would be precluded, a very conservative criterion should be used to define a point before which an accident must be terminated by scram. The criteria chosen are as follows:

1. Significant fuel damage is defined as gross failure of the clad in one fuel pin, using all nonrandom peaking and hot-spot factors in the calculation.

2. Gross clad failure is assumed to occur if:

- a. clad surface temperature exceeds the sodium saturation temperature at the local pressure, or
- b. 50% of fuel in pin cross section is molten.

Rupture of the fuel pin due to fission gas pressures and elevated clad temperature was not considered since the preferred fuel-pin design was vented to the coolant.

2.2.1. Whole-core Accidents

A detailed examination of the response of the advanced and conservative designs to a range of hypothetical whole-core accidents showed no significant

TABLE 3. EFFECT OF FEEDBACK ON THE REACTIVITY ADDITION REQUIRED TO INITIATE FUEL FAILURES

Insertion Rate.....	1 \$/sec		5 \$/sec	
Reactor	Ad- vanced	Con- servative	Ad- vanced	Con- servative
Feedback, ^(a) \$				
1. No overall changes of core dimensions	1.13	1.19	1.37	1.47
2. Axial fuel growth caused by axial expansion of cladding	1.19	1.23		
3. Gross radial core expansion	1.25	1.36		
4. Cladding-controlled-axial plus gross radial expansion	1.31	1.40		
5. Fuel ^(b) axial plus gross radial	~1.60	~1.65		

^(a) All cases include Doppler feedback, cladding and structure axial expansion, sodium thermal expansion, and displacement of sodium by radial expansion of the fuel pin.

^(b) The fuel (oxide) is assumed to have no restraints to axial growth inside of the cladding and expands because of its own temperature rise. In reactivity-addition incidents from full power, this expansion is larger than the cladding expansion.

difference between the two concepts with respect to the time to initiation of fuel damage and/or sodium boiling. This work, carried out using the FORE-II program, examined severe reactivity and flow-reduction accidents not terminated by scram. The results give a measure of the response time of the control system required to prevent fuel damage for specified degrees of flow reduction or reactivity additions. The analyses took account of the mechanisms for reactivity feedback that would normally be expected to come into play. Because of the uncertainties in many of these feedback effects a number of combinations were examined. In accordance with safety criteria imposed to allow for calculational uncertainties, only 80% of the calculated Doppler* effect was assumed in the analyses.

For the analyses of reactivity insertion, a number of cases were run at arbitrary ramp rates of 1 \$/sec and 5 \$/sec. Table 3 shows the amount of reactivity that is added to the reactor at full power at these ramp rates before cladding failure. Both the advanced and conservative reactors can accommodate rapid reactivity insertions of slightly more than \$1 before the postulated cladding failure and resulting addi-

tional reactivity addition occurs. The conservative design has a trivial advantage in the amount of reactivity that can be inserted before fuel failure starts. The effect of various combinations of feedback mechanism are also shown in Table 3.

From Table 3, it can be seen that there is little difference in the response of the conservative and advanced cores. Moreover, for rates of reactivity insertion of 1 \$/sec or less a scram triggered by 10–20% overpower, with rods moving substantially into the core within 0.5 sec, will probably preclude damage. The resultant specifications for the control system are not difficult to meet. Table 4 shows that the time to cladding failure for both the advanced and conservative cores, for a reactivity ramp of 1 \$/sec and for axial movement of the fuel keyed to the clad expansion (no radial expansion feedbacks), is of the order of 1.2 sec. Also shown in Table 4 are some of the other core characteristics and parameters at 1.2 sec after start of the transient. The average fuel temperature in the core is 3900°F for the conservative core and 3500°F for the advanced core, and the power of both cores has increased by a factor of 5.

For the analyses of flow reduction, the flow was reduced from the normal, full-power condition with a rapid ramp to a fraction of the normal flow. The inlet temperature was held constant. No external re-

TABLE 4. COMPARISON OF RESPONSES OF ADVANCED AND CONSERVATIVE CORES TO 1 \$/SEC INSERTION, AXIAL FEEDBACK CAUSED BY THERMAL EXPANSION OF CLADDING
(Fuel Matrix Restrained Axially; Expands as Cladding Expands)

	Advanced	Conservative
Initial power P_0 , MW	2188	2281
$\bar{T}$ fuel, °F	2351	2453
Time to failure and start of voiding, sec	~1.17	~1.22
P/P_0 at t'	5.26	5.36
$\bar{T}$ fuel, °F	3532	3864
Δk added, \$	1.170	1.222
Δk Doppler, \$	-0.659	-0.590
	0.511	0.632
Δk other feedbacks, \$	+0.185	+0.058
	0.696	0.690
Δk net, \$		
Breakdown of other feedbacks, \$		
Fuel and core axial expansion	-0.0893	-0.058
Cladding	+0.0152	+0.010
Sodium	+0.2512	+0.100
Structure	+0.0074	+0.005
BeO	—	+0.001

* Calculated Doppler $T(dk/dT)$ for the advanced core was estimated after these transient calculations, resulting in a reduction in magnitude from -0.0082 to -0.0076. The value used in transient analysis was 80% of -0.0082.

TABLE 5. RESULTS OF FLOW-REDUCTION CALCULATIONS

Reactor	Flow Reduction, %	Axial Fuel	Radial Fuel	Results
a. Advanced	55	No	Yes	No voiding
b. Advanced	55	No	No	Voided
c. Advanced	55	Yes	No	Voided
d. Advanced	65	Yes	Yes	Voided
e. Advanced	75	Yes	Yes	Voided
f. Conservative	55	No	Yes	No voiding
g. Conservative	65	Yes	No	Voided
h. Conservative	75	Yes	Yes	Voided

activity changes were made during the transient. The cause of the flow reduction was not defined.

In general, the fuel (oxide) temperatures and the cladding temperature do not increase more than several hundred degrees prior to sodium voiding. Sodium voiding was assumed to occur if either the fuel cladding or the sodium itself exceeds the saturation temperature of sodium. No superheating of the liquid sodium was assumed in the analysis; this is a conservative assumption insofar as the time to failure is concerned.

Table 5 summarizes the results obtained from some large (>50%) flow-reduction accidents. As noted from the table, the extent of flow reduction which can be tolerated before fuel damage occurs (voiding commences) varies according to the assumptions concerning the feedback effects. For example, a radial-expansion feedback is seen to be effective in preventing damage for a 55% flow-reduction accident for either the conservative or advanced cores. Analysis of the calculations indicated that beyond a 65% reduction both the advanced and conservative cores will experience clad damage and sodium voiding.

The accidents discussed thus far are not necessarily probable, but they may be taken as indicative of the response of the advanced and conservative cores to the specified hypothetical reactivity insertions and flow reductions. The results indicate little difference between the two cores. Analyses of slower accidents might show greater differences, but the slower accidents would be less demanding on the control system response for either core.

2.2.2. Locally Initiated Accidents

For accidents initiated by local events, such as flow blockage of a fuel assembly or failure of a fuel pin, there is no reason to expect the two core designs to behave differently in any qualitative sense, for the fuel-pin and fuel-assembly designs, the fuel burnup, and the operating temperatures are not significantly different in the two designs. Reactivity feedbacks due

to abnormal conditions in a single assembly will become significant until a sodium void forms in a large part of the assembly or until a large fraction of the fuel melts and redistributes itself. The reactivity addition due to complete voiding of an assembly in the center of the core is estimated at 8¢ for the advanced design, and a negligible amount for the conservative design. The reactivity addition due to a maximum possible compaction of the center assembly, with both the top and bottom moving toward the center, is estimated at 30¢ for the advanced design and 20¢ for the conservative design. As will be shown later, these amounts, even if inserted as a step, are far below what would be required to cause damage to the rest of the core, given a normal scram response to an overpower indication. Furthermore it is more probable that any actual reactivity changes due to fuel movement in a single channel will be quite small, possibly even negative. Hence the single-assembly sodium-voiding reactivity is the more important quantity.

From this point of view the 8¢ of void reactivity available from an advanced-design assembly, while too small to cause a severe excursion, might nevertheless provide additional information to the reactor safety system and thus improve the probability of rapid scram. In any case it is not likely that the reactivity characteristics of the advanced design will be found to cause any important disadvantage in detecting and controlling locally initiated accidents.

3. Safety Performance of the Reference Advanced Design

The viability of the advanced design rests not only on its providing safety comparable to the conservative design in the more probable accidents for which large-scale core damage is prevented by the control system, but more importantly in showing that the probability of progressing to core disassembly with large energy releases can be made exceedingly small. A number of accident analyses were performed specifically for the advanced core in order to obtain initial information on this problem.

The core design on which these analyses were performed was slightly altered from the design described in Table 1. The core height was reduced from 36 to 30 in. because of a reevaluation of the overall cost of pumping power, and the clad thickness was specified at 10 mils rather than the nominal 15 mils used for the comparison studies between the advanced and conservative cores. The thinner clad is the result of making a definite choice of a vented fuel pin, of optimization studies described in Ref. 2. In addition, the plutonium-alpha value was adjusted up-

ward⁽⁵⁾ and the plutonium isotopic composition assumed for fabricated fuel corresponds to water-reactor discharge rather than fast reactor recycle. Table 6 indicates the characteristics of the advanced design as finally modified. This is considered to be the reference for the 1000-MWe LMFBR study. Table 7 shows reactivity coefficients for the design.

These accident analyses assumed that scram would occur, and consequently a set of assumptions was necessary to define the response characteristics of the instrumentation and control system, and to define the points at which a signal would be sufficient to initiate a scram. Table 8 summarizes these assumptions. They are, in general, representative of current technology as far as response is concerned.

A number of analyses of whole-core flow reductions and reactivity insertions were performed. To allow margin for calculational uncertainties, 90% of the calculated Doppler and 120% of the calculated sodium coefficient were used. Axial expansion of the fuel column was assumed to be controlled by either clad expansion or fuel-meat expansion, whichever was less. In reactivity-insertion accidents the clad expansion was used; in flow reductions the fuel-meat expansion was smaller than the clad expansion. Radial expansion of the core was assumed to occur.

Figure 3 summarizes the result of the analyses of reactivity insertion, which were in the form of limited-magnitude ramps. Each point on Fig. 3 corresponds to a specific amount of reactivity added over a certain length of time. In the area labelled "safe" all damage (as previously defined) is prevented by action of the control system. It can be seen that over \$1 of reactivity can be tolerated even in a step, due largely to the Doppler feedback. The time coordinate of the knee in the curve is dependent on the delays assumed for the instruments and the control system; this point may therefore be shifted to the left by successful development programs. In the reactivity-insertion accidents the primary scram signal is overpower indicated by the neutron monitor.

Figure 4 shows a similar map for ramp flow reductions. If the flow reduction is instantaneous, the system can still respond successfully if as much as 35% of the original flow is maintained. For slower reductions, the required residual flow does not drop to zero, due to the decay heat.

It seems feasible, with current technology and at reasonable cost, to design so that all reactivity additions or flow reductions falling in the "unsafe" regions would be virtually precluded. The acceptability of the advanced design for accidents of this kind would then hinge entirely on the reliability of this instrumentation and the control system. This

TABLE 6. PRINCIPAL REACTOR CHARACTERISTICS OF REFERENCE ADVANCED CONCEPT

Core height, in.	30.0
Core diameter (equivalent cylinder), in.	97.4
Axial blanket thickness (each end), in.	15.0
Fuel material	(U, Pu)O ₂
Number of control rods	31
Specific power (core average), kW/kg	
U + Pu	157
Fissile (midcycle)	1304
Core fissile load, kg Pu-239, -241	1624
Core conversion ratio (zone 1/zone 2)	1.01/0.76
Fissile enrichment (zone 1/zone 2)	10.73/13.45
Breeding ratio	1.40
Doubling time, yr	7.75
Fuel-pin OD, in.	0.25
Ratio of pin pitch to diameter	1.20
Subassembly pressure drop, psi	64
Peak cladding temperature at full power, °F	1300
Mixed mean coolant outlet temperature, °F	1150
Temperature rise of core coolant, °F	340
Reactor vessel diameter, ft	15.3
Blast shields and meltdown trays	No
Power output at midcycle (total reactor), MWT	2417
Core composition at midcycle, vol %	
Fuel	46.7
Sodium	38.0
Steel	14.9
BeO	—
Tantalum	0.4
Refueling interval, months	12
Average burnup, MWd/Te U + Pu	100,000
Plutonium composition, as-supplied,	0.589/0.257/
239/240/241/242	0.122/0.032

can certainly be made very high by an appropriate combination of development and design redundancy.

Local flow reductions in a fuel assembly caused by blockages at, or upstream of, the assembly inlet nozzle were analyzed as a function of the resultant degree of flow reduction. Figure 5 shows the changes of calculated outlet temperature as a function of time. One may conclude that outlet temperature is not a good indicator for detecting sudden local flow reductions, since the change in temperature becomes less as the blockage becomes more severe over the time interval of interest. Furthermore, the response time of present thermocouples is too long to prevent boiling. A radically improved sensor would be needed. Figure 6 shows the assembly flow blockage in more general terms similar to Figures 3 and 4. The effect of system response time is indicated, showing that even if scram begins immediately upon flow blockage, a step reduction greater than 80% cannot be tolerated because of decay heat. On the other hand, reductions of up to 50% can be tolerated without any rapid system response. One may conclude that

TABLE 7. REACTIVITY COEFFICIENTS OF
REFERENCE ADVANCED CONCEPT

Fixed core size	
Fuel $\rho dk/d\rho$	0.3714
Sodium $\rho dk/d\rho$	-0.01397
Steel $\rho dk/d\rho$	-0.03357
BeO $\rho dk/d\rho$	—
Fixed density	
Hdk/dH	0.1953
Rdk/dR	0.1012
Net effects	
Axial fuel growth ^(a)	-0.1761
Radial core growth ^(b)	-0.5816
Doppler coefficient ($-T dk/dT$)	
Sodium in	0.00602
Sodium out	0.0037
Effect of sodium-void reactivity, ^(c) β	
Total core and blankets	2.24
"Maximum" ^(d)	5.20
One-bundle "maximum" ^(e)	0.06

^(a) $Hdk/dH - \rho dk/d\rho$ (fuel).^(b) $Rdk/dR - 2 \times \rho dk/d\rho$ (fuel, sodium, steel, BeO) + effect of added volume of sodium when core expands.^(c) $\beta_{eff} = 0.0035$.^(d) Based upon partial voiding outward from core center to $\frac{2}{3}$ of core height and 80% of core volume in radial direction.^(e) Based upon one subassembly at the center of the reactor.TABLE 8. ASSUMPTIONS ABOUT INSTRUMENTATION
AND CONTROL

A. Response of Control System	
Scram mechanism	150-ms mechanism delay; slightly faster than current technology
Scram rod acceleration	3 g's (2.5 g's—present BWR)
Flux, power meters	10–20 ms—present technology
Flowmeters	500 ms—present technology
Thermocouples	800 ms—present technology

B. Scram Criteria—Whole-core Accident

1. Flux or power indication increases by 15%
2. Core-flow indication reduced by 10%
3. Core outlet temperature—not used (flow indication would usually be faster)
4. Boiling-noise or fission-product detection—occurs subsequent to damage
5. Loss of pump power—flow reductions assumed possible without loss of pump power (for purposes of analysis)

the assembly inlet must be designed to prevent flow blockages greater than 80%, and this should be a feasible design goal. If reductions greater than 80% can be prevented, the same can probably be done for blockages greater than 50%. Thus, the incentive for improving system response to this type of accident is limited. However, instrumentation should be included on every fuel assembly to detect the accident and reduce the duration of any overtemperature conditions. This instrumentation would preferably

take the form of a flow measurement, but might be a measurement of outlet temperature if the assembly is designed to prevent flow reductions greater than 50%.

A more difficult problem, in the current state of knowledge, is that of local fuel failures occurring within an assembly. It will be necessary to achieve a very high level of assurance that such a failure cannot propagate through the core to a point that core disassembly is a possibility, without prior detection and scram. One may distinguish four mechanisms of propagation:

1. Sodium vapor is expelled from the bottom of an assembly and then swept into neighboring assemblies,

FIG. 3. Reactivity-addition Map for Whole-core Accident.

FIG. 4. Flow-reduction Map for Whole-core Accident.

causing reductions in mass flow and/or local hot spots.

2. Reactivity additions due to sodium voiding and/or fuel movement cause power excursions which damage other portions of the core.

3. Within an assembly, debris from the initial fuel failure blocks flow to adjacent fuel pins, causing them to fail; propagation between assemblies then results from an accumulation of fuel material against the channel wall, causing a melt-through.

4. Contact of hot fuel and sodium results in sufficiently rapid heat transfer to cause a sodium vapor "explosion" which damages other parts of the core.

The first of these is thought to be very unlikely; the path between adjacent assembly inlets may be designed so that large vapor bubbles cannot traverse it, and bubbles will tend to collapse rapidly in the inlet plenum, due to the presence of the highly subcooled inlet sodium. Additional analyses and perhaps tests will be necessary to verify this. The second mechanism was already discussed. Potential reactivity additions are not large enough to cause significant propagation of damage before the over-power results in reactor shutdown.

The third possibility was examined using the following simplifying assumptions:

1. As a fuel pin is contacted by debris (either foreign matter or molten fuel) in a subassembly, all heat transfer between the pin and surroundings is stopped (adiabatic failure mechanism).

2. Pin failure is assumed to occur when the average fuel temperature reaches 5000°F. The calculated time interval between the assumed loss of cooling and failure is arbitrarily reduced by a factor of 10 in order to get a conservative estimate of the speed of propagation.

3. Upon failure the debris from the failed pin distributes itself in such a way as to block cooling to adjacent pins. The time required for this distribution is arbitrarily taken to be 0.25 sec.

4. The geometric propagation of the fuel failure within a subassembly starts at the subassembly center and proceeds to each geometric ring of fuel pins failed by the adiabatic mechanism; the failure thus proceeds radially in a stepwise fashion as shown in Fig. 7.

The time for the failures to propagate through the subassembly were calculated with these assumptions. Results are shown in Fig. 8. It is seen that approximately 5 sec is required for complete propagation through the subassembly. Thus, for this very conservative model and these simplified assumptions, at least 5 sec are available before the damage reaches the boundary of the channel wall.

Fig. 5. Behavior of Coolant Outlet Temperature for Flow Blockage in Peak Power Fuel Assembly.

Fig. 6. Flow Blockage in Peak Fuel Assembly.

To determine the time required for the accident to proceed through the channel wall, further simplifying assumptions were made. These involved an assumed layer of molten fuel applied to the wall, which has an insulated boundary on the opposite side of the molten layer from the wall. The heat transfer between the fuel, wall, and sodium on the opposite side of the wall was considered. The resulting analysis showed that from 10 to 30 sec would be required for melt-through of the channel wall.

These results would seem to indicate that considerable time is available to detect and react to this type of propagation. However, reliable indications that an abnormal condition exists may be difficult to obtain, particularly if a decision to scram must be based on such measurements. To evaluate the possible flow reduction in a subassembly which results from the annular propagation model of Fig. 7, it was assumed that any local blockage would extend for the full axial length of the subassembly. The distributed pressure loss and the resulting subassembly flow reduction were then computed. Table 9 shows the results. As can be seen, the sodium

5.220 in. HEX FLATS
 5.259 in. PITCH HEX CHANNEL
 O 250 in. PIN DIAMETER
 O 300 in. PITCH PINS

FIG. 7. Typical LMFBR Fuel-pin Bundle.

FIG. 8. Conservative Estimate of Pin Failure within a Sub-assembly.

flow velocity past the unblocked pins would not be drastically reduced until a major portion of the sub-assembly had been blocked. Unfortunately, this also makes detection of the propagation difficult. Moreover, a more realistic model in which the blockage did not extend over the entire length of the sub-assembly would show a lesser effect and would be even more difficult to detect. Estimates were made of the reduced effect by examining an idealized "flat plate" blockage. The result showed that at the sixth stage of blockage, the flow velocity was reduced to 31.77 ft/sec, compared with 31.02 ft/sec shown in Table 9. At the third stage of blockage the total assembly flow reduction appeared to be perhaps 5%, which might not provide a sufficiently unambiguous signal in either flow or temperature.

Analysis to determine the geometry of relatively

stagnant sodium areas adjacent to regions of local blockages and the time constants associated with the transfer of heat from the stagnant region to the cooler flowing surrounding sodium led to the conclusion that little if any sustained sodium boiling could be relied upon from this cause, so that boiling detectors might not be especially useful. Fission-product detectors are subject to time delays and a high-noise background from vented fuel and/or the small percentage of minor failures that may be expected in any normal steady-state operation.

As a result of these calculations, it was concluded that perhaps a 2-in.-diameter "flat plate" blockage could exist in one horizontal plane without giving a significant outlet temperature, flow, or boiling signal (except in the immediate vicinity of the blockage). Total subassembly flow would change less than 10%. Consequently, there could easily be sufficient fuel released into the coolant to collect and melt through the channel wall. Detection of local failure propagation in time to limit it to a single assembly, *with the reliability required for safety purposes*, therefore appears doubtful. On the other hand, detection of this type of process before it can proceed across the diameter of the next assembly would seem to be assured, using relatively simple flow and/or temperature instrumentation on each assembly. Consequently, it appears that voiding and melting of up to seven assemblies might be a suitable maximum accident which could be used as the basis for containment design with an advanced design approach.

The fourth mechanism, mechanical damage by sodium vapor pressure, depends upon transferring heat from the fuel into the sodium very rapidly. Although the sodium vapor explosion could undoubtedly be detected and used as a basis for shutdown, the propa-

TABLE 9. VELOCITY PAST UNBLOCKED PINS, ASSUMING TOTAL AXIAL BLOCKAGE

(Assumptions Used Overpredict the Velocity and Flow Fraction Reductions)

Blockage Stages	No. Failed Pins	Average D (ft)	Average V (ft/sec)	Bundle Flow Fraction
0	0	0.01125	32.00	1.0
1	1	0.01122	31.96	0.988
2	7	0.01118	31.90	0.953
3	19	0.01112	31.81	0.891
4	37	0.01105	31.71	0.815
5	61	0.01086	31.44	0.716
6	91	0.01057	31.02	0.590
7	127	0.01011	30.34	0.438
8	169	0.00912	28.81	0.261
9	217	0.00612	23.60	0.075
10	271	0.0	0.0	0.0

tion of damage might be too rapid for the action to be effective. Experimental work is being carried out at Argonne National Laboratory and in Great Britain to determine the possible rates of heat transfer. It seems likely that when the conditions required to generate sufficient energy to damage adjacent assemblies are better defined, they will correspond to exceedingly improbable modes of fuel failure. At the present time, however, this area remains a potential problem in damage propagation, and fuel design measures to minimize the probability that finely divided fuel can be suddenly dispersed into sodium should be taken. Mechanisms that might provide a driving pressure for such dispersion during a fuel failure should, and probably can, be avoided. The most obvious source of such a pressure is fission gas, and this consideration is of importance in our choice of a vented fuel design, which always operates with negligible pressure differential. Another possible source of pressure would be melting of fuel, resulting in sudden release of fission gas coupled with fuel volumetric expansion. Fortunately the current calculations indicate that in local failures which do not involve power excursions, the amount of fuel melting prior to clad failure will be very small.

4. Disassembly Accidents in the Conservative Design

Since the conservative approach to safety generally requires consideration of a disassembly accident, and since the conservative core is compromised in its design to reduce the magnitude of such an accident, it is logical to consider briefly what energy release the containment might have to deal with. A pure conservative approach could make no explicit use of probabilistic concepts; if it did, logic would require that the probability of meltdown or voiding be considered before one assumed that a disassembly accident must be contained. A strict application of the conservative approach to safety requires, therefore, that probability not be considered in the fixing of the magnitude of a containment design-basis accident, any more than it was in deciding on the nature of the accident. We proceeded, then, to investigate some possibilities for a design-basis accident for the conservative core. One of these possibilities is described. The events postulated ignore all questions of credibility (that is to say, of probability), except that they are not physically impossible in the light of our current limited knowledge.

An arbitrary reactivity input of 20 ϵ /sec was postulated, without scram. The absolute ramp rate is not critical; one might use the maximum rate of normal rod withdrawal in the following analysis and obtain similar results.

FIG. 9. Channel Void Worth (18-in. Core).

FIG. 10. Void Growth vs Time (18-in. Core).

The eventual result of this input ramp is fuel failures caused by exceeding the 50% molten criterion. These failures occur at the axial center of the fuel pins, beginning with the highest power assemblies (core center) and proceeding to the lower power assemblies according to the radial power gradient. As each fuel assembly sustains a failure, it is assumed to void due to the contact of hot fuel and sodium. The driving pressure for voiding is not known in these circumstances; for purposes of illustration it is assumed to be a constant 1000 psi.

Figure 9 shows the relative void reactivity worth as a function of position in the core for the conservative core. Figure 10 shows the axial void-growth patterns as a function of time in the core for an arbitrarily assumed driving pressure in the channels of

FIG. 11. 20-¢/sec Voiding of an 18-in. Core.

1000 psig. Figure 11 shows the application of the data from Figures 9 and 10 to obtain the voiding-reactivity histories of the individual channels in the conservative core. The pulses occur as a result of the voiding of consecutive rings of fuel channels. Although the peak rate of reactivity input by the voiding channels is seen to be 176 \$/sec, the peak cumulative void worth input is only approximately 60 ¢. In order for disassembly to occur, a threshold for reactivity of from \$1.50 to \$2.00 is required. The particular reactivity insertions illustrated in Fig. 11 are below this level.

Unfortunately, the results are highly dependent upon the assumptions used in the accident. For example, if a lower driving pressure were used instead of the 1000 psig, the voiding pulses shown in Fig. 11 would be slower, tend to overlap, and be additive as more than one ring of subassemblies voided simultaneously. Although this would reduce the maximum rate of reactivity addition, the total reactivity could exceed the threshold disassembly level. Another point is that the effect of the reactivity pulses shown in Fig. 11 on the failure times in the remaining channels was not taken into account. One of the first pulses might well cause an incremental power pulse that would trigger the remaining assemblies nearly simultaneously. Thus, it is not at all clear that some combination of circumstances cannot be imagined resulting in a ramp rate of 176 \$/sec at the time of disassembly, even in the conservative core.

The conclusion to be drawn from this exercise is not that the conservative core is as difficult to contain in a disassembly accident as the advanced core; with similar sets of assumptions, the conservative-design energy release will often be less than that of the advanced design, and probably seldom or never exceed it. The conclusion is rather that neither type of design can afford the luxury of unrestrained imagination in determining what accidents must be contained.

5. Importance of the Probabilistic Approach to Reactor Safety

The problems resulting from the application of the conservative safety approach are not unique to the LMFBR. The only reason that an impasse has not arisen even in the safety of water reactors is that probabilistic considerations are implicitly accepted, although not systematically applied. For example, the proposed AEC water-reactor criteria require a backup shutdown system. This implies that a judgment has been made that the probability of failure to shutdown is unacceptably high with one system, but acceptably low with two systems. Such decisions are necessary and cannot be made without taking into account judgments on probability even if only qualitatively stated. It would be far preferable to make such judgment based upon systematic analyses and accepted criteria for risk, rather than upon intuition and precedent.

It seems that in fast reactors the crux of the difficulty in applying a conservative approach will be in the definition of a design-basis accident for the containment. It is, in our view, unlikely that one will be able to design for the worst accident permitted by the laws of nature and end up with an economically interesting system, even after extensive

TABLE 10. REQUIREMENTS FOR IMPLEMENTATION OF THE ADVANCED-CONCEPT SAFETY APPROACH

1. Methodology for analyzing system reliability, and probabilities for undesired events.
2. Criteria to establish whether a given probability for a particular accident is sufficiently low to be acceptable, and whether the total accident risk for a plant is acceptable.
3. Adequate components and systems, as needed to meet the requirements deduced for
 - a) Reliability;
 - b) Response characteristics;
 - c) Possibly precluding some accidents through design.
4. Sufficient data on reliability to support the calculations of accident probabilities.
5. Sufficient knowledge of the physical processes involved in accidents to support the calculations of accident probabilities and resulting damage.

Additional research and development has been carried out. In effect, conservative designs cannot avoid judgments of probabilities; there is no such thing as a purely "conservative" approach that is also logical.

On the other hand, although the reference design chosen for the 1000-MWe LMFBR has no special containment for disassembly accidents, it is not to be inferred that an advanced design would never have such containment. A systematic analyses of the probabilities of various accidents would provide a basis for determining what alternative modifications could be made to the design for those accidents whose probabilities did not satisfy the criteria. The most economic modification might in some cases be additional containment (over and above that already inherent in the pot and refueling cell design). For that matter, it is not impossible that some modification of core design might be logical, though it appears at this time that this is an expensive way of buying safety. It is not actually the presence or absence of some particular design feature that is the essential distinction between an "advanced" and "conservative" concept, but rather how the decisions were made that led to the choice of the design characteristics.

It is our conclusion that the use of probabilistic approach to safety is required to assure orderly and successful development of fast reactors. It may be only slightly less critical for water reactors to adopt this approach, to avoid a slow accumulation of safety features which may render them nonoperable (or even noncompetitive) while providing negligible or unverifiable increases in actual safety. Consequently, initiation of the work required to develop and support the advanced-concept safety approach should have a high priority. The time and funding requirements to establish this approach are substantial. Table 10 lists items that must be in existence for a thoroughgoing application of the advanced safety approach.

Item 1, the methodology, is essentially available as standard practice in the aerospace industry, although additional code development will be necessary to adapt these methods to reactor safety. Item 3, adequate components and systems, will require development particularly to achieve the desired reliability levels. An immediate need is to perform further analysis that will provide the basis for preliminary reliability goals for components, subsystems, and systems. This is necessary to determine in each case where additional development is needed and where the desired reliabilities should be achieved through redundancy in already available components.

Item 4 is the data needed to show that the reliability goals are indeed satisfied. Again, work is needed to define exactly what data are needed. Some of the information might be available through testing, but system data under operating conditions will be available only after prototype operation. It will therefore not be possible thoroughly to exploit the probabilistic approach to safety for early plants.

Item 5 applies particularly to acquiring additional knowledge of fuel-failure modes, possible sodium vapor pressures, resistance of the core lattice to damage, and the possible damage corresponding to different energy releases during core disassembly. Item 2 is the most difficult, but the most necessary, of these requirements. The concept of an acceptable level of risk is now accepted by regulatory bodies, industry, and the public. It must however be quantitatively defined in terms that allow its application to a reactor design. It is obvious that the nontechnical aspects of this will make it a difficult thing to achieve. What may not be as obvious is that even from a technical standpoint the definition of a level of risk in such a way that it can be compared to a computed characteristic of a plant design is a problem requiring careful study. The fact that it will not be easy to implement the advanced-concept probabilistic safety approach makes it more urgent that a coordinated program to supply the needs indicated should be initiated without delay.

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Discussion

See Discussion following paper by A. S. Gibson *et al.*, THIS SESSION.

Alternate Core Design Parameters and Economic Trade-offs for a 1000-MWe Sodium-cooled Fast Reactor

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I. Introduction and Conclusions

The 1000-MWe LMFBR Follow-on Studies which General Electric have performed for Argonne National Laboratory has resulted in a choice of a core design which, we believe, represents a reasonable projection of LMFBR performance based on current knowledge and on extrapolations of what may be expected from future development. Considerable effort was made to ensure that the parameters of the design, and therefore the costs, were in a near-optimum region. The interaction of core and coolant system was taken into account. Where great uncertainty existed in important areas, the effect of alternative assumptions was analyzed. This paper describes some of the parametric studies and trade-offs considered in arriving at the reference core design.

Although the number and range of different parameters is too great to allow a thorough coverage, and the uncertainties in material behavior are large, some interesting tentative conclusions may be drawn:

1. The fuel-cost incentives to reduce structure in the core make it probable that it will be optimal to make clad thickness as thin as is consistent with fabrication and corrosion considerations. This conclusion holds for either vented or sealed-pin fuel. Thicker clad might become optimum if the effect of clad restraint on fuel swelling were very strong.

2. The uncertainties in fuel-fabrication and reprocessing costs appear to have effects on optimum pin dimensions, optimum clad temperature, and fuel costs that are equal to or greater than the uncertainties from any other source.

3. The optimum value of maximum bulk (mid-wall) clad temperature appears to be rather insensitive to the range of assumptions investigated, generally falling between 1250 and 1300°F.

4. It appears that a 25% change in the assumed fuel-swelling rate or in the allowable clad strain may alter the fuel cost by 0.02 mill/kWh or more.

5. Vented fuel has economic advantages over sealed-pin plenum fuel that are small, but not insignificant, at clad temperatures in the optimal design region.

6. Only if fabrication costs are very low (~ 200 \$/kg including inventory and losses) and plutonium value high (~ 15 \$/gram) will fuel-pin diameters less than 0.25 in. be optimal.

2. Reference Core Design

Table 1 lists parameters of the reference core design which resulted from the parametric studies. It was not feasible to examine all the independent variables simultaneously, and consequently studies made in reaching this design were performed in two main steps. First, the desirable core geometry and composition were determined using a set of fixed, but reasonable, assumptions for the fuel-pin design and for limits on clad temperature and pressure drop. Second, the fuel-pin dimensions, clad temperatures, and burnup were considered using the geometry and composition (except insofar as affected by changes in fuel-pin design) as determined from the first step. A third, and largely independent, parameter was the thickness of the channel wall of the hexagonal assembly.

3. Core Geometry and Composition Studies

The studies of core geometry and composition were carried out largely in connection with the comparison between the advanced and conservative safety approaches discussed in a previous paper.⁽¹⁾ The core design variables held fixed, and the limits placed on other variables, are shown in Table 2.

Since the thicknesses of fuel clad and the assembly wall are fixed, the structure volume fraction is effectively determined. For a core with no beryllium oxide, only one independent composition variable remains. In Fig. 1 the volume fraction of fuel has been chosen as an independent variable, and fuel-cycle costs* are plotted for a number of core heights. Structure volume fraction is close to 20% for the range of designs covered by Fig. 1. Curves of constant pressure drop and pitch-to-diameter (p/d) ratio are also

* Fuel-cycle costs shown in connection with the parametric studies are approximate; cost trends are more significant than the absolute values.

TABLE 1. CORE PARAMETERS OF REFERENCE CONCEPT

Core height, in.	30.0
Core diameter (equivalent cylinder), in.	97.4
Axial blanket thickness (each end), in.	15.0
Radial blanket thickness, in.	10.9
Core composition at midcycle, vol %	
Fuel (U,Pu)O ₂	46.7
Sodium	38.0
Steel	14.9
BeO	—
Tantalum	0.4
Midcycle fissile enrichment (Zone 1/Zone 2)	10.73/13.45
Breeding ratio	1.40
Specific power (core average), kW/kg	
U + Pu	157
Fissile (midcycle)	1304
Outer diameter of fuel pin, in.	0.25
Clad thickness, in.	0.010
Length of vent device, in.	18.5
Peak bulk clad temperature, full power, °F	1300
Average burnup, MWD/Te U + Pu	100,000
Hexagonal assembly	
Dimension across flats, in.	5.67
Wall thickness, in.	0.06
Mixed mean coolant outlet temperature, °F	1150
Core coolant temperature rise, °F	340

TABLE 2. FIXED PARAMETERS AND LIMITS FOR OPTIMIZATION OF CORE GEOMETRY AND COMPOSITION

Fuel material	Mixed oxide, gas bond
Diameter of fuel pin, in.	0.25
Clad thickness, in.	0.015
Maximum fuel temperature (including over-power), °F	5200
Average burnup, MWD/Te U + Pu	110,000
Assembly wall thickness, in.	~0.060
Ratio of plenum or vent length to core length	2.0
Maximum clad surface temperature, °F	≤ 1300
Maximum coolant outlet temperature, °F	≤ 1150

shown. Enrichment, breeding ratio, and other nuclear data are dependent variables.

It should also be noted that core outlet temperature varies from about 1140 to 1170°F in Fig. 1, roughly as the fuel p/d goes from 1.25 to 1.15, because peaking and hot-spot factors are changing with core design, even though peak clad temperature and core temperature rise are constant at 1300°F and 300°F, respectively. Both the outlet temperature and the pressure drop have an effect on plant efficiency, and this effect is included in the fuel-cycle cost values shown. Specific power is also varied in Fig. 1, due to the changes in peaking and hot-spot factors.

The point initially chosen as representative of an advanced safety philosophy, without explicit limitations on Doppler coefficient or sodium-void reactivity,

is indicated on Fig. 1. It has a 36-in. core height, which was adjusted to 30 in. in the final reference design. It can be seen that the fuel cost differential between different p/d ratios, at constant pressure drop, is very small. There is no incentive to increase core height unless higher pressure drops are acceptable.

When capital-cost effects are included, the efficiency penalty makes the incentive for cores with high pressure drop diminish. This is shown in Fig. 2, which shows differential total costs, related to the initially chosen core design as a base point.

The choices of a core temperature rise of 300°F and a clad maximum temperature of 1300°F, the assumed limit, are due to considerations of plant efficiency and cost of coolant components. Figure 3 shows the variation of costs with rise of core temperature, the outlet temperature being fixed at 1150°F. The main cost trade-off is between pumping-equipment and heat-exchanger costs. Since small rises of core temperature require large sodium flow rates and core pressure drops, the required primary pumping power increases rapidly, and the cost of the pumping equipment rises. On the other hand, large rises of core temperature increase the cost of the heat exchangers by reducing the available log mean temperature differen-

FIG. 1. 1000-MWe LMFBR Fuel Costs vs Volume Fraction of Fuel and Core Height; Advanced Design.

FIG. 2. Differential Cost of Power vs Volume Fraction of Fuel and Core Height; Advanced Design.

FIG. 3. Calculation of Optimum Core ΔT ; Advanced Design with 36-in. Core Height.

tial between the sodium and water. A secondary effect is that of pumping-power requirements on plant efficiency. The resultant curve of differential cost versus core temperature rise shows a shallow optimum around 325°F.

Repeating this process for different outlet temperatures (for each of which the secondary sodium and turbine throttle temperatures were reoptimized) gives the curves shown in Fig. 4. These show clearly the incentive for high outlet temperature. However, the maximum limit for clad temperature obviously cannot be satisfied at all points on this figure, especially for the higher outlet temperatures. Lines of maximum clad temperature are therefore shown, indicating that one cannot actually utilize the minimum point on the 1150°F outlet temperature curve, and the resultant optimum rise of core temperature is 300°F.

Although this analysis of optimum rise of core temperature applies to the 36-in. core initially indicated as the choice for an advanced design, it is true that each core in Fig. 1 will have a slightly different optimum temperature rise. A rigorous optimization procedure would require that all these optima be identified, and the core temperature rise allowed to vary accordingly in Fig. 1. Rather than attempt such

FIG. 4. Differential Cost of Power vs Reactor ΔT and T_{outlet} ; Advanced Design with 36-in. Core Height.

Fig. 5. 1000-MWe LMFBR Fuel Costs vs Volume Fraction of BeO and Core Height; Conservative Core.

a laborious procedure, a revision of Fig. 1 was made utilizing a core temperature rise of 250°F to determine whether this would have a significant effect on the core selection. It was found that it did not.

When considering a core design limited by criteria on sodium-void and Doppler effects, as is the case for a conservative approach to safety, the procedure of optimization is somewhat more complicated. A new variable, that of the core volumetric fraction of beryllium oxide, appears. The criteria that must be satisfied are:

1. Complete voiding of the core and blankets shall result in a zero or negative reactivity effect.
2. The Doppler $T'(dk/dT)$ with sodium voided from the core and blanket shall be -0.004 or greater (in magnitude).

The criterion relating to the reactivity effect of loss of coolant is usually satisfied by designing a relatively high-leakage core. The large fissile inventory of such cores has a deleterious effect on the Doppler coefficient, and its lower limit is then met by incorporating a spectrum-softening material such as beryllium oxide.

The results of the optimization effort on a conservative core are shown in Fig. 5. Fuel cost is again the ordinate, but the abscissa is now the volume fraction of beryllium oxide. Lines of constant core height are

shown. The fuel pin pitch-to-diameter ratio is held constant at about 1.2 in this figure, and the coolant volume fraction is therefore also constant, at 39%. This is not an optimized value, but should be adequate to give a good measure of the performance of the conservative core. As is to be expected, fuel costs increase steeply with BeO fraction. In order to satisfy the sodium-void criterion with a reasonable margin of error (0.003 in Δk for this case), short cores are necessary, or else large BeO fractions. Most of the designs represented on Fig. 5 are excluded by the sodium-void criterion. In order to satisfy the Doppler criterion a certain BeO fraction is required, which increases with decreases in core height. The resultant allowable design area is indicated in Fig. 5. The conservative core design with an 18-in. height and 3% BeO is close to the minimum cost attainable within the allowable design area. It should be noted that the shape of the cost curves in Fig. 5 is such that the costs of an optimum design are very sensitive to uncertainties in the sodium-void or Doppler criteria.

Because of the short height of the conservative core, its pressure drops are relatively low and its peaking factors are different from the advanced core. This is reflected in the fact that the outlet temperature limit, 1150°F, is reached before the clad temperature limit of 1300°F. Figure 6, which is similar to Fig. 4 for the advanced core, demonstrates the point. At or near the optimum coolant temperature rise for an outlet temperature of 1150°F, the conservative core clad temperature is less than 1300°F. The selected values for the conservative core turn out to

Fig. 6. Differential Cost of Power vs Reactor ΔT and T_{Outlet} ; Conservative Core.

be a core temperature rise of 270°F and a maximum clad surface temperature of 1265°F.

A comparison between Figs. 1 and 5 shows the difference in fuel-cycle costs between the advanced and conservative designs to be approximately 0.2 mill/kWh. This was calculated under optimistic assumptions on reprocessing and fabrication costs, and with plutonium at 10 \$/gm. The previous paper⁽¹⁾ covers this difference in more detail.

4. Studies of Fuel-pin Design

As Table 1 shows, the core concept adopted reflects an advanced safety approach, with no beryllium oxide. The final core height was reduced from 36 to 30 in. because of a re-evaluation of the overall cost of pumping power. The fuel-pin design and operating conditions were altered as a result of the studies described below. The major effort was expended on vented fuel, since previous study⁽²⁾ has indicated that it is economically equal or superior to a sealed-pin design, and the elimination of internal pressure is felt to be very desirable in minimizing the possible results of fuel failures.

4.1. Vented Fuel

Work performed on core geometry and composition demonstrated the incentives for a high coolant outlet temperature, which in general necessitates high clad temperature (see Fig. 4). Opposing incentives, for low clad temperature, are due to considerations of fuel-pin design. It is to be expected that with sufficient understanding of clad and fuel design a curve could be generated showing the cost of heat from the core versus clad temperature. Expressing the dependent variable as heat cost (\$/MBtu) rather than fuel cost

FIG. 7. Typical Cost-of-heat vs Clad-temperature Curve.

FIG. 8. Assumed Fuel Swelling vs Burnup and Density.

(mills/kWh) avoids variations of system efficiency, which are a function of outlet, and therefore of clad temperature. Such a curve will probably have the shape shown in Fig. 7. The reason for increasing heat cost would be a need for either thicker clad or lower burnups, as the clad strength is reduced with temperature or as the ductility of the clad is reduced both with irradiation and with temperature. Once the curve of heat cost versus clad temperature is available, the clad temperature, burnup, and thickness can be optimized by balancing the conflicting considerations of fuel cost and plant efficiency.

Unfortunately, sufficient data to substantiate one particular curve are as yet unavailable. However, it is possible to make reasonable assumptions about the behavior of materials, consistent with the limited available data, and deduce the corresponding heat cost-clad temperature variation. The assumptions can then be varied and the sensitivity of the results analyzed. This was the approach taken in this study.

The critical areas of fuel and clad behavior for which assumptions were made were:

1. fuel swelling versus burnup, smeared fuel density, and clad thickness;
2. allowable clad strain versus clad temperature and irradiation.

The reference assumptions for these areas are shown in Figs. 8, 9, and 10. Figure 8 shows the fuel swelling as a function of burnup and smeared density for 15-mil clad. Figure 9 gives the effect of clad thickness, normalized to 15 mils since almost all available data is at that thickness. Figure 10 shows the assumed

Fig. 9. Assumed Effect of Clad Thickness on Fuel Swelling

Fig. 10. Allowable Clad Strain vs Clad Temperature and Exposure Level.

allowable clad strain as a function of clad temperature and irradiation. The implication of Figures 8, 9, and 10, taken together, is that at any given clad thickness and clad temperature, the burnup is limited.

The analyses of clad thickness, burnup, and density were performed holding the radius at the midpoint of the clad thickness constant at 0.216 in. With 15-mil clad this gives a pin diameter of 0.231 in., which corresponds with the result of a brief preliminary optimization of the 36-in. advanced design. Subsequent

work indicated that there was no incentive (at \$10/gm Pu) to reduce the diameter below 0.25 in., but this difference would have little effect on the results.

The effect of fuel smeared density can show up in two ways in the analysis; it may have some effect on swelling, as indicated in Fig. 8, and it can also change the specific power through changing the thermal conductivity and the central-void size. The increased central-void size associated with low-density fuel may well allow significant increases in specific power. The assumptions utilized in this study have that effect.

Figure 11 shows the trends of specific power (in kW/kg of metal) and the core volume fractions as clad thickness and fuel density vary. This information was combined with nuclear analyses to yield Fig. 12, which gives fuel costs as a function of clad thickness, fuel density, and burnup. The fuel costs are given in terms of cents per million Btu, in order to factor out efficiency, which will vary with different outlet temperatures. Not all points on Fig. 12 are actually feasible, since the relationship between allowable strain, burnup, and clad thickness has not yet been taken into consideration.

Fig. 11. Variation of Specific Power and Volume Fractions as a Function of Clad Thickness and Fuel Density.

FIG. 12. Variation of Core Power Costs with Clad Thickness, Burnup, and Density.

TABLE 3. OPTIMISTIC COST BASIS FOR FUEL-OPTIMIZATION STUDY

	Range	0.23-in. Fuel-pin Diameter, 85% Dense
Fabrication costs, \$/kg (U + Pu)		
Core zones	120-250	~200
Axial blankets	25-40	~30
Radial blanket	20-25	~25
Recovery costs, \$/kg (U + Pu)		
Core and axial blankets	60	
Radial blanket	50	
Working capital charge rate, %/year	10	
Fissile plutonium value, \$/gm	10	

The cost assumptions used to generate Fig. 12 and in most of the work described here are given in Table 3. The fabrication and recovery costs are relatively optimistic and would correspond to a well-developed, high-volume industry. The fabrication costs varied over the range indicated as the fuel-pin diameter and fuel smeared density varied.

The data in Fig. 12 may be replotted versus burnup, with clad thickness as a parameter, at any

particular value of fuel density. This has been done in Fig. 13 for a density of 80% of theoretical. In order to satisfy the clad-strain limit, each point on this figure has a maximum allowable clad temperature. The resultant lines of constant clad temperature are indicated.

It was deduced from Fig. 13 and similar figures for other densities that with the assumptions used it is always more economical to design for minimum allowable clad thickness, relative to using thicker clad and higher burnups. The slope of the clad-temperature lines is also sufficiently close to the vertical that gains in clad temperature by using thicker clad (while holding burnup constant) are not large. The increased efficiency possible with a higher clad temperature (and concomitant higher outlet temperature) does not offset the fuel-cost penalty. This conclusion is, of course, largely a result of the assumed effect of clad thickness on swelling. It indicates that the restraint effect must be distinctly larger than assumed in Fig. 9, before thicker clad is worthwhile. Consequently 10-mil clad was chosen as a reference design for vented fuel. The 10-mil thickness is believed to be the minimum feasible, considering allowances for corrosion, tolerances, and handling.

If we assume 10-mil clad, fuel costs may be plotted as a function of maximum clad temperature; at each clad temperature there will be a specified maximum burnup allowed. This is shown in Fig. 14 for fuel smeared densities of 80, 85, and 90% of theoretical. The curves of Fig. 14 can be used in conjunction with calculations of cycle efficiency and capital cost to

FIG. 13. Heat Cost vs Burnup and Clad Thickness.

FIG. 14. Fuel Costs vs Clad Temperature and Fuel Density; Optimistic Cost Basis.

determine the optimum clad temperature and corresponding burnup. The horizontal scale can be translated into outlet temperature, for any given core and core temperature rise, by an expression of the form

$$T_{\text{Clad}} = T_{\text{Outlet}} + \frac{\Delta T}{\delta} + C,$$

where ΔT is the core temperature rise, δ is determined by peaking and hot-spot factors, and C is a constant accounting for temperature drop from the clad to the coolant. The result of the optimization, for the reference core and plant design, was a maximum bulk clad temperature of 1300°F and a burnup of 100,000 MWD/Te U + Pu.

Figure 14 indicates an incentive for lower fuel densities. However, the incentive is small, and depends on the assumptions made about the effect of density on swelling and the size of the central void formed. Data to substantiate the performance of low-density fuel are comparatively scarce, and low-density fuel may be more subject to undesirable axial redistribution than the high-density fuel. As a result, 85% of theoretical density was chosen as a reference value for this study. An incentive appears to exist for obtaining additional data on the performance of fuel with densities from 70% to 85% of theoretical.

Since the cost assumptions used in Fig. 14 are optimistic, an alternative set of assumptions, approximately doubling the fabrication and reprocessing

costs, and raising the charge rate on nondepreciating capital from 10% to 13%, was investigated. The result is the cost curve of Fig. 15, which would result in lower optimum clad temperature (1265°F) than the optimistic cost basis because of the greater slope of the curve of fuel cost versus clad temperature. This is covered in more detail in another paper.⁽³⁾

It is of interest to consider how strong the effect of clad restraint on swelling would have to be to make clad thicknesses greater than 10 mils optimum. Figure 16 shows several possible assumptions that might be considered (as an exercise—no consideration has been given to the probability of these functions actually being correct). These are all of the form

$$\frac{\Delta D/D}{\Delta D/D \text{ with 15-mil clad}} = e^{-B(T-15)}.$$

Using a value of B of 0.15, one would obtain Fig. 17 and would conclude that at any clad temperature below 1350°F, optimum heat cost is obtained at clad thicknesses greater than 10 mils. Efficiency trends might provide further incentive to increase clad thickness, since higher outlet and clad temperatures could thereby be obtained.

Alternative assumptions regarding fuel-swelling rates and clad strain may be considered together; it is only the ratio of allowable strain to swelling rate which is important in determining the fuel costs, as long as total fuel-pin growth does not cause undesirable thermal-hydraulic effects. Figure 18 shows the effect of assuming fuel swelling lower than the

FIG. 15. Fuel Costs vs Clad Temperature and Fuel Density; Conservative Cost Basis.

FIG. 16. Different Assumptions for Relative Effectiveness of Clad Restraint on Fuel Swelling.

FIG. 17. Variation of Heat Costs with Burnup and Clad Thickness.

base assumption by a factor of 0.75, or higher than the base assumption by a factor of 1.25. These variations may also be viewed as representing a higher and lower allowable strain, respectively.

To establish a final reference design, an optimization study was performed on fuel-pin diameter. Figure 19 shows cost trends with fuel-pin diameter for

FIG. 18. Fuel Costs vs Clad Temperature for Alternative Allowable Clad Strain-Fuel Swelling Relationships.

FIG. 19. Fuel Costs vs Fuel-pin-diameter and Plutonium Value.

FIG. 20. Fuel Costs vs Pin Diameter and Plutonium Value.

various values of plutonium, considering only effects of inventory and fabrication cost. Figure 20 shows the same curves corrected for the effect of pressure drop on both fuel and capital costs, and finally shows the \$10/gram of plutonium curve also corrected for effects on the core volume fractions, which are changing because clad thickness is being held fixed. The \$10 plutonium curve exhibits a shallow optimum around a diameter of 0.25 in. It can be seen that the penalties for being off optimum by 20 mils are small, but might become significant if one optimizes at \$5/gram and the plutonium value then rises to \$15/gram.

4.2. Sealed Fuel

The reference core design was based on the use of a vented fuel pin. A sealed-pin concept was also studied in order to evaluate the economic differences.

The performance and economics of the sealed-pin design were evaluated on a comparative basis with the results of the vented-pin optimization. In effect, pin dimensions, temperatures, and burnup were assumed to be the same as for the vented design, so that fuel costs would be similar. The length of plenum required to allow this was then computed. Penalties associated with the plenum appear as higher pressure drop and pumping power, and a longer assembly to handle and store.

The plenum was assumed to be located at the core inlet, 100% fission gas release was used, and the creep was integrated over time, using a linearly increasing pressure stress. Figure 21 compares the allowable

clad strain, the strain due to fuel swelling, and the strain due to gas pressure, as a function of axial position, for 10-mil clad. The assumptions about allowable clad strain and the fuel swelling are the same as were used for the vented-to-coolant analyses. A ratio of plenum to core length of over three is required to satisfy the limitation on clad strain. No additive effect is assumed between strain due to fuel swelling and strain due to fission gas.

The analysis was also performed for 15-mil clad and showed that a plenum-to-core length ratio of 1.75

FIG. 21. Design of Sealed Pin.

TABLE 4. SEALED VS. VENTED FUEL DESIGN

	Vented-pin Design (Reference)	Sealed-pin Design		
		10-mil Clad	15-mil Clad	20-mil Clad
Required plenum region length, Ratio $L_{\text{plenum}}/L_{\text{core}}$	0.7	3.25	1.75	~0.7
Friction pressure drop, psi	57	111	80	57
Differential costs due to incremental pressure drop, mill/kWh	0.0	+0.027	+0.0115	0.0
Differential costs due to thicker clad, mill/kWh	0.0	0.0	+0.065	+0.140
Net incremental costs, mill/kWh	0.0	+0.027	+0.0765	+0.14

would be required. The fuel-cycle cost penalty associated with the thicker clad is greater than the benefits of a shorter plenum, so that a minimal clad thickness of 10 mils appeared optimum for sealed as well as vented fuel. Table 4 summarizes the comparison. The cost penalty of the 10-mil-clad plenum design is small, but the effects on reactor vessel length, refueling cell height, and refueling mechanisms are not included.

5. Considerations of Core Structure

The choice of a nonfree-standing assembly channel wall, which will deform plastically under coolant pressure differentials and which depends on neighboring assemblies for support, was made because of cost incentives for minimizing the amount of structure in the core. The thickness of the channel wall is somewhat arbitrarily set at 60 mils to withstand general handling and provide some rigidity. A free-standing channel design, designed not to exceed the yield point, would be twice as thick, increasing the structure volume fraction by over 3%, and decreasing the fuel volume fraction by about 2%. The effect on fuel-cycle costs would be an increase of about 0.5 mill/kWh.

The development of a nonfree-standing design will not be easy, because of the radiation embrittlement of the channels, cycling of temperature and pressure conditions during operation, the need for clearance during refueling, and limitation of radial motions during operation. The conclusion is that other possible core designs involving minimization of the assembly structure should be investigated. The reduction, by some means, of the amount of structure associated with the assembly to the values used in this study is thought to be a reasonable goal.

A relatively recent development which will have important consequences for mechanical design of the core is the demonstration of swelling of structural materials under irradiation. This was not explicitly taken into account in the optimization work.

One aspect of structural swelling (and also of fuel swelling) is the effect on sodium flow and clad temperature. In this connection it can be observed that the power produced by the core section of an LMFBR fuel assembly decreases by about 10% over its lifetime. Thus, flow requirements also decrease, and substantial growth in pin diameter could be tolerated.

A more difficult problem may be to maintain good mechanical support of the fuel pins and fuel assemblies at the beginning of life without causing distortion and binding at the end of life. Grid spacers may be much more adaptable for this purpose than are spiral wire wraps. The effects on radial core clamping requirements and/or clearances between assemblies have not been analyzed. These areas should receive immediate attention, since development programs may be altered as a result.

Acknowledgments

Nuclear analyses used in the work were performed by C. L. Cowan. Mechanical design of the core and core support was contributed by J. H. Germer. C. E. Boardman and W. R. Gee contributed to the optimization of coolant temperatures.

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Discussion

See Discussion following paper by A. S. Gibson et al., THIS SESSION.

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Plant Design, System Optimization, and Equipment Selection for an Advanced LMFBR

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1. Introduction, Summary Description, and Capital Costs

1.1. Introduction

This paper presents the results of studies concerning system optimization, major equipment selection, and plant design for a 1000-MWe LMFBR. The work was performed for Argonne National Laboratory as a part of the 1000-MWe Follow-on Contract. The objectives of this particular work were as follows:

- to design conceptually the reactor, supporting systems, and plant in sufficient detail so that technical feasibility is established, capital costs are defined, and research and development may be planned;
- to optimize, with respect to cost, key plant and system design parameters.

The methods for implementing these objectives are described later. The result of this implementation, and of an evaluation of the state-of-art expected in the 1980s (assuming that suitable research and development is performed), is the definition of a 1000-MWe liquid metal fast breeder reactor plant and its principal characteristics.

1.2. Summary Description

Figure 1 shows a pictorial view of the reactor and refueling cell. As can be seen from this figure, the reactor and primary-system equipment are enclosed in a large vessel, and refueling is accomplished through an open plug into a hot cell. The three intermediate heat exchangers (IHX) and primary pumps are mounted on the upper shield deck. They are removable for maintenance. An IHX is shown in the removed condition. Normally, equipment removal is accomplished by utilizing a cask into which the equipment is lifted for transportation to a maintenance facility. The cask is not shown in the picture for purposes of clarity.

The primary sodium tank of stainless steel has an open top below the shield deck, with a series of horizontal plates with limited leakage seals at the penetrations through these plates. Argon gas flows inward

through these seals to prevent condensation of sodium vapor on the cold surfaces surrounding the tank. This open-top arrangement allows for thermal expansion of the primary-system vessel with regard to the fixed position of the reactor vessel, sodium pumps, and heat exchangers. Pipe expansion between the reactor vessel and the pumps is accommodated by sliding seals in the piping. Some sodium leakage at these seals is tolerated.

A secondary, or containment, vessel of carbon steel surrounds the primary system vessel and is cooled on the outside. The secondary vessel is insulated on the inside and is sealed at the shield deck.

A major determinant in the ultimate technical viability and economic success of the LMFBR is the manner in which the total plant complex is arranged. Proper consideration must be given to pipe expansion, equipment lay-down space, instrument racks, design of electrical equipment, equipment arrangement, and structural design. To this end, General Electric prepared preliminary studies of equipment and piping arrangement, and Bechtel Corporation performed in the final plant arrangement and design. Figure 2 shows a plan view of the plant, and Fig. 3 shows a cross section through the plant. A basic premise used in the arrangement was that all equipment for the primary sodium system would be located in internal low-leakage vaults, removed by at least one room or corridor from outside walls. The substructure is of reinforced concrete and steel, and the superstructure is structural steel and metal paneling (except for portions of the reactor building superstructure which is reinforced concrete).

Table 1 summarizes some of the major parameters or design features of the reference design. Also shown are some of the key problem areas identified during the design study. The problem areas are related to the need for particular development for the concept which is described.

Figure 4 is a heat balance for the plant showing the system temperatures, flows, and other conditions as they were finally selected for the reference cycle.

Figure 5 is a plan view of the primary-system ar-

FIG. 1. Sodium Breeder Reactor Plant.

rangement and the refueling cell. Three intermediate heat exchangers are located at one end of the primary vessel opposite from the reactor, which is offset in the primary vessel. Three pumps are located irregularly around the outside of the vessel, and only one corner of the hot cell protrudes over the primary vessel. Figure 6 shows a cross-sectional view of this same arrangement. In this view the arrangement of the hot cell is portrayed with the space shown for removal of the top shield plug of the reactor.

Figure 7 is a more detailed cross section of the primary system vessel and reactor. In this view it is seen that the primary coolant circulates from the primary vessel, into the pumps, through pipes to the lower plenum of the reactor vessel, through the core to the upper plenum and into three large pipes leading to the tube side of IHXs, and finally through the IHXs to the primary vessel. The IHXs and connect-

ing pipes are designed as units of low pressure drop (5 psi) to reduce the pressure differential between the reactor cover gas and the cover gas on the rest of the tank. In order to deactivate one loop, it is merely necessary to raise the cover-gas pressure in the IHX region so that the sodium level drops below the entrance shroud. The sodium in the inactivated exchanger will then drop about $\frac{2}{3}$ of the height in the exchanger (at full load) and flow will cease.

Figure 8 shows a schematic of the refueling steps. These steps are:

1. Place new fuel in thimbles in fuel-storage tank.
2. Remove shield plug.
3. Place new fuel and thimbles in temporary storage positions at periphery of reactor.
4. Remove used fuel from core and place in empty thimbles at periphery of reactor.
5. Remove new fuel from thimble in temporary

FIG. 2. Plan View.

FIG. 3. View through Cross Section.

storage position and place in core (keeping under sodium during transfer).

6. Transport thimble and used fuel (with sodium in thimble for cooling) to decay tank.
7. Replace shield plug.
8. Remove thimble and decayed fuel from storage tank to shipment-preparation vault.

1.3. Capital Costs

Based upon the conceptual plant design described above, an estimate of the capital cost of the reference plant was prepared. Table 2 shows a summary of these costs. Two fast reactor concepts are shown: a "First 1000-MWe LMFBR," and an "Early Commercial LMFBR." The only difference between the two is a 15-million dollar allowance for first-of-a-kind engineering, and an additional contingency allowance of \$9,400,000 which was included in the first plant but not the early commercial plant. Also included in both the early and first fast reactors is an allowance of \$12,000,000 for incomplete design. Thus, a total contingency of \$29,400,000 is included for the first fast reactor, plus \$15,000,000 for first-of-a-kind engineering; the early commercial fast reactor also includes a total contingency allowance of \$20,000,000. These are large contingencies when compared with current practice for water reactors. Further, more detailed design should result in a lowering of these contingencies, and it seems likely that this will result in a lowering of the cost. There is a finite probability, however, that the costs could also increase. The range of possible cost variance will be discussed in the forthcoming Task II Report.⁽¹⁾ The water-reactor costs are shown for comparison and are extracted from Public Utility Commission records with an adjustment for escalation and customer costs.

2. Method of Approach

The conceptual plant design described briefly above was conceived only after numerous optimization and economic tradeoff studies. Indeed, as noted in the introduction, the performance of such optimization studies was a major objective of the contract. To implement this objective, and the objective of establishing technical feasibility and capital costs, a method of approach was utilized which was intended to reduce the uncertainty in sensitive areas. Typical of the sensitive areas and of the causes of uncertainty often found in conceptual design studies are the following:

1. The mechanical design and arrangement of the reactor lacks sufficient detail to identify clearly all technical problems or economic incentives.

TABLE 1. PRINCIPAL FEATURES OF REFERENCE DESIGN FOR 1000-MWe LMFBR

Reactor and Refueling
Mixed-oxide vented fuel
Non-free-standing channel walls
"Pot" concept
Offset reactor in primary vessel
Primary and secondary vessel
Controlled cover gas
Hot-cell refueling
Key Problem Areas:
Local accident detection and prevention of propagation
Reliability of fuel and core structure
Pipe expansion in primary vessel
Reactor and primary pipe insulation
Design of cover-gas system
Equipment maintenance
Failure of hot-cell equipment
Sodium Systems
3-loop primary and secondary
Redundant electrical system and pony motors for emergency cooling
No large sodium valves
Constant-temperature inlet and outlet reactor sodium
Cold trapping only
Maximum temperature of 1150°F
Key Problem Areas:
Equipment reliability
Thermal transients
System control
Mass transfer
System contamination
Steam Systems
Once-through steam-generators with parallel reheaters and superheaters
Steam conditions of 2400 psig and 950/950°F
Tandem-compound 3600-rpm turbine
Key Problem Areas:
Reliability and maintainability of steam generator
Mass transfer
Buildings
Metal panel construction
Rectangular arrangement
Modest leakage requirement
Controlled effluent
Primary systems contained
Key Problem Area:
System control of environment

2. Major sodium equipment, such as heat exchangers and pumps, are at such an early state of development that generated cost information is not meaningful.

3. Major system and equipment evaluation requires so many economic tradeoffs of subsystems, for each parameter treated, that meaningful optimization studies are usually not practical.

4. Estimates of plant capital cost are extremely suspect because of the lack of a commercial business to provide feedback price information to compare with cost estimates.

FIG. 4. Heat Balance.

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Fig. 5. Plan View of 3-loop System.

Fig. 6. Cross-sectional View of 3-loop System.

FIG. 7. Cross-sectional View of Primary Vessel and Reactor.

FIG. 8. Refueling Schematic.

For the present effort, in the area of mechanical design numerous arrangement studies were made and sufficient design was performed (within the limits of contractual constraints) to identify advantages and disadvantages of the different schemes. Where an attractive concept was identified, additional design study was performed to a level of detail sufficient to outline major problems or development areas. Where possible, tentative solutions to the problem areas were identified.

For major equipment design and costing, manufacturers of this equipment were consulted, and methods were developed to allow the conceptual design and costing of the equipment to proceed. In the case of the heat exchangers, this involved a consideration of the various steps needed to manufacture the exchangers. Cost coefficients were derived for each manufacturing step. These coefficients were varied over a wide range from the expected value to determine the overall cost sensitivity to such changes.

TABLE 2. SUMMARY OF POWER PLANT CAPITAL COST FOR 1000-MWe REACTOR
(Costs are in 10⁶ 1968 Dollars)

	First 1000-MWe LMFBR	Early Commercial LMFBR	1000-MWe Water Reactor ^(c)
21 Structures and Improvements	14,624	14,624	
22 Reactor Plant Equipment ^(a)	76,054	76,054	
221 Reactor equipment	10,906		
222 Heat-transfer systems	39,934		
223 Nuclear fuel handling	3,399		
225 Radioactive waste	673		
226 Instrument and control	13,312		
227 Feedwater supply and treatment	3,010		
228 Steam, condenser, and feedwater piping	4,820		
23 Turbine-generator Plant Equipment	24,618	24,618	
24 Accessory Electrical Equipment	3,025	3,025	
25 Miscellaneous Power-plant Equipment	1,055	1,055	
First-of-a-kind engineering	15,000		
Allowance for incomplete design	12,000	12,000	
Subtotal; Direct Construction Costs	146,376	131,376	
Field distributables	13,441	13,441	
Engineer-constructor; Engineering and inspection fees	10,300	10,300	
Contingency; General uncertainty	17,400	8,000	
Subtotal: Constructions Costs	187,517	163,117	139,500
Customer costs (Land, interest during construction, G&A, Ad-valorem taxes) 20%	37,503	32,623	27,900
Sodium inventory	925	925	
Total Plant ^(b)	225,945	196,665	167,400
Net output, MWe	1,010	1,010	1,060
Total Plant, \$/kWe, net	224	195	158

^(a) Includes Reactor Manufacturer Engineering, G&A, Fees^(b) Excludes Step-up Transformer, Transmission, and Substation.^(c) Based on Public Utility Commission records with adjustments for escalation and customer costs.

System design and optimization required special techniques to accommodate the numerous economic tradeoffs and system design parameters. The problem is illustrated in Fig. 9A, which shows a typical flow schematic for a LMFBR power plant. The schematic shown is for a reheat cycle with parallel reheaters and superheaters, and a once-through steam cycle. Numerous variations of this cycle are possible, and each requires an independent evaluation. Figures 9B and 9C show, respectively, the temperature profile in the heat exchanger and the trends of power cost as a function of throttle and reheat temperature. From the latter curve, it is possible to see the type of economic trade-off required for an evaluation of each cycle. For example, if for one possible parametric study core outlet temperature is held constant at 1150°F, then for increasing throttle and reheat temperatures the heat-exchanger costs increase sharply as the temperature approaches 1100°F, due to the decreasing LMTD. However, as throttle and

reheat temperatures increase, the cycle efficiency increases, and for a given core size the plant can produce more power. This reduces both capital and fuel-cycle costs. The total power cost is the sum of these competing effects and is seen to pass through a minimum at about 970°F.

To perform the computations required for system analyses of the type indicated, a digital-computer code was used which performs all of the computations and optimizations required for complete system evaluation for numerous cycle arrangements. Additionally, it defines the details of major system equipment (such as number of tubes in heat exchanger, size of tubes, thickness and length of tubes, and shell diameter) based upon the optimized system parameters. Heat-transfer coefficients are calculated as a function of the system conditions and of the type of heat transfer (e.g., film boiling and nucleate boiling).

In the area of capital-cost generation, the method

FIG. 9A. Flow Schematic for LMFBR Power Plant.

FIG. 9B. Temperature Profile in Heat Exchanger.

FIG. 9C. Trends of Power Cost.

of approach was to identify a series of steps that would enhance the reliability of the resulting cost estimate. These steps were:

1. Engineer the equipment and systems in sufficient detail to permit costing individual items.
2. Utilize previous experience on fossil and water-reactor plants in systems arrangement and design.

3. Provide "three-point" cost estimate of the clear steam-supply system.
4. Compare individual cost accounts with published information on water reactors.
5. Provide contingency to allow for incompleteness of design and for general uncertainty.
6. Obtain "Customer Costs" from utilities and Bechtel Corporation.
7. Determine the indirect costs, such as field distributables and engineering, from previous water-reactor experience.
8. Show costs for a "First-of-a-kind" LMFBR and for a "Commercial LMFBR."
9. Compare with the total costs of a 1968 water reactor.

The material that was presented in the summary and that which follows are the results obtained by applying these various design, optimization, and cost-estimating methods to the system and plant areas of the 1000-MWe LMFBR. Only a few of the optimization and tradeoff studies are presented.

3. Reactor Equipment and Plant Design Studies

In the arrangement of the primary system, a major consideration with regard to cost is the number and location of heat exchangers and pumps within the primary vessel. The location of the reactor vessel is also an important factor, as is the method of sealing the top of the primary vessel. Figure 10 shows one arrangement that was considered in the present study. In this arrangement the reactor vessel is located within the center of the primary vessel; six intermediate heat exchangers are located around the periphery of the primary vessel and below the refueling cell; the three primary pumps are located at one side of the primary vessel outside of the refueling cell. Some of the advantages and disadvantages of this arrangement are as follows:

Advantages:

1. easy access to the reactor and the IHX units from the hot-cell crane;
2. fairly symmetrical piping or coolant flow paths;
3. structural support for the shield deck from the hot-cell wall which spans the deck;
4. hot-cell walls provide necessary shielding around IHX units;
5. IHX removal cask for maintenance is unnecessary;
6. the loss of any one IHX only reduces power output by one-sixth if the remaining loops can be used.

Disadvantages:

1. very high hot cell required in order to allow for IHX removal;

FIG. 10. Six-loop System.

2. the six secondary loops result in high capital costs compared with fewer loops;
3. the IHXs are close to the reactor vessel and require more shielding to minimize secondary

sodium activity than if they could be moved farther away.

Numerous alternative arrangements to that shown in Fig. 10 were considered, including six pumps and

six exchangers, location of pumps inside of the hot cell, and irregular shaped primary vessel. All of these arrangements proved to be less attractive than that in Fig. 10, and this was the one selected for the Task-I design work. However, the disadvantages mentioned above caused further studies to be made. Although not immediately apparent, the first two disadvantages mentioned prove to impose a serious cost penalty compared with the arrangement finally selected as the reference. That reference has already been illustrated in Figs. 5, 6, and 7. Although the reference arrangement results in nonsymmetrical piping and coolant flow, and it requires shielding of the IHXs above the shield deck and a removal cask for the IHXs, the advantages far outweigh these disadvantages. The advantages are:

1. much smaller hot cell, both in height and width;
2. reduced capital cost for the three secondary loops;
3. optimum shielding arrangement for reducing secondary sodium activity in the IHXs.

Many other tradeoff studies of the reactor arrangement were involved in the final arrangement shown in Fig. 5, 6, and 7. For example, the method of insulating the hot sodium leaving the reactor from the cooler sodium in the primary vessel required an analysis of techniques that could minimize thermal stresses across structural components. Other alternatives to the refueling scheme shown in Fig. 8 were also considered, including storage of used fuel within the primary sodium vessel. The reference method was selected because of its basic simplicity, lack of complex mechanisms, and minimum interference with reactor operation. It is noted that the selection of a reactor concept with no inner vessel head simplifies fuel handling. The shield plug in the raised position

contains the disconnected control-rod drives and the instrument probes (which have fixed positions). Means are provided in the transport of used fuel to the hot-cell storage positions to assure continuity of cooling under all conditions.

It was previously mentioned that the six-loop (six IHXs), six-primary pump arrangement was more costly than the three-loop, three-pump arrangement. Figure 11 shows a summary of the relative costs of a 6-loop, 6-primary pump and 6-loop, 3-primary pump plant compared with a 3-loop, 3-pump plant. All capital costs for each arrangement have been determined and summed. Additionally, estimates of the plant unavailability for the different loop combinations have been made, and the cost of buying makeup power over the life of the plant has been summed and discounted to present value. The resulting "composite costs" shown in Fig. 11 are thus the sum of the capital and discounted purchased power costs (at different discount rates) for the 3-loop, 3-pump plant subtracted from the same costs for the 6-loop, 6-pump plant or the 6-loop, 3-pump plant. By far the greatest effect is that of the capital cost differences between the systems (from 8 to 12 million dollars). It should be noted that the cost of the 6-loop, 3-pump plant increases with increasing purchased power cost because it requires greater makeup power quantities than does the 3-loop, 3-pump case. This is in part due to a simplification in the assumptions which required that no credit be taken for loops operating at less than 100% of their rated capability. If partial loop-loading capability had been allowed, the slope of this line would have changed, but the conclusions would not have been affected for any reasonable purchased power cost. A basic conclusion of the study was that if large reliable components can be developed, the fewer the number of loops, the lower will be the cost of the system. For the 1980 period in question, 3 loops were selected as being a reasonable compromise between development problems (and costs) and power cost.

FIG. 11. Composite Cost Comparison of Different Loop Arrangements.

4. System Optimization and Equipment Selection

Many different studies of system optimization were performed; these included:

1. optimization of throttle temperature for alternative throttle pressures, alternative core outlet temperatures, and alternative core cost of heat;
2. optimization of throttle temperature as a function of different heat-exchanger costs.
3. selection of a reference cycle arrangement from:
 - a. parallel reheater and superheater arrangement with the evaporator in series with the superheater only;

- b. parallel reheater and superheater-evaporator arrangement with the evaporator in series with the superheater only;
- c. series reheater-superheater-evaporator arrangement;
- d. recirculating boiler vs. once-through;
4. comparison of reheat and nonreheat steam cycle;
5. optimization of core ΔT as a function of fuel pin-to-diameter ratio (P/D), and maximum cladding temperature;
6. optimization of maximum cladding temperature as a function of cost of core heat.
7. optimization of design characteristics of heat exchanger;
8. optimization of pumps and drivers.

Only a few of these studies will be reviewed below.

Figure 12 shows the results of an analysis of the relative cost of energy as a function of throttle pressure and throttle temperature (main and reheat). Core outlet temperature and cost of heat are kept constant. As can be seen, for these conditions optimum throttle and reheat temperature occurs at 960°F.

Figure 13 shows the relative cost of energy vs. main and reheat throttle temperature for different assumed values of cost of core heat, and alternative core coolant outlet temperatures. From the figure it is apparent that the predominant effect on energy cost is that of cost of core heat. The range selected for the assumed cost of heat variation (3¢/M BTU to 12¢/M BTU) corresponds approximately to the finally computed range for the cost of core heat in the core-design studies. This is discussed in more detail by Sherer.⁽²⁾

The data from Fig. 13 have been cross-plotted in Fig. 14 to show the optimum main throttle and reheat temperatures as a function of the cost of core heat and core coolant outlet temperatures. For a given core outlet temperature, the optimum main throttle and reheat temperatures are relatively insensitive to cost of heat except at high outlet temperatures.

Numerous evaluations were made of the type of cycle and the steam-generator arrangement which should be used for the reference cycle. Figure 15 shows a steam-generator arrangement in which the superheater and evaporator units are in series, and both of these units are in parallel with the reheater. A once-through steam generator is used. Figure 16 shows an arrangement in which the superheater and reheater are in parallel, and both of these units are in series with the evaporator. The third arrangement considered was one in which the reheater, superheater, and evaporator were in series.

FIG. 12. Energy Cost (mills/kWh) vs Throttle Temperature (°F) (for Throttle Pressures of 2400, 1800, and 1450 psi with Reheat).

FIG. 13. Energy Cost vs Throttle Temperature for Variable Cost of Heat and Core Outlet Temperature.

FIG. 14. Optimum Throttle Temperature vs Fuel Cost and Core Outlet Temperature.

FIG. 15. Parallel, Once-through with Superheater and Evaporator in Same Shell.

FIG. 16. Parallel, Once-through with Superheater and Evaporator in Separate Shells.

Figures 17A and B show the temperature profiles for the two parallel arrangements mentioned above. The principal difference between the two cycles is seen to occur in the reheater. In the case in which the reheater is in parallel with both the superheater and evaporator, the secondary sodium outlet temperature from the reheater is much lower (640°F vs. 940°F).

This results in a much lower LMTD in the reheater unit (which has a lower overall heat-transfer coefficient than either the superheater or evaporator units) and therefore requires more total area.

Figure 18 shows the differential energy costs as a function of throttle temperature (main and reheat) for a recirculating boiler with a parallel reheater-

FIG. 17A. Heat-exchanger Temperature Profile for Parallel Reheat Superheat-Evaporator with Superheater and Evaporator in Separate Shells.

FIG. 17B. Heat-exchanger Temperature Profile for Parallel Reheat Superheat-Evaporator with Superheater and Evaporator in Same Shell.

superheater arrangement, a once-through parallel unit, and a once-through series arrangement. It is seen that the once-through units enjoy a considerable cost advantage over the recirculating boiler. This is primarily due to the additional steam drum and recirculating equipment on the recirculating boiler, and to a somewhat larger superheater in the optimized recirculating unit compared with the once-through design. The once-through series arrangement has comparable costs with the once-through parallel unit. The once-through series design utilizes ferritic steel in the superheater. Unfortunately, the feasibility of using ferritic steels at the high sodium inlet temperature to the superheater (1015°F) is questionable.

Figure 19 illustrates the differential energy costs between reheat and nonreheat steam cycles for alternative conditions of steam pressure, steam temperature, and core outlet temperature. Reheat

enjoys a cost advantage of 0.05 mill/kWh over non-reheat, even at the low assumed fuel cost of 3.4¢/M BTU. The lower cost of the reheat cycle results from the lower cost of the reheat turbine and the more efficient cycle. Although the nonreheat cycle saves on piping costs, this effect is not as great as might be expected due to an increased throttle flow and higher throttle temperature for the optimized nonreheat cycle. Moreover, the total surface area of all steam-generator equipment is not significantly different for the optimized reheat and nonreheat cycles.

For the reasons discussed above, the reference cycle utilizes reheat with steam conditions of 2400 psia and 950°F/950°F, and a once-through parallel steam-generator arrangement with the evaporator and superheater in separate shells.

Another series of interesting optimization studies involved the maximum temperature of the fuel clad-

FIG. 18. Energy Cost vs Steam-generator Arrangement (Reheat at 2400-psia Cycle).

FIG. 19. Differential Energy Cost vs Throttle Temperature and Pressure for Reheat and Nonreheat Steam Cycles.

FIG. 20. Energy Cost vs Maximum Cladding Temperature for Conservative and Optimistic Heat-cost Basis.

FIG. 21. Curves of Core Heat Cost.

ding and its effect on system design. In these studies, different parameters were varied as a function of maximum fuel-cladding temperature. Figure 20 is illustrative of one of these optimization studies. It shows differential energy cost as a function of maximum fuel-cladding temperature for alternative assumptions of cost of core heat. These assumptions for core heat are shown in Fig. 21. The "conservative" cost basis is greater by almost a factor of two than the "optimistic" cost basis. These alternative assumptions on the cost of heat and the derivation of them are described in more detail by Sherer.⁽²⁾ Both cost bases show a strong trend towards increasing cost with increasing cladding temperature. Referring again to Fig. 20, it is seen that when the cost of core heat is coupled with the total plant costs, the net effect of their interrelationship is to produce an optimum cost at a particular cladding temperature. The optimistic cost basis optimizes at a higher

cladding temperature than does the conservative cost basis. As a point of interest, the effect of constant heat costs are also included. These costs are seen to continue to decrease with increasing cladding temperature throughout the range studied. This is because of the continuing improvement in plant efficiency with higher cladding temperature (and also sodium coolant and steam temperature) without an offsetting penalty on the fuel costs.

Other optimizations were performed to determine the optimum core coolant ΔT as a function of maximum cladding temperature at different fuel pin pitch-to-diameter ratios. These studies led to the selection of a core coolant ΔT of 340°F with a fuel-pin pitch-to-diameter ratio of 1.2, and a mixed mean coolant outlet temperature of 1150°F (corresponding to a maximum cladding temperature of 1340°F and a design point maximum cladding temperature of 1225°F).

Many other plant and system optimizations were performed.

Acknowledgment

P. M. Tschamper performed the work associated with defining the optimum number of loops, J. A. Forrest performed some of the work on optimization of steam generators, and W. F. Garetson generated much of the information about capital cost.

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Discussion

Mr. Smidt (Karlsruhe): One of your designs is quite similar to the designs followed in Europe; while we are glad that you called these designs advanced, we are deeply convinced that this is the only sensible way to proceed. Now these designs certainly rely strongly on instrumentation, and I wonder if you could elaborate a little on core instrumentation. You mentioned thermocouples and boiling-noise detectors, but certainly there may be more instrumentation for the coolant channels of the subassemblies. Noise detection is rather late; the system might be at rather high temperatures for some time, and some damage may occur before detection of boiling noise. Also, thermocouples are not very sensitive for certain types of partial flow blockage, for instance in the sub-

assembly, so I wonder if you have more to say on the subject of in-core instruments.

Mr. Sherer: Generally we agree with you; we need the instrumentation, and we have been assuming that instrumentation would be placed on every sub-assembly. It's our belief that the instrumentation would be sufficient if we had a highly reliable means of measuring flow and a highly reliable means of measuring outlet temperature for every subassembly. Additional instrumentation, such as boiling detectors, that is, thermal-noise detectors would, of course, be desirable, but we believe that they're not absolutely essential to the success of the concept. I think we're in agreement that for certain types of accidents flow measurement is superior to a thermocouple measurement of temperature, but we did not go into the details of exactly how much redundancy is required. This has to be integrated with an overall study of just exactly how large a signal is acceptable for scram, or to what extent, and to what accidents, are thermocouples partially redundant with the flow measurement. We were simply not able to go into that. In addition we don't, for example, have any good numbers for reliability of any particular scheme of flow measurement, which, of course, is essential to deciding how much redundancy is needed.

Mr. Cobb (Nuclear Power Group, UK): I have a question for Mr. Gibson. You cited as one of the advantages of your refueling cell that it acts as a sort of secondary containment over the reactor portion. Can you say how it is assured that any breach of the top cover of the reactor vessel will be into that cell rather than into the reactor compartment? What sort of pressures do you design that cell to withstand in the event of any accident condition?

Mr. Gibson: We depend upon the safety philosophy that Mr. Murphy described to insure that accidents leading to the breaching of the upper cover will not occur. We don't anticipate any significant pressures under the plug. In the conservative design which we also studied, we did anticipate such pressures. For that case, we designed the cell for a pressure of 5 psi and designed the upper plug to withstand an unknown pressure under it. By design, I mean we conceptually placed bolts around the periphery of the plug, which we believed could be designed to withstand a modest pressure. Additionally, we put blast shields and meltdown trays into the vessel itself. We believe, however, that the difficulty associated with determining the so-called design-basis accident precludes an accurate determination of design values unless you follow the approach that suggests that engineered safeguards and instrumentation do work

and you can assign a probability value to the accident. For that reason, for the advanced plant we did not design the upper cell for an accident penetration. We did design it to accommodate normal leakage through the seals, and we have an argon atmosphere in the cell. The same is true for the penetrations around other equipment.

Mr. Cobb: You have to take leakage from under the plug into the cell, and you have forced flow through your intermediate heat exchangers, or so I understand it. Could you say what the pressure level is under the plug for normal operating conditions?

Mr. Gibson: It is 5 psig under the plug. There's a 2-psi drop between the plug and the heat exchanger, and a 3-psi drop through the heat exchanger. So there's a differential pressure in the cover-gas region of 3 psi over the top of the IHX and 5 over the top of the reactor.

Mr. Trinko (ANL): I noticed that fuel-cycle costs were lower for lower theoretical densities; I find this surprising. I wonder if you could comment on this result.

Mr. Gibson: I think the chief effect is the fabrication and reprocessing costs. I noticed in other papers that the fabrication and reprocessing costs were, I think, closer to our alternative or more conservative cost basis than they were to our optimistic one. I think that's the major reason for the difference. I don't remember well enough to be able to say whether our final fuel densities are particularly different from those of others. The cost incentive for reduced fuel density was due partially to the effect of an assumed larger central void, allowing higher specific power, and partially to the assumed effect of fuel density upon swelling, which allows slightly higher burnups, other conditions being equal.

Mr. Golan (AI): I am very much interested in the low fabrication costs that you have projected, and I wonder if you could comment as to the basis for these costs. What assumptions have you made with respect to production capacity; do you assume improvements in fabrication techniques, etc.?

Mr. Gibson: The basis is a fuel-fabrication model based upon the arrangement of a fabrication line that is designed with facilities for a large throughput, of the order of 10,000-MWe equivalent capacity. It's anticipated that a rather large and viable business has to be established in order for the very optimistic cost to be realized. In the earlier business period, we would anticipate that the cost would be higher. Our low costs were shown to be of the order of 0.4 mill, I think, and that cost is expected to be attained if a fast reactor economy develops that's very large

and supports such facilities, and would, therefore, average out over the life of the reactor system. In the earlier period we would anticipate an additional couple of tenths of a mill that would be adequate to cover our fabrication costs.

Mr. Noyes (CE): You indicated a fuel-subassembly design that, as I understand it, has a box wall thickness of 60 mils, which is apparently indicated as workable for pressure drops up to about 200 psi. How do you achieve that sort of trick mechanically?

Mr. Sherer: That design will, of course, deform plastically and will have to be supported by adjacent assemblies. We did some analysis of this design; we believe that it is feasible, but it will require a lot more work. It will depend, as does the cladding design, upon the allowable strain that can be endured in the corners of the hex; also it will require more analysis of strain cycling, which will depend upon operating temperatures and pressure cycles during operation. I would say, in general, that there obviously is a strong incentive to reduce the amount of structure in the core and to reduce the amount of structure in the hexagonal subassemblies. It's our belief that for a long-term goal an amount of structure equivalent to what we assumed can probably be achieved, if not by this type of design, then by some other.

Mr. Menzel (CE): In connection with the assembly wall thickness of approximately 60 mils, you mentioned that the thickness was one of the fixed parameters in core optimization; on the other hand, you had a fairly wide variation of pressure drop. Does this mean that for some core designs the stress on the shroud wall is less than for others? Essentially, are some more over-designed than others?

Mr. Sherer: The 60-mil thickness is an approximate number, and we used some variations—not enough, however, to have a very significant effect on the core structure content over that range of pressure drops. But we found in the analysis, as far as we took it, that if you allow the channel to deform and be supported by its neighbors, assuming that kind of a design with a strain limit in the corners, wall thickness appeared to be less dependent on pressure drop than in the case of a free-standing channel wall. That was the reason we did not go further into the variations of wall thickness.

Mr. Gast (Karlsruhe): I had some difficulties in following those slides of the presentation by Mr. Murphy which apparently deal with propagation of fuel-channel blockage and failure. I wonder if you could say a few words about this.

Mr. Murphy: We looked first at blockages of a single channel, originating either in the inlet nozzle or upstream of the nozzle, to determine what the probabilities or the possibilities were of escalating into a larger event. We found these were detectable, and, in short, believed that they could be prevented. The big difficulty was found for events originating within the fuel channel itself, particularly failures of a single pin, and the accumulation of debris from reduction of flow, and the fact that this could then propagate through the whole channel. This study showed that such propagation could proceed through the channel without giving, from a safety standpoint, any firm signal until a significant number of the pins had been involved. If you assume that the incident started at the center pin in a hex of 271 pins, we found that approximately the sixth hex row could be involved before there is a reliable indication of what is happening from any of the signals—reactivity, flow, or temperature; flow would be the first to give you a signal. This, then, led to the consideration of what happens after this event had propagated through the channel and onto the channel wall. We concluded that it would take about 5 sec to go from the center pin to the assembly wall. Most of that time you would not have obtained a reliable signal. Then, further, the event would proceed through the channel wall, taking about 10–20 sec, and our conclusion was that, for the advanced design, a possible design-basis accident would be an event starting in a channel with a pin failure that would not be detected until it had propagated into the adjacent channel. You have to assume that it would propagate into all of the seven adjacent channels if it proceeded symmetrically radially outward, so the design-basis accident would be the voiding of seven channels within the core. We figure that by the time it would propagate into the adjacent channels, you would have very lively signals from both flow and reactivity, and the control system would take over then.

Mr. Campise (ANL): There's a great interest in operating with failed elements in advanced systems. In your design, you have a vented fuel element. What kind of sensitivity do you believe you will have in your fission product-detection system to detect the onset of failure in your reactor?

Mr. Murphy: I think we would not consider this a reliable scram-indicating mechanism, both because it's too slow and because of the background noise from the products of the venting itself.

Mr. Campise: Are you saying that you would not rely on fission product detection to signal the onset of fuel failure, but would rely on temperature or reactivity or some other means?

Mr. Murphy: We would probably rely on flow; we would not rely on fission products.

Mr. Campise: Well, this would also imply that you're willing to take a considerable amount of damage in a subassembly before you would be notified of failure in your core. Usually fission product detection in a nonvented system is a very sensitive means of telling that you have released fission products to the system. Are you implying that you would take the loss of one subassembly in your design?

Mr. Murphy: Probably, yes.

Mr. Gibson: I would like to comment further on this point. Most of the work that we have addressed ourselves to so far was related to safety. The question of operability and the downtime associated with maintenance considerations is another issue. It's also related to types of instrumentation required. Our comments to this point have been addressed to what would be required in order to license a reactor and get it operating safely, assuming that the necessary R&D is accomplished, and is successful in achieving its goal. Obviously, there are other detection devices, including fission products and delayed neutrons, that can be used in the gross sense to determine operational problems in the reactor. That's another issue.

Mr. Charak (ANL): How sensitive is the viability of the advanced concept to your assumption that expulsion of molten fuel into sodium has either a low probability of occurrence or is incredible; phrasing it another way, what is the likelihood of developing instrumentation to shut down the reactor in the event of such a situation?

Mr. Murphy: The written version of the paper goes into this in greater detail, but in summary it says that we are quite sensitive to this assumption and a great deal more work has to be done in this area. There's work going on at Argonne and in Britain along these lines. This is indeed a critical area to both the advanced and the conservative design, for that matter.

Mr. Zebroski (GE): Not a question, but a very brief comment to Mr. Campise's question. The vented fuel gives no delayed-neutron signal, so it (the vented fuel) doesn't affect the sensitivity of the delayed-neutron system at all.

Mr. Lagowski (APDA): I wonder if Mr. Gibson could comment briefly on what happens to the fuel after it's been removed from the reactor. What processing does it receive; how is it shipped; what assumptions, if any, have you made on shipping costs?

Mr. Gibson: I'll make it very brief. We didn't study it.

Mr. Wirtz (Karlsruhe): Could you say a few words about how your vented fuel top is constructed?

Mr. Gibson: I think Dr. Zebroski has talked about the various types of vents we studied. The particular vent type in this design is a simple diving bell with a pinched top and a little solder seal on the pinched top which dissolves in the sodium. This allows the venting process to proceed with a very low holdup time. We took the penalties associated with the introduction of the fission products into the system in much the same way that it was described in the previous paper in this conference.

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The Westinghouse 1000-MWe Follow-on Study

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INTRODUCTION

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The Westinghouse Electric Corporation's plans for development of economic, safe, and reliable LMFBR Nuclear Power Plants have always included conceptual design of the objective large plant, or "target" plant, as an essential step in program definition. Therefore, we welcomed the opportunity to participate in the 1000-MWe Follow-on Studies, managed for the AEC by Argonne National Laboratory, which in large measure fulfilled that need. Concurrently with this study, we carried on a parallel conceptual design of a 200- to 400-MWe demonstration plant, the features of which were selected so that its successful operation would provide a basis for extrapolation to the target plant. In this manner, the Follow-on Study contributed to our demonstration-plant design. Conversely, a number of systems were defined in greater detail in the demonstration-plant study than was possible within the funding limitations of the Follow-on Study, and these designs were adapted to the 1000-MWe reference design. Thus both efforts were strengthened.

Westinghouse elected to perform a number of major-concept trade-off studies in Task I prior to initiation of the complete conceptual design of the selected concept and still unresolved alternatives in Task II. The results of these studies were reported by C. A. Anderson, Jr., at the April 1968 ANS Topical Meeting on Fast Reactor Systems, Materials and Components.⁽¹⁾ The trade-off studies completed in Task I were:

- a. four-module versus seven-module versus pancake cores;
- b. vented versus sealed fuel rods;
- c. hot-cell versus under-the-plug refueling;
- d. hot-cell containment versus pressure-shell containment;
- e. two versus three coolant loops;
- f. piped primary coolant system versus integral (also called Combined Unit Reactor Vessel) primary coolant system;

g. sodium-reheat steam cycles versus steam reheat or moisture-separation steam cycles.

In the above studies, we endeavored to develop alternative designs with equivalent safety, reliability, and technical feasibility so that selection could be made on the basis of minimum power cost. In some cases, the comparisons were clear cut. For example, the economic comparison of sodium-reheat steam cycles with moisture separation or steam-reheat cycles showed the former to be more costly for a wide range of fuel costs, capital costs, and plant-cost scaling factors. The designs and costs involved in the comparison are reasonably well-known, the two systems being compared are very much alike, and the uncertainty in cost comparison is small.

On the other extreme, the economic comparison of piped and integral primary coolant systems has a large uncertainty associated with it. The design of each system lacks the test of time, there are large uncertainties in estimates of costs of components, and the systems being compared are dissimilar. Despite the difficulty of clear establishment of the superiority of one concept in such a situation, decisions are necessary in order to proceed, and such decisions can be made with some confidence that, if the wrong concept were chosen, the economic penalty for the error is not great.

Westinghouse elected to study a carbide-fueled target plant, the choice being based upon the ultimate economic potential of carbide, which had been developed in previous work. We recognize that the state of technology will dictate the use of oxide fuel in early LMFBR plants. We believe it necessary, however, to define the carbide-fueled system to provide a target for development which has the higher potential. This selection, plus the results of the trade-off studies enumerated above, resulted in the selection of a carbide-fueled, four-module reactor utilizing vented fuel rods. The primary system is piped with two coolant loops, and utilizes a hot-cell refueling and maintenance system, with the hot-cell structure providing containment. The energy-conversion system utilizes a moisture-separation cycle, as opposed to sodium reheat.

Task II consisted of developing a conceptual design based upon these concepts. A major alternative, also carried through Task II and the balance of the study, was a single cylindrical core, optimized for low power cost but not meeting a criterion that the sodium-void coefficient be zero or negative. This comparison results in one of the more significant conclusions of the study.

Figure 1 illustrates the two cores, which have the same fuel-assembly design and are compatible with the same basic plant system. Note that the economic penalty of the spoiled geometry is approximately 0.25 mill/kWh, an enormous incentive for research and development in this area.

In the following sections, members of the Advanced Reactors Division staff will cover fuels and materials selections, and the reactor and plant design, and optimization studies of Task III. In the Summary, preliminary results of the Task III parametric studies and Task IV, Research and Development Program Planning, will be discussed.

FUELS AND MATERIALS

A. BIANCHERIA

Manager, Fuel Irradiation

1. Fuel Selection

Sodium-bonded (U,Pu)C fuel was selected as the reference fuel-rod concept. Helium-bonded mixed oxide was selected as an alternative concept. The general criteria employed for the selection of reference and alternate materials were:

- sufficiently characterized properties to provide a reasonable basis for estimating the required design information;
- projected ability to meet the performance requirements of a large LMFBR;
- good development potential which could be realized in time for design of a 1980 plant.

Carbides and oxides were considered to meet these criteria, although to varying degrees. Although oxide has been studied more extensively, carbide has greater development potential and was selected as the reference fuel primarily for this reason. Only the reference concept will be discussed in this section.

A sodium-bonded fuel was chosen to provide internal voidage for swelling accommodation, thereby preventing cladding strain from this source, and to provide high heat transfer between fuel and cladding.

Because of the anticipated low ductility of available cladding materials at high neutron exposures and the moderately high swelling rates of the fuel, it was necessary to minimize or eliminate fuel-cladding

FIG. 1. Comparison of Reference Reactor Cores.

mechanical interaction to achieve the desired high burnups (peaks of $\sim 150,000$ MWD/T).

Other advantages also accrue from use of a sodium bond. For example, the bond gap can be varied over a large range without significantly affecting fuel temperatures and swelling rates; this capability permits the designer to select the conservative portion of the usual wide scatter in fuel-swelling data without significant penalty. Also, the lower fuel-operating temperatures in sodium-bonded elements (while still maintaining high linear powers) provide a greater safety margin to melting. The satisfactory in-reactor performance of the sodium bond must be verified, however. The behavior of fission-gas bubbles in the bond and the potential for loss of bond from a defective rod must be investigated. Use of vented fuel rods (the reference design) is expected to resolve the latter problem.

2. Fuel Composition

In-pile fuel-sodium-cladding compatibility must also be verified. Hypostoichiometric carbides contain

(U,Pu) metal as a second phase which may form eutectics of low melting point with iron- or nickel-base cladding alloys. Hyperstoichiometric carbides contain $(\text{U,Pu})_2\text{C}_3$ as a second phase from which carbon may be transferred by sodium to the cladding. Evidence has been presented⁽¹⁾ that indicates that hypostoichiometric and hyperstoichiometric UC fuel may react with cladding. Hyperstoichiometric UC contains dicarbide, which is less stable than the sesquicarbide, $(\text{U,Pu})_2\text{C}_3$, present in mixed carbide fuel. The sesquicarbide, however, is still thermodynamically unstable with respect to stainless steels. Recent out-of-pile results verify that 100% $(\text{U,Pu})_2\text{C}_3$ fuel in sodium at 800°C will carburize Type 304 stainless steel.⁽¹⁾

Clearly, the ideal composition for compatibility is single-phase stoichiometric fuel. However, the very narrow range of homogeneity of single-phase (U,Pu)C may make it impractical to manufacture the fuel on a large scale. Further, the burnup of (U,Pu) atoms in-pile tends to shift the fuel towards the hyperstoichiometric structure and thus may introduce a problem involving high-burnup compatibility. Therefore, a modified fuel was proposed to buffer the carbon activity against irradiation-induced changes and minimize the formation of potentially deleterious second phases. Two thermodynamically suitable modification systems were previously examined which employed chromium-based and iron-based additions.⁽²⁾ Modifications based on other materials, such as vanadium, also may be feasible, but were not examined in detail. The chromium-based modification, which yields fuel containing stoichiometric (U,Pu)C with chromium and Cr_{23}C_6 as grain-boundary second phases, was selected as the reference system. The composition of the reference fuel with 10% excess additive is

U + Pu, 92.0–94.2 wt % (46.9–48.9 atom %);

C, 4.65–4.75 wt % (47.0–49.0 atom %);

Cr, 1.0–3.1 wt % (2.1–4.1 atom %).

Out-of-pile compatibility tests in sodium employing hypostoichiometric UC and Cr_{23}C_6 - and iron-modified UC have indicated that the modified fuels operate as proposed and are stable in sodium.⁽²⁾ Additional evidence of the stability in sodium of these modifications employing (U,Pu)C also has been presented.⁽³⁾ In these latter tests, modified (U,Pu)C fuels in sodium at 800°C showed no metallographic evidence of carburization of Type 304 stainless steel after approximately 1400 hr of testing.

Although modified (U,Pu)C was selected for the reference material, other (U,Pu)C compositions may

also be satisfactory. Modified fuel was chosen as compatibility conservatism because it was uncertain whether small amounts of thermodynamically unstable second phases would have a significant effect. There is evidence that suggests they may not. Hypostoichiometric (U,Pu)C containing a fine dispersion of (U,Pu) metal second phase showed no reaction with Type 304 stainless steels in sodium at 800°C after 1400 hr of testing.⁽³⁾ These results contrast with those reported for hypostoichiometric UC (previously mentioned) that contained areas of high concentration of free metal. Results of tests with hyperstoichiometric (U,Pu)C containing 10 vol % of sesquicarbide showed no compatibility problems with stainless steel in sodium at 800°C after 4,000 hr of testing.⁽⁴⁾ If these latter results can be verified by significant in-pile testing as well as additional out-of-pile testing and the conditions of stability are identified, then the additional compatibility protection afforded by Cr_{23}C_6 -modified fuels may not be required. Accordingly, carbide fuels which should be considered as potential candidates for 1000-MW reactors in addition to the reference composition include stoichiometric and slightly hyperstoichiometric (U,Pu)C.

Recent laboratory results suggest that essentially single-phase stoichiometric fuel may not be as difficult to prepare as previously envisioned. Carbides containing high oxygen concentrations, of the order of 1 wt % (oxycarbides), which are being investigated in the UK, should also be tested as potential candidates.

3. Fuel Swelling and Fission Gas Release

At the time this study was conducted, no in-pile data were available on modified fuels or sodium-bonded (U,Pu)C fuels in general. The available open literature data on UC and (U,Pu)C were employed to establish the design basis required for the overall reactor study. The swelling data were critically reviewed to determine their relevance to the reference fuel-pin concept. Most of the data were obtained under restrained conditions in a thermal flux. Simple models were used to estimate adjustments in center temperature that account for thermal-flux-depression effects and to estimate unrestrained swelling rates.⁽⁵⁾ The effects of stoichiometry and addition of plutonium as a second component were also examined. The available data for hyperstoichiometric fuel adjusted to account for temperature effects and to represent unrestrained swelling rates are summarized in Fig. 2 as a function of average central temperature. Temperature-adjusted data for hypostoichiometric fuels with equivalent carbon contents of <4.65 wt % are also shown in Fig. 2.

FIG. 2. Swelling of Adjusted Carbide Fuel.

Results for hypostoichiometric UC show accelerated swelling behavior above approximately 1100°C. This behavior has been previously reported and attributed to the presence of a free uranium metal (melting point, 1130°C).⁽⁶⁾ The swelling rate of this fuel decreases with increasing carbon content.⁽⁶⁾ The results for hyperstoichiometric fuel do not show accelerated swelling behavior up to the temperature limit of the data ($\sim 1400^\circ\text{C}$). Significant extrapolation beyond this limit was considered too liberal. Consequently, the maximum fuel operating temperature was conservatively set at 1450°C. The hyperstoichiometric curve in Fig. 2 was recommended as the design basis for the study.

Accelerated swelling of the reference fuel, similar to that of hypostoichiometric UC, was considered a possibility. The Cr and Cr_{23}C_6 second phases in the reference carbide fuel may form a eutectic at $\sim 1500^\circ\text{C}$, which, on melting, may form a pseudoternary eutectic containing (U,Pu)C. The potential effects of these liquid phases on performance are not clear. In any event, the maximum operating temperature was set below the estimated melting point of the binary.

Since this study was completed, 9 pins of (U,Pu)C sodium-bonded to Type 316 stainless steel were inserted in GETR. Unmodified hypostoichiometric and

iron- and Cr_{23}C_6 -modified fuels were included. Neutron radiographs of the six hottest pins taken at maximum burnups of $\sim 35,000$ MWD/T showed no evidence of gross incompatibility. Preliminary meas-

FIG. 3. (a) X Radiograph of W1GH-Preirradiation
 (b) Neutron Radiograph of W1GH-Postirradiation
 (c) Neutron Radiograph of Fuel Column in W1GH-Postirradiation
 (d) Neutron Radiograph of Fuel Column in W3GH-Postirradiation

FIG. 4. Calculated Fission Gas Release for Reference Fuel.

TABLE 1. PHYSICAL PROPERTIES OF 90% DENSE (U_{0.9}Pu_{0.2})C

Property	at 800°C	1250°C
Thermal conductivity, W cm ⁻¹ °C ⁻¹	0.16 ^(a)	0.16 ^(a)
Coefficient of thermal expansion (25 - T°C), °C ⁻¹ × 10 ⁶	11.10	11.95
Specific heat, cal g ⁻¹ °C ⁻¹	0.058 ^(a)	0.063 ^(a)
Bulk melting temperature, °C	2470	
Minimum melting temperature (chromium-modified fuel), °C	1450	
Density, g cm ⁻³	11.70 (Cr-modified) 12.27 (stoichiometric)	
Density of plutonium, g cm ⁻³	2.18 (Cr-modified) 2.34 (stoichiometric)	

^(a) Based on UC data.

urements of fuel-column heights showed the rates of unrestrained fuel swelling to be in essential agreement with the recommended curve. Typical neutron radiographs are shown in Fig. 3 along with an X radiograph of an unirradiated capsule for comparison.

An analysis of the available data for fission gas release also was conducted. A correlation was developed employing a simple model for the escape-rate coefficient. The model was used chiefly as a correlative technique, since fission gas release probably is not entirely controlled by diffusional processes. Details of the calculational method developed have been described.⁽⁵⁾ Calculated data for fission gas release at two temperatures, representative of the average and peak fuel temperatures, are shown in Fig. 4 as a function of burnup. Because of the scatter in the raw data, the $+2\sigma$ limits are substantially higher than the nominal curves (a factor of approximately 2 at 70,000 MWD/T for Curve B).

4. Fuel Physical Properties

The principal physical-property data required for reactor design are melting point, thermal conductivity, linear expansion, density, specific heat, and vapor pressure. In most cases, only little or no data were available for (U,Pu)C; hence, they were inferred from or assumed to be equal to those of UC. Table 1 summarizes some of these properties for specific temperatures. More complete information has been given elsewhere.⁽⁵⁾

Because of the importance of thermal conductivity, the recommended design curve for 100% dense fuel is shown in Fig. 5 with the bands of (U,Pu)C and UC data used to establish the curve. To correct the

FIG. 5. Thermal Conductivity of ~100% Dense (Case) UC and (U,Pu)C.

thermal conductivity to 90% theoretical density (the reference density) an α factor of 2 was used in equation reported in Ref. 7. The conductivity was further adjusted for end-of-life conditions to account for the anticipated effects of irradiation. Two major irradiation effects which should lower the thermal conductivity are formation of soluble fission-product carbides and porosity due to fission gas bubbles. The combined estimates indicated that for 10^5 -MWD/T burnup the initial thermal conductivity should be lowered by 25%. Use of this factor was considered conservative because no account was made for sodium impregnation of the fuel pores and cracks, which would significantly improve the effective thermal conductivity. Estimates indicate that sodium impregnation of half the pores in 90% theoretically done carbide fuel would increase the thermal conductivity by 25%.⁽⁸⁾ Recent examination of sodium-bonded oxide fuels showed that sodium is in fact found in all regions of the fuel. Examination of irradiated sodium-bonded metal fuels also have indicated that sodium is present in the pores.

5. Cladding

Large liquid-metal fast breeder reactors with high-power-density cores will impose stringent requirements on the cladding material. For the present Westinghouse design, cladding will have to operate with OD temperature as high as 1300°F (700°C) in flowing sodium and ID temperatures near 1400°F (700°C) in static sodium (bond sodium) for ~2 yr. Total neutron exposures in excess of 10^{23} n/cm² are anticipated. No presently available material ideally satisfies all the requirements of an LMFBR. Selection of cladding, therefore, must represent the best compromise of available material.

The austenitic stainless steels in general were chosen as the leading candidate cladding materials with annealed Type AISI 316 stainless steels as the reference material. Type 347 stainless steel and Incoloy 800 were considered as potential alternative

candidates. Materials other than stainless steels were briefly evaluated and eliminated for various reasons. These reasons included the relatively poor high-temperature strength and increased fabrication problems of the ferritic steels, the reduced strength of the precipitation-hardened alloys after long-time exposure at high temperature, and the insufficient service data and technology of the nickel-base and vanadium-base alloys. Vanadium-base alloys show a high potential as LMFBR cladding materials, however, and should be developed as longer-range candidates.

A detailed evaluation of the mechanical properties, metallurgical stability, effects of irradiation, and resistance to corrosion in sodium of the candidate materials was conducted.⁽⁵⁾ The final selection was based, to a large degree, on unirradiated properties. Although a significant volume of irradiation data has been generated, differentiation between the selected materials was difficult because of the variations and uncertainty in test conditions and of materials characteristics. Generally, extrapolation of US data indicates that ductility of these materials will be extremely low at the end of life—less than 1% total elongation. One set of UK data for Type 316 stainless steel, however, shows a postirradiation ductility near 20% (10^{23} n/cm²).⁽⁹⁾ The UK results suggest the possibility that specifications for a highly satisfactory material can be developed.

A qualitative summary of the basis for cladding alloy selection is shown in Table 2. Type 316 stainless steel has the highest unirradiated creep rupture strength; its yield strength, creep strength, and tensile ductility are acceptable and comparable to the alternates. The variation in metallurgical stability between the materials is not very great. All tend to precipitate a second phase at some temperature within the operating range. At the time the evaluation was made, there was no significantly experimentally demonstrated difference in the corrosion behavior of the materials in sodium. All the cladding materials are readily available as thin-wall tubing. Welding ex-

TABLE 2. SUMMARY OF BASIS FOR CLADDING-ALLOY SELECTION (1300-1400°F)

Cladding Alloy	Unirradiated Mechanical Properties				Corrosion Resistance	Irradiated Tensile Ductility	Structural Stability	Fabricability	Totals		
	Tensile Yield Strength	Creep Resistance	Creep Rupture Strength	Tensile Ductility					A	B	C
316 SS	A	B	A	C	A	A	B	A	5	2	1
347 SS	A	A	B	B	A	C	A	B	4	3	1
Incoloy 800	B	A	B	A	A	C	A	B	4	3	1
304 SS	C	C	C	C	A	B	B	A	2	2	4

Notes: A to C. Comparative rating in decreasing order.

Unacceptability not necessarily indicated by presence of B or C.

Fig. 6. Selection of Plant Materials.

perience with Type 316 has been excellent. Although some difficulties have been experienced with the other candidates, it is expected that commercially acceptable end-closure weld processes can be developed.

As indicated in Table 2, annealed Type 316 stainless steel has marginal but distinct advantages at the maximum cladding operating temperatures (1300–1400°F) compared to annealed Type 347 stainless steel and Incoloy 800. In addition, the LMFBR state of technology is more advanced, and more applicable information is being developed for Type 316 stainless steel than the alternates. Because the alternates do not offer any clear and significant potential advantages, this latter consideration is of prime importance. Type 304 stainless steel is shown in Table 2 for comparison because it was initially included in the evaluation. This material has poorer unirradiated mechanical properties than other materials at present design conditions. A preliminary mechanical design analysis of cladding behavior of Type 304 and 316 stainless steels and Incoloy 800 also indicated that Type 304 was the poorest of the three and that Type 316 was preferred.

At the time this evaluation was made, the evidence indicated that there were no special advantages of cold-worked material compared to annealed material. Also, there were several reasons why annealed material was preferred, such as better control of property variability, a more advanced state of technology, and a potentially more metallurgically stable structure in-pile (the lower recrystallization temperature of cold-worked material was considered a potential disadvantage). Since the evaluation was conducted, however, additional UK data on irradiated oxide fuel pins has been reported which shows that the total diametral strain for cold-worked Type 316 stainless steel cladding is significantly less than that of annealed cladding. Details of the tests are not available, but the existing information indicates that cold-worked material may have some advantages

in the design proposed for this study. However, until a detailed analysis is conducted and additional information is obtained, annealed material remains the reference choice.

6. Plant and Reactor Materials

The scope of the Fuels and Materials Task of this study was essentially limited to fuel-rod design; consequently, detailed analyses of other materials were not conducted. Materials selections and evaluations for components other than the fuel rod were based primarily on work conducted for the Westinghouse Demonstration Plant. A brief summary of the basis for materials choices of key components is given below, and an overall summary of selections is shown schematically in Fig. 6. Operating conditions are presented in other sections of this document.

In general, evaluation of plant components required consideration of materials compatibility with other components in the overall coolant system, in addition to the specific requirements. Material transfer due to chemical and thermal gradients are the major effects to be considered. To minimize such effects, stainless steels were selected as the reference material in preference to ferritic steels for all major plant components in contact with flowing sodium.

6.1. Reactor Vessel and Barrel

Except for considerations of high temperature, the same data and evaluations were employed for cladding materials applied to the reactor vessel and barrel. However, Type 304 stainless steel is selected as the reference material and Type 316 stainless steel as the alternate.

Type 304 stainless steel is suitable for the reactor vessel and barrel because these components will operate from 750 to 1,000°F. At these temperatures, the strength advantages of Type 316 over 304 stainless steel are marginal and the greater amount of information available on the latter material can be utilized. The abundant data on Type 304 stainless steel indicate that good reliability with respect to resistance to corrosion by sodium and mechanical properties can be achieved with this material. A detailed analysis of potential irradiation effects on this component was not conducted.

6.2. Coolant Piping

The excellent experience with Type 304 stainless steel in similar applications, such as EBR-II, makes this material a logical choice for piping because of its adequate strength, good corrosion resistance, weldability, and commercial availability at low cost.

6.3. IHX and Steam Generator

The factors influencing the choice of Type 304 stainless steel for piping are equally applicable to the IHX.

One of the principal problems in the selection of materials for the steam-generator tube is the range of corrosive environments. Although Type 316 stainless steel is an adequate material for the sodium exposure, it would be subject to stress-corrosion cracking if excursions in operating conditions introduced high concentrations of chloride or hydroxide in the system. The high-nickel alloys have demonstrated excellent resistance to stress corrosion, but are subject to mass-transfer mechanisms in flowing sodium, especially in loops with dissimilar materials, i.e., Type 304 stainless steel in the IHX. In order to avoid the penalties of design complexity and high construction costs associated with the normal solution to this problem, i.e., the use of duplex tubing, either metallurgically or static fluid bonded, it was proposed to use a single material in the tube, Incoloy 800. This material promises adequate resistance to corrosion in both media, as well as acceptable mechanical properties and commercial availability in tubing form. However, considerable work on weld development will be required to characterize all the pertinent properties of this alloy. Development of this component is considered one of the most critical items required for plant design.

6.4. Control Rods

Tantalum has been selected as the reference material for control rods because of its strength at high temperatures, corrosion resistance in high-purity sodium, and adequate nuclear properties. Some work remains to be done to qualify this material, however.

Boron carbide has been selected as the alternative control material. This material has been used successfully in thermal reactors and for limited service in the Fermi Reactor. However, because of the exposures to high flux anticipated in LMFBRs, additional work is required to assess the limitations of the material. Two factors which may limit life, or affect design, are embrittlement of cladding material and helium gas release.

6.5. Pumps

A limited materials analysis of pump components was conducted under AEC Contract No. AT(30-1)-3762. The information developed was applied to the present study. Carbon transfer, sensitization, sigma-phase formation, mass transfer, wear, corrosion,

TABLE 3. STRESS-RELAXATION DATA FOR INCONEL 718

Test Temperature, °F	Initial Stress Condition, ksi	Percentage Relaxation after			
		200 hr	600 hr	800 hr	1000 hr
1000	60	1.4	1.7	1.8	1.8
1100	60	2.2	3.5	3.8	4.0
1100	70	4.1	6.0	6.5	6.9

fabrication, and dimensional stability were considered in the evaluation. Although long-term service experience is not available for all these considerations, no major problems were anticipated. A significant testing program is required, however, to verify the design.

Types 316 and 304 stainless steels and Incoloy 800 selected for use as hydraulic and structural components for service at 900–1200°F. Type 316 stainless steel is recommended for the higher temperatures, with Type 304 being adequate for the lower temperatures. Carbides and Stellite Nos. 1, 3, and Star-J are recommended as bearing material for the higher temperatures, whereas Colmonoy Nos. 5 and 6, and Stellite Nos. 1, 3 and Star-J are adequate for the lower temperature.

6.6 Subassembly Grids

Subassemblies based on a spring-clip type of grid design have been proposed for this study. The high yield strength and stress-relaxation requirements for this type of design suggest the choice of Inconel 718 as the reference material. Inconel 718 has a 100-hr rupture strength at 1200°F of 105,000 psi and a 0.2% offset strength at 1200°F of 126,000 psi. Stress-relaxation data for Inconel 718 is shown in Table 3. Experience with detailed design, fabrication, and successful operation in PWR's supports this material choice. Although PWR experience cannot be extrapolated directly to LMFBR conditions, it provides a base for the development and testing of the grid design.

Significant development and testing, in-pile and out-of-pile, will be required to verify the performance of the subassemblies. Potential brazing alloy candidates, such as Nicrobraz-50, must be thoroughly tested also. A development program for testing grid-type subassemblies in EBR-II has been initiated by Westinghouse under AEC Contract No. AT(30-1)-3791. A possible alternative material for this type of design is Inconel X-750.

Another possible candidate material is Type 316 stainless steel, but this must be used with a grid design other than the spring-clip type. Subassemblies

employing Type 316 stainless grids with a dimple-type design to support the fuel rods have been successfully operated in DFR to fuel burnups of approximately 7 atom %. Details of the tests are not known at present, however, and a detail design analysis was not conducted for the present study.

CORE DESIGN

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1. General

The obvious starting point for the Westinghouse 1000-MWe LMFBR Follow-on Study was the 1964 Westinghouse 1000-MWe design study.⁽¹⁰⁾ Important features of that design have been re-examined in preliminary studies, prior to performance of the conceptual design itself.

Preliminary reactor design studies were performed on:

- a. geometries which would reduce the reactivity increase upon voiding of core and axial blanket,
- b. the effect of substitution of oxide fuel in a reactor designed for carbide fuel,
- c. the merits of fuel venting, and
- d. fuel-assembly design.

All of these studies are discussed in detail elsewhere.⁽¹¹⁾ Because of its importance to the choice of design concept, (a) above will be discussed in some detail in this paper.

All of these preliminary design studies proceeded in parallel from a common design base, as did other studies of fuels and materials and of plant design. At the end of Task I of the 1000-MWe Study, these preliminary design studies and trade-off studies formed the basis for a conceptual design. During the conceptual-design effort, many interesting variants of the selected conceptual designs presented themselves (e.g., a new fuel-element design evolved in the Westinghouse Demonstration Plant design study), and these were included when time and funding permitted. However, a design study must end sometime, and such variants were left out of the final conceptual design if they arose too late.

2. Evolution of Reactor Design—Preliminary Studies

2.1. Comparison of Pancake and Modular Designs

The large positive reactivity change that could occur upon voiding of a large LMFBR, in conjunction with the uncertainties involved in measuring or calculating this reactivity change, the uncertainty con-

cerning the space-time behavior of such a reactor, the uncertainties in sodium superheat and boiling, and the unsatisfactory state of Design Basis Accident (DBA) calculations, lead to a desire to avoid the potential problem by spoiling the reactor geometry.

Modification of LMFBR design to achieve a less positive sodium-void reactivity has been done in several ways over a considerable period of time.⁽¹²⁾ Annular, pancake, and modular designs have been considered. Westinghouse, in the present study, considered pancake and modular arrangements to be the best ways of spoiling the geometry for the purpose of achieving a less positive sodium-void reactivity. Our first studies, then, were devoted to a comparison of four pancake designs of the type studied earlier by GE,⁽¹³⁾ and four 7-module designs of the type studied earlier by Westinghouse.⁽¹⁰⁾ To cover something in between, we also analyzed four 4-module designs. Table 4 lists the core designs studied.

Depletion calculations were performed prior to determination of the sodium-void reactivity at the end of an equilibrium cycle for each of these designs. The results (see Fig. 7), show that nonpositive sodium-void reactivities are possible for each of the three types of designs.

Next, the fuel cycle economics of these three design types considered:

- a. fuel-fabrication cost,
- b. capitalization charge for fabrication,
- c. plutonium credit,
- d. inventory charge,
- e. shipping cost, and
- f. reprocessing cost.

The results (see Fig. 8) show that, of the designs having satisfactory sodium-void reactivity, the 4-module designs have the lowest fuel-cycle cost.

A 4-module design close to design H (see Table 4) was chosen for further analysis; this, along with a reactor design not constrained by the requirement for sodium-void reactivity, formed the basis for the conceptual designs that followed.

Thus, one of the conceptual designs has a reactor which is separated into four units in order to provide a more negative reactivity change upon voiding of sodium from the core and axial blanket. This we will refer to as the "4-module" reactor design to distinguish it from the other design, a "single-module" reactor that has a configuration limited only by heat-transfer and fluid-flow requirements. The 4-module reactor is subject to the same heat-transfer and fluid-flow requirements as the single-module reactor.

One can consider the 4-module reactor to be a backup design for the more economical single-module

TABLE 4. SUMMARY OF CORE DESIGNS STUDIED

Design	Modules	Core Height ^(a) (in.)	Core Diameter (in.)	L/D	Blanket Thickness		Blanket Width (in assembly widths)	Initial Average Plutonium Core Loading ^(b) $\left(\frac{\text{Pu}}{\text{Pu} + \text{U}\%}\right)$
					Radial (in.)	Axial (in.)		
D	7	60.56	31.03	1.94	14.81	12.08	4	21.25
B		52.38	33.36	1.56	11.54	12.08	3	19.63
E		52.38	33.36	1.56	18.33	12.08	5	21.78
A		43.07	36.79	1.16	11.23	12.08	3	19.52
I	4	48.99	45.64	1.06	15.45	15.10	3	17.70
H		36.92	52.57	0.696	22.86	15.10	5	19.
G		36.92	52.57	0.696	30.96	15.10	7	19.40
F		33.52	55.18	0.603	24.24	15.10	5	19.64
M	1 (pancake)	30.37	113.56	0.267	16.55	18.12	2	16.08
L		23.51	131.77	0.178	16.35	18.12	2	18.80
K		23.51	131.77	0.178	22.44	18.12	3	18.80
J		15.52	162.18	0.0954	16.75	18.12	2	25.67

^(a) All dimensions for the hot conditions of the core (890°F).

^(b) Isotopic composition of the plutonium: Pu-239, 61 wt %; Pu-240, 22 wt %; Pu-241, 13 wt %; Pu-242, 4 wt %.

reactor, to be employed if safety problems associated with a single-module reactor are not resolved in time to commit a plant containing a single-module reactor. Alternatively, one can consider the single-module reactor to be an advanced version of the 4-module reactor, to be employed when the uncertainties of sodium-void reactivity, space-time behavior, sodium superheat and boiling, and Design Basis Accident (DBA) analyses have been sufficiently resolved to allow its use. Whichever viewpoint one takes, it is advisable to consider one plant capable of accommodating either reactor, since the problem of sodium-void reactivity will surely be laid to rest sometime during the 30-yr lifetime of the plant.

Comparison of the two reactor designs provides the impetus for more careful study of the problem of sodium-void reactivity by indicating the power-cost penalty associated with a design having a zero or negative sodium-void reactivity relative to a design not subject to that constraint. A comparison of fuel-cycle costs for the two designs is given in "Fuel-cycle Economics" of this paper.

2.2. Replacement of Carbide Fuel by Oxide Fuel

A parallel study was performed to see the effect on economics due to replacement of carbide fuel by oxide fuel in the 7-module reactor of Ref. 10 at a very late stage in the core design-fabrication-operation sequence, such that the same nonvented fuel rods could be used, changing only the fuel material.* As one might expect, this is not an economical thing to do.

* Please note that this study is *not* intended as a comparison of oxide- and carbide-fueled LMFBF's.

FIG. 7. Sodium-void Reactivities.

The magnitude of the economic disadvantage was, however, surprising. As a result of the lower linear power rating and higher rate of release at fission product gas of the oxide fuel, the fuel-cycle cost is 80% greater for the oxide-fueled than for the carbide-fueled reactor. There is also an appreciable capital-charge penalty. Thus, a decision to switch from carbide to oxide fuel should be made when it is still economically possible to change the fuel-assembly

FIG. 8. Relative Fuel-cycle Costs.

and fuel-rod design. If the decision is made later, the oxide core should be replaced by a carbide core as soon as possible.

2.3. Vented vs Sealed Fuel Rods

The comparison of vented and sealed sodium-bonded (U,Pu)C fuel rods has been reported elsewhere.^(1, 11) The comparison shows a small cost advantage (about \$800,000 capital cost and \$300,000 annual operating cost) in favor of vented fuel rods. The potential for operation of fuel rods beyond the design burnup, which is greater for vented rods, was not included in these analyses. The factor leading to recommendation of vented fuel rods in subsequent design studies is, however, not economics, but safety. The vented design enables retention of the sodium bond in the event of a pinhole cladding failure and decreases the probability of adjacent fuel rods being blanketed by fission gas in the event of cladding rupture.

2.4. Mechanical Design

The mechanical design has several unusual features. Some of these features arose entirely within this LMFBR study, but some resulted from Westinghouse experience in design, construction, and operation of light-water reactors. As an example of the latter, those aspects of the reactor internals design that related to flow-induced vibration problems were considered; the resultant design has an all-welded construction and low coolant velocities. Core barrel mounting and fluid flow paths were also chosen with PWR experience in mind, and the vessel is at coolant inlet temperature.

The conceptual designs described here do not show a

can around the fuel assembly. It is clear that possible disadvantages, as well as advantages, arise from use of canless fuel assemblies. We have calculated that crossflow of coolant in the canless core, following a flow blockage at the fuel-assembly inlet, permits more than 80% flow recovery at the hot spot of the fuel rod. This is a substantial advantage, but it must be weighed against the disadvantage that canless assemblies do not permit use of orificing. Canless assemblies will probably be less satisfactory if fuel failures can propagate; further studies are needed on propagation of fuel failures and on the effectiveness of can walls to stop such propagation.

In the trade-off study that compared a piped-type reactor with a pot-type reactor,^(1, 11) the piped concept, ultimately selected by Westinghouse, had to be modified somewhat in order to be as inherently safe as the pot concept, that is, to prevent drainage of core sodium. This required the addition of a siphon breaker to prevent the core from being uncovered as a result of a hypothetical pipe break, the addition of an auxiliary coolant loop to cool the core after such a pipe break, and the addition of a container outside the vessel to preclude core drainage in the event of a vessel leak. The original siphon-breaker concept⁽¹¹⁾ (weep holes between inlet and outlet coolant) was found to require too high a bypass flow of sodium during normal operation and was replaced by a large cover-gas reservoir and check valves adjacent to the cover-gas space which admit gas to the inlet line upon loss of positive sodium pressure. Further studies are needed to compare the reliability and cost of this anti-siphon concept with the reliability and cost of a different piping arrangement which could preclude siphoning.

2.5. Miscellaneous

Before going on to the conceptual designs, we should mention some additional design features. The choice of materials for the reactor is discussed in a companion paper. Throughout the reactor design studies, the fuel is Cr₂₃C₆-modified (U,Pu)C, bonded by sodium to Type 316 stainless steel cladding, and vented. The axial blanket is depleted UC bonded by sodium to the same cladding. The radial blanket is UO₂, helium bonded to Type 316 cladding and non-vented. Sodium is the coolant, of course, and all structural materials are Type 304 stainless steel (except for the small amount of material in strongbacks and rod-spacer grids).

Some of the design parameters (e.g., fuel center-line temperature) changed as a result of preliminary design studies, and only the values used in the final conceptual design will be given in this paper.

3. Reactor Conceptual Designs

The conceptual designs are based on the results of the preliminary studies just discussed. As stated earlier, two designs are considered, the "4-module" and the "single-module." In order to isolate the effect of the sodium-void criterion on design, these two reactor designs use the same designs of fuel, fuel rod, fuel assembly, reactor internals, vessel, and plant. They are subject to the same heat-transfer and fluid-flow constraints. They differ only in fuel enrichment, number and arrangement of core subassemblies, and number and arrangement of blanket subassemblies.

Table 5 shows the principal features of the two reactor designs. Table 6 shows the design and performance features of the fuel assembly. The four-module design of Table 6 has a reactivity gain of 51¢ upon voiding of a module core and axial blanket. This resulted from a succession of changes in control area, core height, blanket thickness, and fuel enrichment. Since safety studies that could indicate the suitability of this number had not been performed, it was decided not to proceed with the minor redesign (e.g., height reduction) needed to achieve a negative sodium-void reactivity.

From Tables 5 and 6, it is evident that the two design concepts are very similar, differing primarily in the volume of radial blanket which is used to decouple the modules of the 4-module design but absent from the single-module design. Examination of peak temperatures shows that the single-module design, although it has a far less favorable sodium-void reactivity, has somewhat more of a thermal design margin because of more favorable hot-channel factors. The effect of this design margin on the probability of initiating an accident by means of the positive sodium-void reactivity must yet be assessed in detail. The cross section and elevation for the two conceptual designs are shown in Figures 9 and 10. Figure 9 shows schematics of one quadrant of the 4-module design and one quadrant of the single-module design. Figure 10 shows a schematic of the reactor vertical section, and illustrates the core support, flow paths, control-rod guides and drives, and nozzles.

4. Fuel-cycle Economics

It should be noted that the breeding ratios and doubling times shown in Table 5 do not represent the best obtainable with designs such as these; rather, those result from an attempt to achieve minimum power costs for the single-module and 4-module reactor designs.

A Westinghouse proprietary fuel-cycle-optimization

TABLE 5. FEATURES OF THE 1000-MWe LMFBR CONCEPTUAL DESIGNS

Item	Four-module Reactor	Single-module Reactor
Thermal power, MWt	2600	2600
Reactor inlet temperature, °F	770	770
Reactor outlet temperature, °F	1000	1000
Total coolant flow rate, lb/hr	1.27×10^8	1.27×10^8
Core height, ft	3.27	3.32
Equivalent core diameter, ft	(four) 3.89	7.710
Blanket thickness, in. between	15.6	
Blanket thickness, in. peripheral	10.5	10.5
Number of fuel assemblies	(4 × 61 =) 244	240
Number of radial blanket assemblies	261	160
Number of control elements	60	57
Simple doubling time, yr	14	11
Breeding ratio (middle of equilibrium cycle)	1.33	1.32
Fuel material	Cr ₂₃ C ₆ -modified (U,Pu)C	Cr ₂₃ C ₆ -modified (U,Pu)C
Axial blanket material	UC	UC
Radial blanket material	UO ₂	UO ₂
Control material	Ta	Ta

code,* together with SFBROPT, which will be discussed in a companion paper, were used to seek the optimum combinations of reactor and plant designs for the 4-module and single-module designs, where "optimum" means minimum power cost. The designs reported in this paper are different from the designs that would result from straightforward application of these computer codes. Some of this difference is due to the desire to use one plant with both reactors, some of it is due to the desire to be further from design limits such as maximum cladding temperature, and some is due to the approximations inherent in the codes. The codes are very useful guides for design comparisons and for indications of relative trends, but cannot be used as direct design tools. The

* This code couples the relevant aspects of thermal-hydraulic, mechanical, and nuclear design with a costing analysis for fuel fabrication, nuclear depletion and accretion, shipping, and reprocessing of spent fuel. Fuel-cycle costs are established at the end of each fuel cycle and normalized to apply at midcycle by means of present worth techniques. The code will handle carbide- or oxide-fueled reactor cores with sodium or gas bonding of the fuel to the cladding. The geometrical disposition of the fuel rods can be in either a hexagonal or square array.

TABLE 6. CHARACTERISTICS OF 1000-MWe
LMFBR FUEL ASSEMBLY

Size of assembly, in.	5.29	
Fuel-rod geometry	Square	
Number of fuel rods	168	
Number of grids	6 (none in core)	
Fuel-rod pitch, in.	0.375	
Outer diameter of fuel rod, in.	0.302	
Wall thickness of fuel rod, in.	0.012	
Fuel-pellet OD, in.	0.246	
Volume fraction of fuel	0.292	
Volume fraction of sodium	0.522	
Volume fraction of steel	0.072	
Volume fraction of molybdenum	0.053	
Volume fraction of tantalum	0.061	
Cladding material	Type 316 SS	
Web material	Type 316 SS	
Strongback material	Mo and Type 316 SS	
Grid material	Inconel 718	
Bond material	Na	
	Four-module Reactor	Single-mod- ule Reactor
Length of axial blanket, in.	15.0	15.0
Fuel enrichment, Pu(Pu + U)		
Zone 1	0.205	0.15
Zone 2	0.250	0.18
Zone 3	—	0.21
Average fuel burnup, MWD/MT	100,000	100,000
Reactivity change upon voiding of core and axial blanket (at end of equilibrium cycle), β	0.51	10.40
Doppler coefficient (at end of equilibrium cycle), $T(\Delta k/\Delta T)$	-0.00243	-0.0028
Pressure drop through fuel as- sembly, psi	56.0	60.1
Vessel nozzle-to-nozzle pressure drop, psi	88.0	93.5
Average velocity of core coolant, ft/sec	27.0	28.2
Peak velocity of core coolant, ft/ sec	28.4	29.3
Average heat flux, btu/hr-ft ²	0.80×10^6	0.81×10^6
Peak heat flux, btu/hr-ft ²	1.69×10^6	1.57×10^6
Average linear power, kW/ft	18.5	18.7
Peak linear power, kW/ft	39.2	36.3
Average power density, kW/kg fuel	165.7	168.2
Maximum cladding surface tem- perature, °F	1300	1239
Maximum fuel centerline tem- perature, °F	2622	2405

results of optimization calculation must be verified by more detailed calculations before a design can be defined.

Table 7 compares the optimum designs chosen by these optimization codes with the conceptual designs reported in this paper. The optimum designs are based on approximations inherent in the optimization codes

FIG. 9. Cross-section Quadrants of the Single-cylinder and Modular Cores.

such as the extrapolation of nuclear parameters and the assumption that the same mechanical design could be used for a 6-ft-high core as for a 3-ft-high core. As can be seen from Table 7 the power cost differences are slight. Thus, considering the inaccuracy of the input data and the limitations of the calculational techniques, the conceptual designs discussed here may be considered, for the purposes of this study, to provide very nearly the minimum power cost for 4-module and single-module designs. For the optimum designs, Table 8 gives the costs of fuel fabrication associated with the fuel-cycle costs of Table 7. Fuel-fabrication costs for the conceptual designs are nearly the same.

PLANT DESIGN

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1. General

The Westinghouse reference design for the LMFBR 1000-MW plant has been established by preparing conceptual designs and cost estimates for six different concepts. These concepts are presented in Table 9. The Westinghouse Advanced Reactors Division was assisted by United Engineers and Constructors, Inc. in this task. The design and costs of specific plant components were the product of a joint effort by the

FIG. 10. Cross-sectional Elevation of the Reactor Vessel and Internals.

TABLE 7. COMPARISON OF OPTIMUM AND CONCEPTUAL DESIGN

Item	Four-module Design		Single-module Design	
	Optimum Design	Conceptual Design	Optimum Design	Conceptual Design
Core height, ft	3.33	3.27	3.36	3.32
Fuel-rod OD, in.	0.306	0.302	0.306	0.302
Reactor ΔT , °F	250	230	224	230
Fuel-cycle cost, mill/kWh	0.75	0.78	0.52	0.52
Contribution to fuel-cycle cost due to: (mill/kWh)				
Rod fabrication	0.30	0.32	0.25	0.25
Assembly fabrication	0.18	0.19	0.15	0.15
Plutonium credit	-0.39	-0.41	-0.40	-0.41
Plutonium inventory	0.45	0.47	0.36	0.37
Shipping and reprocessing	0.21	0.21	0.16	0.16

Westinghouse Atomic Equipment Division, the Westinghouse Advanced Reactors Division, Westinghouse Heat Transfer Division, and Westinghouse Large Turbine Division.

In order to make the selection of the Task I reference plant concept, technical and economic comparisons were performed for the six concepts considered. The base plant was chosen as a base to which the five alternative concepts were compared. The effects on plant desirability were determined with regard to:

- piped versus pool primary heat-removal systems,
- two versus three primary heat-removal loops,

- hot-cell versus under-the-plug refueling,
- moisture-separation or steam-reheat versus sodium-reheat steam cycles, and
- hot-cell versus pressure-shell reactor plant containments.

With the assistance of the Architect-Engineer, reactor plant layouts and technical and economic analyses were made for:

- several containment arrangements,
- hot-cell under-the-plug refueling,
- two-loop and three-loop plants, and
- pool concept of the primary coolant system.

The studies included detailed plant heat balances, preliminary nuclear shielding design, and preliminary auxiliary system design.

2. Evolution of Plant Design

2.1. Comparison of Piped Versus Pool-type Primary Arrangements

There are a number of possible arrangements for primary coolant systems. In addition to economic and

TABLE 8. COSTS OF FUEL FABRICATION FOR OPTIMUM DESIGN

	Four-module		Single-module	
	Core	Radial Blanket	Core	Radial Blanket
Fabrication cost, \$/kg	163	33	164	33
Assembly costs, \$/kg	100	45	101	47
Total, \$/kg	263	78	265	80

TABLE 9. CONCEPTS STUDIED IN TASK I

Concept No.	Title	Loops (No.)	Type of Refueling	Type of Containment
1	Base plant	2	Hot cell	Hot cell
2	Integral primary system	2	Hot cell	Hot cell
3	3 Heat-removal loops	3	Hot cell	Hot cell
4	Under-the-plug refueling	2	Under plug	Pressure
5	Sodium-reheat plant	2	Hot cell	Hot cell
6	Pressure containment	2	Hot cell	Pressure

TABLE 10. COST COMPARISON OF 2-LOOP PLANT VS POOL CONCEPT^a

Item	Piped Concept		Pool Concept	
Reactor containment structure	7,310		7,480	
Reactor vessel	1,400	2,970	1,180	2,500
Reactor vessel plug	1,160	2,430	1,840	3,900
Reactor tank			1,430	3,580
Reactor thermal shield	660	1,640	1,680	3,910
Serpentine shield with B ₄ C and tank	540	700		
Cooling of reactor shield	130		180	
Reactor preheating system	Base		10	
Emergency cooling system	2,420	2,100	50	100
Reactor plant cranes	340		310	
Primary pumps and drives	2,980		3,900	
Primary pipes	400	800	40	80
Intermediate piping and valves	610		730	
Intermediate heat exchangers	3,920		3,010	
IHX skirt			190	480
IHX plug			790	1,620
Coolant receiving and storage	Base		1,800	
Primary coolant charge	340		1,680	
Intermediate coolant charge	270		280	
Capitalized heat-rate differential	280	440	Base	
Total direct costs	22,760	27,980	26,580	35,550
Differential direct cost	Base	Base	3,820	7,570
Differential price	Base	Base	5,540	11,000

^(a) Costs in Thousands of Dollars.

reliability considerations, other factors which were considered to be significant in evaluating the various arrangements were:

- capacities of future sodium-to-sodium heat exchangers,
- capacities of future sodium pumps,

- pipe layout, including structural and hydraulic considerations,
- reactor and hot-cell orientation,
- accessibility of components for maintenance, and
- safety aspects.

Three conceptual designs of primary coolant systems were prepared. These were:

- a two-loop pipe system,
- a three-loop pipe system, and
- a pool-type system.

The design was sufficiently detailed to perform the design evaluation and cost estimates. The system design included specifications for the reactor vessel, the piping size and arrangement, heat-exchange equipment, sodium pumps, and primary-system containment.

The evaluation of the concepts considered the differential power costs, capacity, reliability, and availability. It was concluded that the piped concept was less costly, easier to maintain, presented fewer technical problems, and afforded a greater level of flexibility both in design and operation. Table 10 compares the costs of the pipe and pool concepts. The detailed study and comparison of the two-loop and three-loop plants indicated that the larger-size components were more economical. Therefore, the Task I reference plant has two externally piped heat-removal loops, each loop consisting of one primary sodium pump, one IHX, and single-walled piping.

The variable-speed coolant pumps are located in the cold leg of the circuit. Inlet and outlet reactor vessel nozzles are placed above the active core to prevent vessel drainage in the event of a pipe rupture. Additional studies are necessary to evaluate the economics and reliability of an elevated loop arrangement as compared to check valves and syphon breakers which are provided in the reference design.

2.2. Number of Coolant Circuits

Experience with light-water reactors in the past ten years has shown that there is an economic advantage to the use of a few large-size components rather than multiple duplication of smaller units. In plants where reactor safety permits, and where continued operation at part load is not an operational requirement, a single, main heat-transport loop plus an auxiliary heat-removal loop is the feasible minimum. However, continued plant operation with one loop shut down for maintenance is a desirable operating characteristic and necessitates multiple heat-transport loops. Apart from these considerations and requirements, the minimum number of heat-transfer

loops for large LMFBR plants will depend on the availability of large sodium components, such as pumps and heat exchangers, that meet the system's functional requirements. Discussion with the designers and manufacturers of intermediate heat exchangers, pumps, and steam generators led to the conclusions that the capability for manufacture of these components by 1980 will not limit the choice of a two- or a three-loop plant.

A detailed study and comparison of a two-loop *vs* a three-loop pipe plant was performed. This study considered safety, reliability, availability, and cost. It was concluded that the larger number of components in the three-loop plant decreased overall plant reliability, increased maintenance cost, had virtually no effect on plant thermal efficiency, and increased allowable partial-load availability. The study also indicated a saving of 4.22 million dollars when a two-loop plant is chosen over a three-loop plant. It was therefore concluded that the conceptual plant would utilize a two-loop configuration.

2.3. Hot-cell Refueling *vs* Under-the-plug Refueling

A preliminary evaluation of under-the-plug refueling, hot-cell refueling (defined as open pool refueling in an inerted containment), and a cask system led to the selection of the under-the-plug and the hot-cell concepts for further study. The hot-cell concept was chosen as the reference concept because it appeared to have the best overall potential. In particular, it has high flexibility in its ability to handle or support unexpected effort in maintenance of the reactor and fuel-handling equipment. The under-the-plug concept was evaluated as an alternate because of its potential for efficient fuel handling. This concept, however, provides poor accessibility and questionable reliability for the mechanisms and internals under-the-plug. Conceptual designs of the refueling machines and preliminary arrangements were prepared for the reference and alternate concepts. The arrangements considered are shown in Table 11.

After these concepts were available, the relative capital costs of the two concepts were developed with the Architect-Engineer's assistance. The economics favored the hot-cell concept, as shown in Table 12. In addition, a qualitative evaluation of such things as reliability, accessibility for maintenance, ease of operation, safety, and development, favored the hot-cell concept. The under-the-plug concept continues to be an attractive alternative because that in the long term, equipment of high reliability can probably be developed and demonstrated to have satisfactory performance in under-the-plug environments.

TABLE 11. REFUELING ARRANGEMENTS

Hot Cell	Under-the-plug
a. Rectangular hot cell	a. Two-loop plant (internal primary piping)
b. Circular hot cell	b. Two-loop plant (external primary piping)
c. "L" shaped hot cell	c. Sea-of-sodium primary system
d. Rectangular hot cell with separate spent-fuel-storage cell	
e. Circular hot cell with separate spent-fuel-storage cell	
f. Sea-of-sodium primary system	

Hot-cell refueling has been selected as the reference design concept.

2.4. Hot-cell Containment *vs* Pressure Containment

The significant difference between "hot-cell" containment and "pressure" containment as used here is that in the "hot-cell" containment the entire containment structure contains an inert atmosphere and thus serves as a "hot cell" for the reactor and refueling area. The "pressure containment" utilizes a steel containment building for the entire primary system, which is protected from missiles by a concrete, steel-lined hot-cell structure which also acts as a biological shield and contains an inert gas atmosphere. In the "hot-cell" containment the superstructure was selected as reinforced concrete sections containing two thin steel liners separated by a nonstructural 2-ft layer of concrete plus an external 4-ft layer of structural concrete. Both designs meet the criteria for a 30-psi design pressure, and missile protection, as well as insulating, structural, and biological requirements.

Four basic containment designs were initially proposed. A preliminary cost analysis of these designs and for two pressure levels, 30 psig as nominal and 60 psig as an alternative, was performed by the Architect-Engineer. The cylindrical structures were determined to be preferable to the rectangular configurations.

Six plant concepts were ultimately studied on the two containment bases selected. The economic evaluation of these studies is shown in Table 13. On the basis of the study of capital cost trade-offs, the "hot-cell" containment was selected as the reference design; however, some basic limitations of the work in this area must be pointed out. First, the plant was designed for ideal ground conditions with suitable bearing characteristics and no ground water. The design required a heavy mat for bearing that was rigid

TABLE 12. ESTIMATES OF CAPITAL COST AFFECTING SELECTION OF FUEL-HANDLING CONCEPT

Description	Cost in Thousands of Dollars			
	Hot-cell Refueling Concept		Under-the-plug Refueling Concept	
Reactor containment structure				
Excavation	208		208	
Structural concrete	1816		1744	
Reinforcing steel	2323		2230	
Insulating concrete	90		90	
Structural steel	135		129	
Siding and roofing	194		194	
Linear plate	2554		2405	
Total	7320		7000	
Reactor equipment				
Vessel and plug	2560	5400	3250	6880
Thermal and neutron shields	1200	2340	1340	2520
Shield cooling	130	130	130	130
Reactor plant cranes	340	340	360	360
Total	4230	8210	5080	9890
Nuclear fuel handling and storage				
Fuel hot cell crane	170		—	
Reactor fuel-handling machine	550		830	
Lateral fuel-transfer machine	200		210	
Manipulators	220		220	
Shielded windows	300		80	
Fuel-transfer machine (tunnel)	—		90	
Vertical fuel-handling machine (fuel hot cell)	—		380	
Misc. hot-cell equipment	230		200	
Total	1670		2010	
Total direct costs of items affected	13,220	17,200	14,090	18,900
Total indirect costs of items affected	5,949	7,740	6,340	8,500
Total costs of items affected	19,169	24,940	20,430	27,400
Cost Differential		Base	1,261	2,460

enough to assume a fixed stance for columns and walls. Other site conditions such as seismic loads, snow, and wind loads were not investigated. Therefore, general topography, geology, hydrology, and meteorology could significantly affect the plant cost.

More work must be done on the DBA to provide better pressure and temperature data on a time-dependence basis. In addition, the heat-sink effect

of the internal and containment structures should be examined in more detail. Pressures and temperatures potentially could be reduced for the containment design.

2.5. Moisture Separation vs Sodium Reheat

Preliminary studies indicated that the 2400-psig/900°F steam cycle selected for the sodium-reheat steam cycle of the 2600-MWt plant required external moisture separation to minimize turbine-blade erosion at low pressure. Additional preliminary studies indicated that there was only a slight advantage in heat rate for a live steam-reheat cycle over an external moisture-separation cycle. Since steam reheat requires a greater capital investment, it was not considered further. In view of this, a detailed comparison was made of the moisture-separation steam cycle with the sodium-reheat cycle. It was concluded that the moisture-separation cycle is superior both tech-

TABLE 13. CAPITAL ITEMS AFFECTED BY HOT-CELL OR PRESSURE CONTAINMENT

Description	Costs in Thousands of Dollars	
	Base Plant for	
	Hot-cell Containment	Pressure Containment
Reactor containment structure		
Excavation	208	192
Structural concrete	1816	1592
Reinforcing steel	2323	1842
Insulating concrete	90	69
Structural steel	135	4924
Siding and roofing	194	119
Liner plate	2554	2382
Total	7320	11,120
Reactor plant cranes	340	360
Coolant systems		
Coolant pipes (primary)	402	310
Piping and valves (secondary)	606	592
Reactor coolant receiving and storage	Base	50
Initial primary coolant	342	308
Total	1350	1260
Inert atmosphere (argon)	50	70
Cost total items affected	9060	12,810
Cost differential items affected	Base	3,750
Price differential	Base	5,450
Price differential, \$/kWe	Base	5.20

nically and economically. Sodium reheat is considered to be more complex and less reliable, and has a higher initial plant capital cost than the moisture-separation cycle. The savings due to the lower heat rate of the sodium reheat was not sufficient to balance the higher capital cost. The power cost increase of sodium reheat over moisture separation was found to vary from 0.06 mill/kWh down to 0.014 mill/kWh, depending upon the fuel cost and other assumptions.

TABLE 14. PLANT PARAMETERS

Nuclear reactor, type	Forced-circulation, sodium-cooled, fast-spectrum breeder
Number of heat-transfer loops	2 main + 1 emergency
Power-conversion system, type	Rankine, steam-reheat, regenerative cycle
Reactor power, MWt	2600
Net plant output, MWe	1055
Turbine steam conditions, psia/°F	2446/900
Net plant efficiency, %	40.58
Primary system	
Flow rate, 10 ⁶ lb/hr	127.4
Reactor inlet temperature, °F	770
Reactor outlet temperature, °F	1000
Pressure drop, ft	285
Piping size, in.	48 and 36
Secondary system	
Flow rate, 10 ⁶ lb/hr	89
IHX inlet temperature, °F	640
IHX outlet temperature, °F	966
Pressure drop, ft	120
Piping size, in.	36 and 26
Power-conversion system	
Fluid	Water/steam
Flow, 10 ⁶ lb/hr	9.807
Feedwater temperature, °F	500
Feedwater pressure, psia	2450
Throttle steam temperature, °F	900
Throttle steam pressure, psia	2400
L. P. turbine inlet pressure, psia	158

FIG. 11. Plan View of Two-loop System for 1000-MWe LMFBF.

FIG. 12. Elevation View of Two-loop System for 1000-MWe LMFBF.

REACTOR POWER, MWt	2600
NET PLANT OUTPUT, MWe	1055
NET PLANT HEAT RATE, BTU/KW-HR	8411
NET PLANT EFFICIENCY, %	40.58

FIG. 13. Schematic of Simplified Plant.

3. Plant Conceptual Design

3.1. General

The conceptual plant design that evolved from these and other studies utilizes a) two external piped primary and intermediate heat-removal circuits, b) combined circular hot-cell refueling and containment, and c) a moisture-separation steam cycle.

Table 14 gives the plant parameters for the conceptual design. Figure 11 illustrates equipment and piping arrangement of the primary and intermediate sodium systems. The components have been grouped to suit structural arrangements, and also to provide accessibility for inspection and maintenance. The evaluation of the thermal, reaction-load, and support requirements indicates that this arrangement is satisfactory; however, additional work will be required to

evaluate the seismic loads. The system arrangement provides adequate separation of equipment from the reactor so that shielding can be used to prevent neutron activation of the components, thus permitting access to components for contact maintenance. Figure 12 shows an elevation view of the containment IHX and steam-generator buildings.

FIG. 14. Cross Section of Reactor Vessel.

3.2. Containment

At this time it appears that the containment must provide a space between the concrete vessel and the inner steel vessel to demonstrate conclusively that leakage of products from the hypothetical core meltdown and compaction accident will meet 10-CRF-100 requirements. It appears that due to the biological half-life of plutonium, even a small amount (2% of core inventory) released into the containment will not permit leak rates as high as 0.1% without very large exclusion areas (greater than 1 mile). The ability to pressure test a containment structure periodically and demonstrate accurately leak rates less than 0.1% is not now apparent.

Presently, the Architect-Engineer is evaluating several multiple-barrier containment designs. The design that appears to be the most promising includes a cylindrical pressure-containing inner steel vessel enclosed by an unlined concrete vessel with a controlled leakage space between the vessels. Both the steel and concrete vessels have spherical domes. The vessels are supported by a reinforced concrete foundation mat which rests on bed-rock.

The below-grade cylindrical section is supplemented by the addition of a half-circular section that houses the primary-system components and the intermediate heat exchangers. Piping from this section passes through penetrations in the cylindrical concrete and steel vessels.

FIG. 15. Plan View of Fuel Handling.

Inert atmospheres are provided within the containment structure. The refueling hot-cell atmosphere is argon; below-grade vaults are inerted with nitrogen.

Containment design work beyond this preliminary Task I effort will depend on further work to:

- a. perform the following analyses for design-basis accidents for the purpose of specifying the functional requirements of the containment and the associated consequence-limiting safety systems for its reference plant:

1. core-reassembly accidents, including core meltdown with reassembly and compaction;
2. core-meltdown accidents without reassembly;
3. primary sodium fires;
- b. specify the functional requirements of containment systems and consequence-limiting systems based on the analyses mentioned in Part a. A design-basis accident that will serve as the basis for Westinghouse Containment Design will be selected after these analyses. The following are

FIG. 16. Elevation View of Fuel Handling.

TABLE 15. PARAMETERS FOR PRIMARY- AND SECONDARY-SYSTEM COMPONENTS

Reactor Vessel		Secondary Sodium Pumps	
Design temperature, °F	1025	Type	Single-stage centrifugal, vertical, gas seal of shaft
Design pressure, psi	200		
Vessel material	Type 304 SS	Flow rate, gpm	102,200
Overall length, ft	45	Design temperature, °F	965
Outside diameter, ft	17.5	Design pressure, psi	200
		Head, ft	120
Primary Pumps		Rated speed, rpm	720
Type	Single-stage centrifugal, vertical, removal concentric casing, gas seal of shaft	Shaft length, ft	20
		Nominal power, hp	2800
Flow rate, gpm	148,800	Material	Type 304 SS
Design temperature, °F	1025		
Design pressure, psi	200	Steam Generators	
Head, ft	285	Type	Counterflow, vertical, shell and tube
Rated speed, rpm	652		
Shaft length, ft	28	Rating, MWt	1300
Nominal power, hp	10,700	Overall length, ft	69.6
Material	Type 304 SS	Outside diameter, ft	14.7
		Effective heat-transfer area, ft ²	61,500
Intermediate Heat Exchangers		Shell side	
Type	Counterflow, vertical, shell and tube	Fluid	Secondary sodium
		Design temperature, °F	1000
Rating, MWt	1300	Design pressure, psi	200
Overall length, ft	39	Material	Type 304 SS
Outside diameter, ft	12.7	Tube side	
Effect heat-transfer area, ft ²	45,096	Fluid	Water/steam
Shell side		Design temperature, °F	1000
Fluid	Primary sodium	Design pressure, psi	3000
Design temperature, °F	1025	Material	Incoloy 800
Design pressure, psi	200		
Material	Type 304 SS	Piping	
Tube side		Primary sodium system	
Fluid	Secondary sodium	Sizes, in.	36 and 48
Design temperature, °F	1025	Design temperature, °F	1025
Design pressure, psi	200	Design pressure, psi	200
Material	Type 304 SS	Material	Type 304 SS
		Secondary sodium system	
		Sizes, in.	26 and 36
		Design temperature, °F	1000
		Design pressure, psi	200
		Material	Type 304 SS

considered to be a minimum on which to base the selection of the design:

1. total energy release,
2. energy partition,
3. pressure and temperature transients,
4. missile hazards,
5. aerosol generation, and
6. radioactivity release, transport, and behavior.

The effect of the following consequence-limiting safety systems must also be investigated:

1. blast shielding,
2. missile shielding,
3. energy-heat-removal devices,
4. radioactivity scavenging systems, and
5. aerosol settling devices.

Finally, following the assessment of the containment concepts, a final complete calculation for a design-basis accident can be performed to determine specific engineering criteria for containment design.

3.3. Systems

Figure 13 is a simplified schematic of one of the two nearly identical main coolant loops; the appropriate temperatures and combined flows for both loops are shown. During shutdown, and in the event of interrupted operation of a primary pump, residual core heat is removed by the auxiliary system flow maintained by the auxiliary electromagnetic pump. The residual heat in the auxiliary system is transferred to the NaK cooling system through the aux-

liary heat exchanger. The equipment is compactly arranged in a fully shielded room adjacent to the reactor cell.

3.4. Reactor Vessel

Figure 14 shows the reactor vessel for the conceptual design. It is a single-walled cylindrical shell with a hemispherical bottom head and a clad shield plug for closure. The vessel shell contains two 48-in. inlet and two 48-in. outlet main coolant nozzles, two 22-in. auxiliary nozzles, a 4-in. sodium-overflow nozzle, a 2-in. argon-outlet nozzle, a 24-in. fuel-transfer nozzle, and four to six 6-in. instrumentation ports. The shell varies in thickness from 2.25 to 4.25 in., with a 14.2-ft internal diameter and a 20-ft outside flange diameter. The vessel is supported approximately on the centerline of the fuel-transfer nozzles to minimize differential expansion between the vessel and the fuel-storage pit. Thermal protection is provided for the vessel by thermal shields and by thermal baffles at the main and auxiliary cooling nozzles.

3.5. Refueling

The refueling concept for the 1000-MW plant is the hot-cell, lateral-transfer approach. In this concept, the fuel assembly is lifted from the core by the #1 Vertical Fuel-Handling Machine and placed in an upright bucket attached to the Lateral Fuel-Handling Machine. The Lateral Fuel-Handling Machine then ro-

FIG. 18. Preliminary Optimum Parameter Variations.

tates the bucket to a horizontal position and withdraws it through a fuel-transfer port in the reactor vessel into the spent-fuel-storage tank. The bucket is then rotated to an upright position, and the fuel assembly is removed and placed in a storage location by the #2 Vertical Fuel-Handling Machine. This procedure is reversed for loading the core. All of the fuel-handling operations are performed under sodium to circumvent cooling problems. Figures 15 and 16 are plan and elevation views which illustrate the fuel-handling systems. The refueling paths for new and spent fuel are also shown.

4. Optimization Studies

4.1. General

The various plant-design studies served to select a reference concept. However, they did not consider all the system parameters and their relationship to component costs to minimize the cost of electricity for each concept.

As mentioned previously, the computer code SFBROPT* was used to obtain an "optimum" design for the reference concept design from the standpoint of minimum power cost. The component parameters shown in Table 15 represent the "optimum" design values, and will undoubtedly be modified as detailed plant and component designs are developed to reflect practical design and fabrication considerations.

* The SFBROPT code uses a pattern-search technique to calculate the cost of electricity in mills/kWh from twenty independent variables. The optimum is found by alternate search and extrapolation moves of the variables until the minimum total cost of electricity is found as a function of fuel-cycle cost and capital charges in investment. Fuel-cycle costs are included by fitting FABROC results to polynomial expressions included in the optimization program.

FIG. 17. Bulk Sodium Outlet Temperature vs Component Costs.

4.2. Component Cost vs Reactor Outlet Temperature

Figure 17 shows the costs of some of the major hardware as a function of reactor outlet temperature. The increase in primary and secondary pump costs are due to increased flow rate and pressure rise as a result of decreased core ΔT .

The decrease in IHX and steam-generator costs up to about 1050°F is due to increased LMTD's at the higher temperatures and decreased total heat transferred due to increased plant efficiency. Above 1050°F, the costs start increasing due to the decrease of allowable material stress and partly because of a reduction of the rate of increase of LMTD due to reaching the constraint on the maximum ΔT across the steam-generator tube sheet.

4.3. Optimized Plant Temperatures and Pressure vs Reactor Outlet Temperature

Figure 18 indicates how the plant parameters, which are optimized for a given reactor outlet temperature, vary with reactor outlet temperature.

SUMMARY

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The previous sections of this paper, together with the summary plant description, have developed a picture of the reference designs. It is emphasized that there are two reference designs, one with a spoiled geometry core to meet the criterion for a zero or negative sodium-void coefficient and a design of unspoiled geometry which has a positive reactivity of approximately \$10 for total voiding of core and axial blanket. We consider the unspoiled geometry design to represent the ultimate target, with the modular design representing a fallback position. Even though the modular design represents a fallback position, its economic promise is sufficient to ensure it entering the economy subsequent to its development. With a discrete difference in probability of attainment of the alternates, but a large economic incentive for attainment of the most economic alternate, both designs were evaluated in the parametric studies of Task III, as well as the research and development program of Task IV.

Task III included the study of power costs as a function of a number of parameters. These studies are not yet complete; however, preliminary results of the study of the effect of mixed mean reactor sodium outlet temperatures are available for presentation in this paper.

FIG. 19. Parametric Study of Costs vs Sodium Outlet Temperature.

Figure 19 is a plot of power cost versus sodium outlet temperature for the four-module core design. The curve represents a locus of optimized designs in which the independent variable of outlet temperature was fixed, and all other parameters allowed to vary within the limits of the design concept and physical design restraints. The curve does not extend below 900°F because the features of the core and plant systems and components become increasingly invalid at those extremes. The minimum point, at approximately 1000°F, represents the optimum temperature for the reference concept.

The separate curves of fuel-cycle cost and fixed charges must be considered to obtain a better understanding of the study. The fixed charges, or plant capital costs, which represent the largest component of power cost, are relatively flat through the range from 1000 to 1060°F and reach a minimum at ~1030°F, somewhat above the minimum for power cost. At the lower range, plant costs rise, reflecting the more expensive turbine plant associated with poorer steam conditions and the large heat-transfer surfaces resulting from lower coolant temperatures. At the upper range, the increasingly lower costs of the turbine plant are overridden by the increasing costs of heat exchangers, pumps, and the reactor vessel as heat-transfer surfaces and structural members increase in thickness and complexity due to lower strength properties at higher coolant temperatures.

The fuel-cycle costs become minimum at lower than

TABLE 16. SAMPLE TASK IV WORK SHEET

System	Subsystem or Component	Critical Reference Plant Functional Requirements	Current Technology	Information Needs to Implement Reference Plant
5.0* Primary Reactor Coolant System	5.2.1.4* Primary Sodium Pumps	Type: Mechanical, single stage, centrifugal with free sodium surface. A vertical shaft driven by a wound rotor induction motor with liquid rheostat speed control.	The two pumps built to date which most closely resemble the reference design are the Fermi and Hallam pumps.	Basic Data
6.0 Intermediate Coolant System	6.2.1.4 Intermediate Sodium Pumps	<p><u>Primary Pumps</u></p> <p>Capacity: 149,200 gpm</p> <p>Dynamic Head: 300 ft. of Na</p> <p>Operating Temp.: 770°F</p> <p>Design Temp.: 800°F</p> <p>Material: 304 SS</p> <p><u>Intermediate Pump</u></p> <p>Capacity: 103,500 gpm</p> <p>Dynamic Head: 140 ft. of Na</p> <p>Operating Temp.: 650°F</p> <p>Design Temp.: 700°F</p> <p>Material: 304 SS</p>	<p>Fermi</p> <p>Capacity: 11,800 gpm</p> <p>Dynamic Head: 310 ft. of Na</p> <p>Operating Temp.: 550°F</p> <p>Design Temp.: 1000°F</p> <p>Operating Time: Intermittent for 5 yrs. up to 1000°F</p> <p>Material: 304 SS</p> <p><u>Hallam</u></p> <p>Capacity: 7200 gpm</p> <p>Dynamic Head: 160 ft. of Na</p> <p>Operating Temp.: 945°F</p> <p>Design Temp.: 1000°F</p> <p>Operating Time: 800 hrs. at 900-1000°F</p> <p><u>EBR-II</u></p> <p>Operating Temp.: 700°F</p> <p>Design Temp.: 800°F</p> <p>Operating Time: "Many thousands of hours"</p> <p><u>S.R.E.</u></p> <p>Operating Temp.: 950°F</p> <p>Design Temp.: 1000°F</p> <p>Operating Time: 800 hrs.</p> <p>Some corrosion and mechanical properties in Na data are available for austenitic materials, but not under operating conditions.</p> <p><u>State-of-the-Art</u></p> <p>Reference: LMFBR Program Plan</p> <p>Section 3, Appendix B, pp. 365 to 379.</p>	<p>1. Mechanical property data for 304 SS in sodium for the temperature range 700-800°F.</p> <p>a) Tensile Data (U.T.S. Yield Ductility)</p> <p>b) Fatigue Data (High Cycle)</p> <p>c) Creep and Stress rupture data (Sections 6-7)**</p> <p>2. Identification of suitable bearing materials, on the basis of wear in sodium, thermal properties (conductivity, expansion), sodium corrosion and mass transfer, under start up and low head conditions. (Sections 6-7)</p> <p>3. Corrosion-erosion data for pump impeller materials. (Sections 6-7)</p> <p><u>Engineering Data and Techniques</u></p> <p>4. Hydraulic data on specific pump concepts in order to optimize the pump hydraulic performance, and to assure good agreement between predicted and actual performance, including variation of head, flow and NPSH requirements with varying sizes and designs of impellers, approach channels, guide vanes, etc. (Section 3)</p> <p>5. Data concerning the overall behavior of and resulting effects on pump operation of the sodium free surface within the pump tank, including ability to control sodium level, potential for gas entrainment, etc. (Section 3)</p> <p>6. Data on the resulting stresses and/or deformation in critical areas (areas where close tolerance must be maintained) due to the severe thermal environment. (Section 3)</p> <p>7. Data concerning the dynamic characteristics of all rotating elements and its relation to vibration of these elements and the pump body. (Section 3)</p> <p>8. Mechanical design bases for specific pump sub-components. Included are static seals, and considerations for assembly, disassembly, and maintenance of the unit. (Section 3)</p> <p>9. Fabrication techniques for large bearings, impeller shafts, and casings. (Sections 3 and 7)</p> <p><u>Experimental Verification and Operational Analysis</u></p> <p>10. A gas type shaft seal for shaft diameters of approximately 8-12 inches. Specific needs include: a) suitable seal materials, b) formulation of acceptable seal performance characteristics, and c) seal proof tests in specially designed rigs and prototype pumps. (Section 3)</p> <p>11. A radial bearing compatible with 8-12 inch shaft diameters. Specific needs include: a) determining actual bearing loads that will be encountered, b) bearing materials suited to this application, and c) proof tests of bearing designs in specially designed rigs and prototype pumps. (Section 3)</p> <p>12. Proof tests of and operational analysis of prototype pumps to verify design and provide a base for further extrapolation. (Sections 2-3)</p> <p>** These section numbers refer to sections of the LMFBR Program Plan.</p>

* These numbers refer to specific paragraphs in the "1000 MWe LMFBR Conceptual Plant Description (Tasks II and III Final Report)" to be issued by Westinghouse under contract to the Argonne National Laboratory.

plant-optimum outlet temperatures, indicating that the core design concept is more limiting than the plant concept. As coolant temperature is raised, core ΔT must become increasingly smaller to hold the cladding surface temperature and coolant velocity below their limits. Modification of the core concept to increase optimum coolant temperatures could bring the minimum of fuel-cycle and plant costs to the same range, but might require retreat from the concept of relatively simple open assemblies, through the use of orificing refractory cladding. These changes would increase fuel-cycle costs through higher fuel-fabrication costs and poorer neutron economy. Since fuel-fabrication costs represent $\sim 60\%$ of the fuel cycle, designs that tend to increase fabrication costs may better be avoided in early commercial plants.

In many cases the parametric studies confirm (and provide quantitative evaluation of) factors that were appreciated prior to the study. The sensitivity of power cost to fuel-fabrication costs, burnup, and specific power are examples. It has, however, pointed out areas in which relaxation of design restraints would produce substantial gain, such as core coolant velocity. Increase of allowable velocity would lower fuel-cycle costs as well as increase the optimum reactor bulk outlet temperature. This provides incentive for experimental evaluation of corrosion-erosion of clad materials and hydraulic-mechanical evaluation of fuel rod and core structural vibration performance at higher coolant velocities.

The payoff for the studies lies in Task IV which provides for:

1. identification of required new data and information required for successful design and construction of the reference design;
2. quantitative evaluation of the penalty on performance and power costs for not attaining these data and information;
3. a research and development program to obtain these data and information.

Most members of this conference, in one way or another, have been concerned with developing research and development programs for LMFBR's. Most of these programs are oriented by technical discipline, reflecting in many cases the structure of the organization producing the program. The 1000-MWe studies provide a different framework for program development, that of a number of target plants systematically described in a format uniform among reactor designers.

In order to capitalize on this tool, Westinghouse has oriented the first step of program development — identification of required new data and information — closely to the reference plant description. Each system

and component of the plant has been reviewed to identify critical reference plant functional requirements. An assessment of current technology is then made in such a way as to parallel the reference plant functional requirements. Comparison of the target plant requirements with current technology then reveals whether new data and information are required to implement the reference design. If current technology is adequate, the item is not a subject for R&D; if current technology is not adequate, then the information needs are identified.

To provide a framework for this analysis of needs, a worksheet was used to identify the system, subsystem, or component, compare critical reference plant functional requirements, and tabulate information needs. An example of this worksheet is presented in Table 16 for large sodium pumps.

The information needs may be categorized as basic data, engineering data and techniques, experimental verification, and operational analysis. They may also be cross referenced to the 10 sections of the LMFBR Program Plan. The needs identified in the first two categories in general may best fit in Base Technology Programs, with broad applicability. The needs categorized as experimental verification may be performed either under Base Technology Programs or Development Programs supporting a Demonstration Plant Project. Operational analysis will come primarily through Demonstration Plant Programs, the FFTF Project, and operation of major development facilities. Clear delineation of information needs is essential prior to formulating R&D programs in order to focus our finite resources on solution of essential problems.

The reference designs are target plants. The probability of achievement of all target features in early plants is less than one. Value analysis, performed in Task IV, attempts to define the penalty for non-attainment or partial attainment of goals. For example, if extrapolation of pump size to provide $\sim 150,000$ gpm is not feasible within the time frame of early plants, a logical fallback position is $\sim 90,000$ gpm in three coolant loops. The penalty for this partial attainment of target objectives is 0.05 mill/kWh in power cost, an acceptable penalty which would be erased in later plants after further evolutionary development.

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Discussion

Mr. Gibson (GE): In one of Mr. Anderson's slides depicting the difference between the four-module and the one-module concept, it showed the average burnup as being 100,000 MW-days/ton, with the peak in the four-module being 172,000, I believe, and the peak in the one-module being 156,000. First, I wonder how these burnup figures were derived; secondly, in the comparison of the economics between these two concepts, is a penalty taken for the reduced probability of achieving the peak burnup of 172,000 in the four-module concept?

Mr. Bishop (W): The difference between the burnup capabilities, that is, 172,000 and 156,000, is due essentially to a difference in hot-channel factors. For example, in the modular design, in contrast to a single-module design, it probably would be due to a difference in the inlet flow distribution to the four modules. In other words, for the single-module concept, the hot-channel factors for flow may be lower, and that could be carried out to the flux, etc.

Mr. Gibson: I was mostly concerned with whether there were additional penalties associated with the decreasing probability of reaching the higher burnup in your assessment of economics.

Mr. Bishop: As yet we have not assessed the decrease *per se* as related to the higher burnup; it certainly would have to be considered.

Mr. Ziegler (Siemens): Mr. Anderson mentioned that you use canless fuel. I would like to know what the sacrifice is in bulk sodium reactor outlet temperature and efficiency due to the fact that you cannot use and are not applying individual throttling. Also, how do you manage to reduce your flow rate through blanket elements in appropriate measure?

Mr. Anderson: The answer to the first question is that we don't know; that's something we want to study next year. We have to look at the relative advantages of canning and uncanning. In a separate study we are examining orificing mechanisms other than bottom fixed orificing, but we used fixed orificing at the bottom of the blanket elements in this study.

Mr. Ziegler: If you apply fixed orificing in the blanket, then you have a pressure difference between the blanket elements and the core elements at the bottom, and this would apply very high forces to them.

Mr. Anderson: We do have cans around the blanket elements, and the forces across them are tolerated by the can walls. A rather heavy wall is used in the blanket region.

Mr. Latzko (Delft): You have explicitly stated that your design was not optimized for breeding ratio nor for doubling time. However, when comparing some of the main data for the oxide and carbide cores which have been presented at this conference, one cannot help wondering about the present incentives for an expensive, large-scale development program for carbide fuels in sodium-cooled fast breeder reactors. I should like to hear your opinion on this point.

Mr. Anderson: I'm afraid that about all I could say is that we think that we've been rather conservative; I don't know about the others.

Mr. Bush (BNWL): I'm not a designer, so this question is probably naive; however, I'm a firm believer in Murphy's law, that if something can go wrong, it will, and I think that existing reactor experience bears this out. Perhaps fast reactors will be different, but I doubt it. My question is: have you considered a parameter such as unexpected and extended downtimes; if so, how adversely does this influence your cost picture?

Mr. Beckett: This particular study did not have a sophisticated analysis of such things as power cost in a particular concept as a function of availability. We are in an availability study, now that we have completed this design, to see what is a realistic availability. For the economic studies, the ground rules in the contract, to provide some method of comparing the work, gave us an availability factor. I believe the availability factor for use in economic studies was around 80%. In our own demonstration

plant design, we're using 70%, so this 80% may be optimistic, certainly for early plants.

Mr. McCarthy (Chairman): I think that the point also can be made that when you have a cost versus temperature curve as you do, there is a lot more incentive to slide down toward the lower temperature than go up toward the higher temperature for just the reason that you bring up—that reliability is terribly important. This size unit will represent, for a big power system, a money generator that accounts for something of the order of 10–15% of the whole income of the company in a year and so; as you point out, this is terribly important.

Mr. Beckett: This factor certainly influences design selections. For example, you may have noticed that ours is the only concept that does not use a sodium reheater. Our study showed that there was no economic advantage. Even if there were a slight economic advantage for sodium reheat in the early plants (and we've enough problems with the steam generator and other equipment as it is), we would option not to add another development problem and another component to fail in early operation when it doesn't provide us any significant economic advantage.

Mr. Wolfe (GE): I notice that since the ANS meeting in San Francisco you've changed your refueling scheme somewhat, and I'd like to ask a couple of questions about it. First, why did you go to through-tube refueling, rather than lifting it out, which I thought you were going to do in the last design. Have you looked at the cooling requirements for transfer of the fuel in a horizontal position through the tube and possible malfunctions as you do it? When you bring your fuel in, why don't you just drop it into the vessel rather than go through the pool and back through the hole? Finally, with regard to your viewing windows, do they give you a problem in containment which, I understand, has double walls separated with concrete to give double containment?

Mr. Beckett: I could get out of all of this by saying that this is a vehicle to define the problem and you have defined some of them for this system. As I pointed out to someone else, they had slides showing different things and we've run into this same problem; we've selected concepts, we've studied them in more detail, and the more you study any particular concept, the more unsatisfactory it becomes. Our early approach was to overlook the problem of cooling; then we began to do some calculations and found that this was no easy task, and there were things to answer to regulatory groups about what happens if the cooling media is cut off, etc. So we came up with this scheme, which has disadvantages, but at least has the advantage of keeping the fuel in the sodium. Extensive heat-trans-

fer calculations verifying adequacy of cooling, I must confess, are needed. We have found that there are disadvantages to this system in that we are forced to turn the fuel, and there's the problem of integrity of the channel. If you have a bellows in there, is it reliable and what kind of isolation valves must you have? In our demonstration plant work we are leaning toward the first approach, using a bucket-type transfer in which we would suck up the element in some sort of bucket, keep it in sodium, and loop it over head. We will study that in fine detail. Now we have the problem of sodium dripping, etc., so there are advantages and disadvantages of each; they are almost at a standoff right now. You asked why did we go to the bother of not going directly into the vessel. I think this might be done—of course, it depends on vent design. Are we going to have seals so that we have a relatively easy way of handling, or will it have to be resealed in an upright position in an inert atmosphere? These are areas that the depth of these studies doesn't really define. It may be necessary to go straight into the pot because we don't dare turn the vented fuel element over, depending on the vent design.

Mr. Dieckamp (AI): I'd like to hear about the choice of Incoloy-800 for the steam generator.

Mr. Hobson: We selected Incoloy-800 for several reasons: one, it gets us away from the chloride stress-corrosion problem; two, it gives us a material from feedwater to steam outlet that has appropriate capabilities; third, it has been by far the best performing material for corrosion-erosion problems in sodium-water reactions.

Mr. Golan (AI): I would like to pursue further the chart that showed energy cost or capital costs as a function of reactor outlet temperature. In our studies at AI, we found that cost dependence on outlet temperature is rather sensitive to assumptions made regarding the unit-surface costs for the IHX and the steam generator. You indicated that as outlet temperature increases, penalties are incurred because of the drop in allowables, increased stresses, and so on. Could it be that this is partly due to a unitized design, and that as you go up in temperature the problems associated with the tube sheet become unmanageable? If there is a trade-off between a unitized design and a smaller steam generator, what is this trade-off? For example, what is the unit-surface cost for your steam-generator design?

Mr. Beckett: Looking at the mechanics of getting these data, two subroutines are used; one of them is a IHX design code, the other is a steam-generator design code. These models are appropriate for our concepts. They don't automatically compare the modular

concept versus our concept, so they have their limitations, but then we're examining only our concept. We think these tools are more sophisticated than many because we have had the services of our equipment divisions to provide us with cost data and design techniques. Surface area, strength, LMTD, number of tubes, and number of welds are the types of things that are in the program, and it doesn't treat increased size of tube sheet as one of the variables. Strength, heat-transfer area, cost of tubing, and cost of some of the major aspects of fabrication are considered.

Mr. Golan: Could you indicate the approximate unit-surface costs used in the study of the IHX and the steam generator?

Mr. Redding (W): The steam generator cost is about \$75/ft², and the IHX may be twice that amount, the reason being that the IHX has short tubes and the steam generator has long tubes. We have chosen to keep a constant thickness of tube wall through our steam generator, so that our wall is fixed, rather than made of welded sections. We are more attentive to final conditions than other people, who start with a thin wall and build up the thickness of the wall through the steam generator.

Mr. Michel (Belgonucléaire): I noticed that the design material for the grid is Incoloy-718. Will you indicate why this particular choice instead of stainless steel?

Mr. Biancheria: The proposed grid has a spring-clip design which is to accommodate fuel-rod expansion. This requires a high stress-relaxation property of the material, which points to Incoloy-718. Type 316 stainless steel is, of course, a possibility which we are considering. DFR people have irradiated a few bundles using Type 316 stainless steel, but this is not a spring-clip design—it's a dimpled design; you couldn't possibly use a spring-clip design. Type 316 is not out of the question for a different type of design, but we haven't studied it in detail.

Mr. Wyckoff (ComEd): You didn't mention an overall capital cost in dollars per kilowatt. Have you folks whipped up such a number, and what might it be?

Mr. Beckett: The architect-engineers in our equipment divisions are now examining these parameters to determine if they are realistic, and are designing and costing out parts of the plant. We could give you a number which we are still in the process of checking: \$157/kilowatt. That number has a rather poorly justified fudge factor to take care of indirect costs, whereas some of the systems have relatively sophisticated models; other parts of the model simply multiply by some factor of the power.

Mr. Lancet (AI): In your modular core, what is

the maximum void worth of the positive regions of the core; is the 50¢ figures for the overall core?

Mr. Anderson: I can't remember the sodium-void reactivity as a function of position; the 51¢ that I gave was for total voiding of the core and axial blanket of one module. That obviously adds up to something between 50¢ and \$2 if you look at all four modules, but \$2 doesn't sound so good. We're aiming at zero, and four times zero is pretty close to zero. By a slight modification of the four-module design, we think that we can come up with zero if voiding of all four core and axial blankets in the modules occurred.

Mr. McCarthy: I'd like to ask a question about the functional requirements of the containment system that you showed this morning. What is it designed to contain, and what temperatures and pressures are assumed? Did you think much about those offset buildings where the pumps and the IHX's are located with regard to how much they'll cost to put in?

Mr. Hobson: We really haven't made a good assessment of that cost—as to whether it's a disadvantage. I think the containment as shown on the slide is somewhat misleading. We currently visualize a steel containment design for about 30 psi, which we believe will accommodate the design-basis accident—then a space and then a concrete vessel, which is essentially biological shielding. They do better than 0.1% leakage to the environment.

Mr. McCarthy: What do you see as the source of pressurization; is it from a fire or from a vaporized core, etc.?

Mr. Hobson: We're looking at both, but we think the predominant source will be the vaporized core.

Mr. Okrent (ANL): If I recollect correctly, when Combustion Engineering compared their favored cylindrical shape with a spoiled geometry, they found that it was hard to draw a conclusion that the reactor having the near-zero sodium-void effect gave a more desirable course of events in some kinds of postulated accident leading to disruption of the core. Have you done similar comparative studies?

Mr. Anderson: That's one of the embarrassments when we prepare an R&D program. Perhaps this is why we reached the same conclusion—that maybe the same R&D is needed for both of these conditions, because it's been pointed out that the sodium-void coefficient may not be the crucial item of consideration. We may have to solve as many problems for the modular, spoiled-geometry reactor as we have to solve for an unspoiled configuration. I think that is the sense of what I've heard in the meeting, and it's the conclusion which we arrived at independently. We

plan to try to do the type of quantitative evaluation that was described by Combustion Engineering and General Electric.

Mr. Hannerz (ASEA): In the plant description distributed before the meeting, it is indicated that there are six spacer grids, but there are no spacer grids in the core. Does this mean that the pins are unsupported through the core length, which is about 3 ft?

Mr. Anderson: This core has what we call a web cluster design. It's a square array, and there are webs welded to the cladding at periodic intervals of about 6 in., such that four rods are bundled together to form a unit. With this web cluster design, which we have tested hydraulically and for which we have looked at the welding process, it appears that we could get by without grids in the core region, and with grids just in the upper and lower blanket and vent regions.

SESSIONS ON LARGE FAST REACTOR DESIGN

SESSION LFRD-VI

Saturday Afternoon, November 9, 1968

Panel Discussion

Chairman: D. Okrent
Argonne National Laboratory

Secretary: C. V. Pearson
Argonne National Laboratory

Panel Members
G. Denielou, CEA-CEN Cadarache
M. Nozawa, Japan Atomic Energy Research Institute
K. Wirtz, Karlsruhe

Panel Discussion

Mr. Okrent: We appreciate Dr. Wirtz's willingness to appear on the Panel in place of Dr. Häfele, who had to wire his regrets because of a last-minute emergency. The panelists will each give a short presentation on subjects of their choice, and we will then open the session up to discussion.

Mr. Denielou: My first point is kind of an exclusion principle. It seems to me that it is almost impossible for a company to do the following things at the same time: (a) build a reactor, and (b) produce cost estimates. You have to do one thing or the other. We have listened to figures of dollars per kilowatt installed, and though these figures don't seem very encouraging in one respect, they seem almost low when you look at all the designs that were presented. In my opinion the situation is not very favorable.

The evaluations of costs were made for 1000-MWe plants with the assumption that the burnup was in excess of 100,000 MW-days per tonne. We have seen figures as high as 160,000 MW-days per tonne on the screen, and more. If the present inflation pattern continues we may expect that the price of uranium is going to increase, and maybe also that the price of plutonium is going to increase. Nevertheless, yesterday and this morning, we saw figures of the order of \$92 to \$165 per kilowatt, which, it seems to me, are so close to one another, that they were of the same family as the \$60 per second ramp. GE was more pessimistic, but it seems to me they also were more realistic. My impression is that our only chance for success is to truly cut down the costs. In this respect, I regret that we were not able to hear people from the big architect-engineer companies, who apparently do a lot of work evaluating prices. It would have been good to listen to the opinions of Sargent & Lundy, and others. I think a lot of work remains to be done in cutting down the capital costs by looking at all the specific items.

We have been doing such a review on the control rods in Rapsodie. Without changing any specifications, we have been able to gain a factor of 2 on the cost of one control-rod drive. I am sure that we probably can gain another factor of 1.5 in the course of the next review. If you look at the number of rods, you will see that the designs have shown as many as 61 rods. I think there is a lot to be gained in this type of reflection.

It seems to me that the control system and instrumentation is one of the main areas where capital costs can be reduced. I am afraid that I cannot follow the general trend of overinstrumentation,

which seems to be more or less a generalized practice. Such practices as putting a lot of instrumentation at the exit of each fuel subassembly will definitely increase the capital cost. Shielding hasn't been mentioned. We had the experience of overdesigning the shielding, which resulted in gross expenses.

I think that safety obviously has been in everyone's mind as an area for reducing the capital costs. I am pretty sure that, generally speaking, safety and economy will not be fused together, but will behave more like systems. For instance, usually when you add a component you don't increase the safety. I am sure that we will have more to say about double-wall containment. When you add some kind of double-wall feature, you add not only double walls, but also the interference problem within the tube walls, such as the atmosphere-control problem and the inspection problem of the internal wall. Because of these complications it is doubtful that, generally speaking, you really increase safety. But you certainly increase the capital costs.

The last thing I wish to add on this point is that I think that what has been said has been fairly shy in incorporating the technical advances that are likely to take place between now and 1980. This span of time is about the same as that necessary to set a man on the moon, and I think we could incorporate more technical prognostications into these 1000-MWe concepts. As an example, what would happen if we had a reliable and cheap, though modest, under-sodium ultrasonic television system? This is very likely to be developed within the next ten years. What would be the influence of such a development on the prototype fuel-handling mechanisms?

The second point I wish to make concerns the irradiation policies. I think that it is clear to everyone here that it is impossible to simulate in any type of machine the irradiation conditions that will prevail in a 1000-MWe reactor. The fluence on the cladding, for instance, is not likely to be simulated in the same manner. You can simulate separately what happens to the cladding and what happens in the fuel, but you cannot simulate exactly the conditions of the large fast reactor. There is a tendency, I think, to acknowledge this fact and to make a separation in the problem between prototype reactors and materials testing reactors to irradiate structural materials. This is because if you wish to have a decent irradiation reactor, you have to have a driver fuel which is in itself sufficient to make a good industrial prototype. Referring to this observation, I would like to ask the audience if they do not think that, perhaps, FFTF, a

reactor which we haven't heard much about, could probably become less loop-minded and go more in the direction of prototype testing. Maybe PEC could also become less loop-minded and evolve in the direction of a materials testing reactor. After all, why test all these dangerous fuel elements when we are really interested in testing safe and reliable fuel elements? My conclusion is that we must not continue to accept everything that the people who have been working on fast reactors for a long time have taken as being evident. We must continually review all the basic assumptions. And I do not think we lack the money as much as we mainly lack imagination or ingenuity. As was said in the Paris revolution, "Ingenuity must take over."

Mr. Okrent: If the Panel continues this trend, we will find that the Panel is asking the audience questions. Later on we will try to see what the audience has to say about FFTF and PEC.

Mr. Nozawa: We had an Argonne conference on large fast reactor safety in 1965. At that time, people were talking about the sodium-void consideration and how important it was that we reduce the sodium-void coefficient. People were also talking about maximum hypothetical accidents and how we could accommodate these big TNT explosions. But within three years, we have changed our minds to say that these sudden accidents should be more realistic. In today's and yesterday's papers, I noticed that most of the cases postulated just single-channel boiling of sodium. This, I think, is a healthy way of organizing our research and development program, so that we can concentrate our efforts on the more realistic cases.

With respect to this basic approach to the development problem, we would like to see more realistic engineering data on sodium boiling in actual fuel channels. This could be done by using actual fuel elements in thermal reactors, or maybe by using electric heaters.

Secondly, we should notice that we are now trying to make fast reactors safe by using instrumentation. We were very concerned about inherent safety before, but now are making more realistic assumptions about the accident. However, now we must develop this instrumentation—not just in-core flow meters, but also in-core neutron instrumentation. This kind of instrumentation must be developed for fast reactors so that we can detect a nominal malfunction in the core and shut down the reactor. In this respect we also need advancement in sodium technology, to measure the velocity and flow rate in a small coolant channel. Not only do we need these kinds of instruments, but also the methods for bringing these signals from the core to the outside of the reactor vessel. This

technology must be developed. I think that in the selection of a double rotating plug concept or a hot-cell concept for refueling we must be careful to consider how we intend to get such signals from the core to the outside of the reactor. This has to be developed as quickly as possible so that it can be tested in existing reactors and in the experimental reactors to be built in the near future.

The third point I would like to mention is in respect to the irradiation procedure that Mr. Denielou just mentioned. Instead of having higher flux or higher power density, I think that a better approach to the development of more reliable fast reactor fuel would be to determine the limitation on high burnup in a fast reactor. One way to study this problem is to inspect these fuels very closely. We can perform this work partly in the EBR-II alpha-gamma cave. I believe that the determination of the limitations of performance of fuel in the fast reactor is very important. People talk about the rupture of fuel pins, but we must avoid such rupture. Other people discuss the use of vented fuel, but no one knows the actual limitation on burnup in either the unvented or vented case. It may be that power reactors using sealed fuel pins can allow some leakage from these sealed fuel pins. If this is so, then what is the difference between vented and sealed fuel pins?

My last question is in regard to Mr. Denielou's last comment and is a very simple one. Many of the power plants presented used the hot-cell concept for refueling. I wonder if it is possible to operate refueling machines automatically in those remote cells? I haven't seen such designs described in this conference. Originally the hot-cell concept was called the visual scheme, which means the ability to see the fuel assemblies during handling. The use of manual remote control seems to be to be a reversal in technology.

In other areas such as fuel fabrication, control of lasers, remotely operated milling machines and other remotely operated processes, computerized machines are used. In recent years the operators have used pushbuttons, tapes, and computer cards to perform their tasks. I wonder if the same type of machine-operation control will be used in the hot-cell refueling concept.

Mr. Wirtz: It is certainly very difficult to deal adequately with what has been presented, and I also must confess that when the prospect of being on the Panel came up, I had to determine what would be the philosophy behind this Panel. You said that the Panel should consist of neutral people. However, in trying to find some relations between the Panelists and neutrality to the subject, I think maybe it is the distance of their normal location from the Argonne Laboratory.

Let me touch on a few items that came to my mind. The first one is fuel. It seems to me that since 1965 there has been a tendency, a strong tendency, to use vented fuel, and I guess this should be appreciated. Personally, I am very much in favor of vented fuel, because when you really think about it the inside gas pressure on the fuel cladding may be as great as 100 atm, and venting is certainly a good way out of the difficulties in designing for this pressure. But as I have already indicated by a few questions during the meeting, I feel that if the vented-fuel concept is really adopted, one should know in detail how the concept works and especially the type of in-reactor test data required to prove the design. If we come back here in a few years, I would very much like you to press on your Panelists and the people who give reports that they should tell more about the real design and behavior of the tested fuel elements that everybody has in mind. Perhaps I should add also that my impression is that in the fuel concepts, in general, there are still many assumptions that are only assumptions, and that many of the fine figures we have seen depend on these assumptions to a high degree. These figures are, therefore, as uncertain as some of the assumptions might turn out to be in the end.

On the other hand, I am impressed by the optimism that has been expressed. For instance, if I correctly understand the last paper presented this morning, vented fuel is expected to achieve a burnup of 170,000 MW-days per tonne. If there really is some chance for this to be realized, this of course will be a wonderful fuel element, and such a step will greatly enhance the whole industry. I think that this is really a very hopeful sign, but allow me to say that there may be some skepticism.

My next item is design. If my memory serves me correctly, there seem to be no major changes in the basic plant concepts from those presented at the 1965 conference. There still is competition between the concepts of the pool and the loop primary coolant system, single- vs double-wall ducts, hot-cell vs through-the-plug fuel-handling concepts, and cylindrical- vs modular-core concepts, as we had formerly. Although I may be wrong, I feel that so far there is no outspoken preference for any one of these alternatives. I would be very happy if someone could rise up and say that he feels definitely that we don't need these cells above the reactor, or that we don't need double walls, or that we do need them, and so on. So I think that we are still in the phase where the alternatives are being discussed.

My next item is costs, in a very general way of course. As far as I understand the situation, the sodium breeder with oxide fuel will for some time to

come have competition from what Dr. Zinn has called the "moving targets" of the large water reactors. Costs of light-water reactors might still come down a bit, but as far as we in Karlsruhe understand the situation, the sodium breeder with oxide fuel will have difficulties in appreciably reducing the fuel costs. Carbide fuel has better prospects for the sodium breeder, but I understand—and this is also the understanding of Karlsruhe—that carbide fuel is not yet ready for use; there is still some research to do on that fuel. I think this was also expressed here. Again I refer to the last presentation of this morning, where considerable optimism with respect to carbide fuel was expressed, and I think this is also reassuring for anybody who is really interested in cheap and long-lasting fuel.

The main scale of preference here in the United States actually is, and always was, cost per kilowatt-hour. I would only like to mention that there might be different scales of preference that could be applied, and I would like to say a few words in that connection. The principal one is breeding ratio, but not being a citizen of the United States, there might be other ones. For instance other scales of preference might be need for natural uranium supply, need for separation capacity, or even need for further research and development. If you develop and compare these scales of preference you see that the desirability of the different types of reactors might be influenced by more than just costs. Actually we have done such studies at Karlsruhe, and I am sorry that I do not have the material here so that I could present it in this connection.

Let me also say a word about other coolants. The sodium breeder with oxide fuel has the advantage that it belongs to a type that has been proof-tested by prototypes, like EBR-II, the Dounreay reactor, and to some extent the Fermi reactor. The basic fuel problems appear to have been solved, and there are no principal problems anticipated with oxide fuels for a sodium breeder. As I said before, this is not the case for carbide fuel. For steam or helium coolant systems, we do not even have a fuel-element design. We hope that the helium reactor fuel element will be good for coolant outlet temperatures of around 600°C. But this type of reactor is, in my mind at least, not very attractive compared to the sodium-cooled oxide breeder. If helium is to enter the field of fast reactors, it must have a fuel element that can stand much higher temperatures, and in my opinion a fuel element of that kind is not at hand. A fortnight ago a meeting was held at Jülich, Germany, at which some gentlemen from the United States were present, where this was stated fairly explicitly.

The same is true for steam. As you know, we have

done quite a bit of research with steam. The result is that although the rest of the reactor is well in hand, we are still missing a fuel-element concept that promises to be competitive. The steam-cooled fast reactor, therefore, is in a stage where we have to worry about the fuel, and when someone develops a good fuel element, then maybe the steam-cooled breeder can come back in. But, we all agree, I think, that its breeding potentials are not very good and its attractiveness is limited, even if there were no canning problems at all. So much for coolants.

Another point is safety. It seems to me that there is a good chance that the sodium breeder will be feasible from the point of view of safety. But, nevertheless, I think some points that have already been mentioned need further clarification. For instance, methods of detecting local failures in the core, including blockages, need better definition. Another is analysis of propagation phenomena and methods for preventing the propagation of failures. Also, there is the need for clarification of the design-analysis consideration for thickness of the subassembly can wall. Instrumentation of the core to cope adequately with these failures has been mentioned very often, but do we have the right specifications and the required quality of instruments at hand? We at Karlsruhe are not quite certain about this.

We would also like to have a fail-safe design against ejection or sudden movement of control rods, which would help prevent sudden and rapid ramps. Still another is the fact that more information is needed about the fuel-sodium reaction, especially for the case wherein hot liquid fuel produced during some type of excursion is ejected into the sodium. We have discussed this quite a bit at home, and we feel that we do not know enough about this problem.

As I have mentioned earlier, I feel that we should know much better what is the range of credible reactivity ramps. \$60 per second appears to be a standard. It is used at Karlsruhe also, but more important than this is the energy release to be expected from such ramps. Sometimes we call it 200, sometimes 2000, or 2400 MW-sec. We are very uncertain about this, and it has great consequence on the design of the reactor.

With respect to safety philosophy, there was one thing that concerned me during the presentations. One presentation distinguished between credible accident and hypothetical accident. This was followed by the requirement that the plant should be able to deal with the hypothetical accident as well as the credible accident. My feeling is that there is some inconsistency in philosophy here. If the accident is hypothetical, the plant shouldn't necessarily have to accommodate that

kind of accident. I feel that the plant design must deal with an accident that is not hypothetical. We should have a new kind of word between credible and true hypothetical accident.

My last point deals with the test reactor that has been already mentioned by my two colleagues here at the table. We have heard about the Italian PEC, a 140-MW test reactor that will deliver flux in excess of 1×10^{15} n/cm²-sec. I think that this will be a very valuable research tool, and we should be happy that someone is going to build it. We in Germany and in the Consortium have been thinking about test reactors of that type too, because there still appears to be the need for much higher fluxes for testing than those currently available. We also know about the FFTF that is being designed. But then there is the problem that Mr. Denielou has mentioned, namely, of what significance is it to build test reactors that can produce fluxes of the order of 1×10^{16} n/cm²-sec? Is it just another reactor that needs to be built, because it has the capability to create test environments of the type with which we have had no experience in the past? Isn't it better perhaps to start with a prototype reactor of the type that we have been talking about, namely, the 300-MW prototype reactor? If we say yes, then we can ask the next question: do we really need another test reactor? Do we need a test reactor at all? If the answer is again yes, wouldn't it be fine to have another test reactor that uses a different type of coolant, maybe gas, because this is such a promising coolant? If the answer is yes, this might be fine too, but why worry about gas now, because it is not as attractive as sodium, and we all agree on this. I can only say that this kind of reflection is going on at Karlsruhe and at other places. I wonder what the situation is here. You can apply the same kind of questions to FFTF. My question is addressed to my American colleagues, and I would like to know what they think in respect to the FFTF Reactor.

Mr. Okrent: Since all of the Panel members have raised questions with regard to test reactors, suppose we take comments from the audience in the area of test reactors at this time.

Mr. Kintner (AEC): There have been so many questions raised about FFTF that I think it would be appropriate to give you some facts about FFTF as its design exists today. These facts are pertinent to the kinds of things that have been talked about in the presentations and other discussions on the 1000-MWe studies.

The first point is its test capability. This is in particular reference to what Mr. Denielou asked in his question. The core design has the flexibility of six closed-loop positions, each of which is about 3 in. in

diameter. These positions can also accept driver fuel if all six are not required at any given time for testing purposes. There are also six positions called open-loop test positions, which are simply driver positions that have the possibility of accommodating contact instrumentation on the fuel. The remainder of the core is driver fuel, but is instrumented to the point that it could be used in various ways for testing of fuel. For example, each of the driver locations has a design objective (and these have, I believe, every reason to be accomplished) of having not only thermocouples in the outlet of the sodium stream, but also flowmeters capable of at least determining serious blockage, and failed-fuel-element detection and location devices, for each one of the assemblies.

There have been various test-reactor flux levels talked about at this conference. For instance, I believe that the present core in Rapsodie was reported to have a flux of about 3×10^{15} n/cm²-sec, and the future Fortissimo core is expected to have about 4×10^{15} n/cm²-sec. The present design level of the FFTF flux to the test specimens is 8×10^{15} n/cm²-sec in the closed-loop positions.

The major problem in FFTF today is one which I think is also beginning to show up in some of the 1000-MWe design studies, namely the interrelationship of instrumentation to the refueling scheme and to the handling of test assemblies. This poses a significant problem to FFTF, and to a much lesser degree is present in the 1000-MWe studies. Dr. Nozawa did raise the question as to how one gets instrumentation to the fuel when one has rotating plugs. This problem of refueling a fast reactor is one which is giving us a great deal of difficulty today in the FFTF concept, and one which, at least in my opinion, has been somewhat superficially discussed in the 1000-MWe studies up to this point.

With regard to the last question that was raised, as to how one develops fuel for a test reactor which itself must then provide equivalent test capability to the test pieces, I think that it is a very fair question. It can only be answered from the standpoint of recognizing that the development of fuel for the test reactor itself, which is the most important single ingredient in that development, will be perhaps the most important contribution to the fast breeder program. This we will do to the very best of our ability, but with the understanding that in a test reactor the economics are not nearly so dependent upon fuel lifetime as they will be in the power plant. This is to say that if we can get 50,000 MW-days per tonne, we will. If we can get 30,000 MW-days per tonne, we still have a test reactor which is very useful for deter-

mining how to go to 50,000 or to 100,000 MW-days per tonne.

In this regard, we are trying very hard to get a good fuel for FFTF. It is interesting to note that in attempting to achieve a good fuel design for FFTF our design is very close to what has been talked about in the various oxide-fuel systems presented in the papers. For example, the present fuel-pin diameter for the FFTF design is 0.23 in., which is very close to those you have seen in these papers. The FFTF smeared fuel density is 0.85, and that also is the same as the numbers you have seen. The fuel length of 36 in. is very close to the kinds of cores described here. Thus in developing the FFTF fuel, we are attempting to set a pattern of development that will contribute in a major way to the fuel that will be used in the future fast breeder reactors, so far as we can see it today. I believe that the very first fuel going into the FFTF will be considerably superior to anything else which has been designed and built for fast reactors up to this point. It will at least be a good enough driver fuel that it will allow us to test the more advanced fuels to the degree of being able to use them with confidence in the fast breeders for the utility and industrial market.

Mr. Okrent: Are there other comments people wish to make on fuel test reactors?

Mr. Aiello (CNEN): I will not try to answer directly, but rather make some comments about the question in general on test-reactor fuel elements as reflected in our design of PEC. Some of the questions asked after my presentation, and other questions which are being asked now, probably can be answered by thinking about the question of compromise that one is forced to think about in technical situations.

I think that the first important question to consider is that of the interrelationship among the neutron flux, the total power, and the power density. These relationships are very simple and are physical in nature. If a high neutron flux is desired, a high power density and/or a high total power is required. If we can't have a very high total power, then we try to get a very high power density. But high power density means high performance from the fuel elements. Then the only way I see to reach a step-by-step evolution of the high-performance fuel element is to begin with a reactor in which the fuel element is subjected to less severe conditions, and in which some special and protected element is tested to increasingly more severe conditions. Then, step by step, we improve the reactor itself to arrive at the situation wherein large reactor fuel can be tested as a whole under the conditions and fluxes approximately equal

to that of the power station reactor. This is the first consideration.

The second consideration is in regard to closed loops. The closed loops are not the central point of fuel-testing reactors. They merely provide an easier and more efficient means for establishing a picture of the behavior of the fuel during the test. It is only when one has designed a prototype reactor that the test assembly can be proof-tested without great difficulty. Aside from the actual tests there are other considerations, such as the special provisions that a test reactor provides, namely, the capability for charging and discharging nonroutine test fuel, special alpha-tight seals, ability to handle the consequences of having oxide fuel in direct contact with the coolant, not to mention the fundamental advantage of accessibility for instrumentation.

Once one has provided these features, adding closed channels is not so difficult. Incidentally, closed channels have advantages, for example, the possibility of testing vented fuels with only a small volume of cover-gas blanket instead of contaminating the entire reactor gas blanket. This is important, especially during the initial tests when the knowledge of the behavior of such new fuel concepts is not well-developed.

Third and last is the fact that to test fuels one needs, in addition to the instrumentation capability and ease of charging and discharging experiments, the possibility of being able to test a full-scale power-station reactor core fuel assembly. Certainly it must be a bundle of 61 or at least not less than 37 pins. This means that the test reactor must house this type of subassembly. Tests in which only capsules, or a small bundle of three pins can be inserted without disturbing the total operation of the reactor are not enough.

Mr. Okrent: Do any of the Panel members wish to comment on the things we have just heard?

Mr. Denielou: May I just ask Mr. Kintner if he does not foresee any conflict or potential conflict between the necessity for testing the driver fuel quickly and the necessity for testing fuel inside the loops. The loops in themselves are not convenient for quick testing of the driver fuel. One could very well imagine that after running FFTF for two years without the loops, you may then find that you wish to install loops in order to perform a quick test on driver fuel. It may not be the policy now, but the policy could evolve in this direction.

Mr. Kintner: I think that you have raised a point that should be clarified. In FFTF the loops may or may not be included in the reactor when it first operates. Our intention is to design and develop the loops

so that they can be used if we want to use them. But the reactor can be operated initially without the test loops if so desired.

Another point that I think needs to be mentioned is that it is possible to provide a difference in the severity of the test of the driver fuel vs. the severity of the test to the closed-loop fuels. For example, in FFTF we expect to run the first core with a maximum outlet temperature of 900°F, but the test loops can be operated at much higher temperatures. This means that you can, at least from a temperature standpoint, produce a difference in severity of the test conditions in the closed loops from that in the open loops.

My last comment is in regard to the number of fuel pins that can be tested. The closed loops are sized so that at least 19 pins of any reasonable size can be accommodated in one test assembly.

Mr. Okrent: I would like to change the topic, if I may. A challenging question was raised by one member of the Panel, namely, are large water reactors so fast a moving target that the oxide-fuel LMFBRs will have trouble competing with them? I wonder if some of the proponents of oxide fuel for LMFBRs would care to comment.

Mr. Cohen (GE): I certainly agree that the development of the water reactors has reached and will continue to advance to the point that they are going to present a very difficult economic target to hit. At the time that we will be interested in introducing fast reactors, these large water reactors will have been built in quantity, will have established sources of supply, will have been built in large sizes, will be in volume production in their components, and the users will be very familiar with these reactors. Feedback from operating experience will produce continuous design refinements. We have already seen that the fuel lifetime in these plants is creeping up. I think that without any question they are going to be an extremely difficult economic target to meet. The costs which we have shown are perhaps not as good a situation as we would like to see, in that they show that there is close to a \$40 differential between a water plant and the fast breeder. This suggests that perhaps we should pay more attention to the capital costs. (Incidentally, the water-reactor cost which was quoted was for a pressurized water reactor.) However, if we can come in with no more than a \$30 or \$40 differential, and we can achieve fuel costs of the order of that which we are talking about over the lifetime of the plant, I believe that we can begin to develop an industry. Then the fast reactors will also begin to benefit from repeat orders, from volume production, and so forth. However, I think that the

economic problem is the principal problem which we face at the present time.

Mr. Harde (Interatom): I think that in talking about these two reactor systems it is not appropriate to compare them only on the basis of their total generating costs by using the same assumptions, since they differ so greatly in their cost structure. The breeders have rather large capital costs, which means high fixed costs, but have low variable costs. The opposite tends to hold for the light-water reactors. If you consider an economy which contains a large number of nuclear power-generating systems, then it is quite obvious that a combination of these two systems may in actuality generate electricity at lower costs than each would have produced separately. For instance, if you have 50% of your electric power coming from nuclear power plants operating on a certain annual load diagram, then you may find that adding fast breeders to the grid would be a real advantage, even though their generating costs, calculated on the same basis as a water reactor, are higher. The reason for this is that you have to add the possibilities of cost savings that stem from the fact that plutonium usage in fast breeders is, of course, much more efficient and economical than is the case with just recycling in the light-water reactors. What I am suggesting is that one has to keep in mind that comparison of the two systems on the same basis isn't really completely appropriate.

Mr. Okrent: Are there other comments in this area? If not, then let me pick up a question that was raised by a Panel member. Is there some basis for the choice of the design alternatives that the different designers have presented? Secondly, how does one expect that this current difference in approach will end? Will people merge upon one system, or will there continue to be several, as is the case with PWRs and BWRs? Will there be some such suitable equivalent in sodium?

Mr. Beckett (W): I would like to combine the answer to these questions with Dr. Nozawa's question on fuel handling as to why we like a hot-cell type of fuel handling. I think if one looks at the evolution of fuel-handling systems, the system we would like to see as the end design would be an under-the-plug system. This system would provide very rapid turn-around refueling time. But the thing which influenced us to go to a hot-cell has been expressed very well by Mr. Gibson. I will use a different set of words that mean the same thing. We like the hot cell because of its flexibility in the event of an unplanned incident or a malfunction. We can't use the Denielou fuel-handling machine in every incident—the rope and the knot. But we can get closer to a way of improvising with a combined hot-cell fuel-handling and main-

tenance system because they are inseparably combined to allow us to take everything out except the vessel, and perhaps even to solve some problems that we can't contemplate now. This is why we favor the hot cell.

An example of evolution and convergence can be seen in the possibility that in later models you might put an under-the-plug system in the hot cell. The main refueling would be done with the under-the-plug system, but it would still be possible to revert back to a hot-cell for a maintenance situation.

Mr. McCarthy (Detroit Edison): I would like to answer your question, Dr. Okrent, for two reasons, one because it needs an answer. Second, it will give me an opportunity to ask some other questions and address myself to the Panel in other areas.

GE and Westinghouse build their steam turbines quite differently, and this is true in many areas of technology all over the world. So I see no reason at all to expect that reactors will ever tend to be more than superficially similar. Some companies will like carbide fuels, some will like oxide fuels, and they will both find customers, unless there is really something basically wrong with one or the other.

Now I would like to comment on Dr. Denielou's comments about cost. As some of you know, I have recently become extremely close to a water-reactor design. In trying to evaluate it I asked some people to prepare a cost sheet for me. The first set of costs, which incidentally were part of one of the pieces of paper that I got from one of the tentacles of the AEC that wanted to know these numbers, was ridiculous. The costs were so low that we should have bought two reactors at that time. So I went back and asked about these numbers and was told, "Oh—you want the real cost?" It turned out that the costs differed by about 35%. This is what Harvey Wyckoff (Commonwealth Edison) was referring to earlier when he asked whether you include all the costs to your company, *all* design and construction costs, and that sort of thing. I feel that the numbers we saw this morning fit into the same bracket. I have no confidence, really, in almost any of these numbers.

Another question that I have is raised by Dr. Nozawa's presence. One month ago today I was in Tokyo, on the tenth floor of a building, when we suddenly had an earthquake having an intensity of 4 on the Richter scale. Things started moving around in a very remarkable way. Pictures swung out from the wall 5 in. and slapped back against the wall. The chandeliers all swung. This came about at a time when one of the purposes for which I was in Tokyo was to talk with people about seismic effects on reac-

tor designs. This was an example of Japanese hospitality, as a matter of fact.

The seismic question had been raised during the design of a plant which is being developed jointly by a number of people from Japan (The Central Research Institute for the Electric Power Industry), APDA, and the Detroit Edison Company. Essentially the question asked was: here is a fast reactor that is being designed for Japan; what should be in it that is different due to the seismic effects that they certainly have in Japan and which in the US are becoming a more and more important part of reactor design?

Therefore the question I have is really not for you specifically, Dr. Nozawa, although I would be very glad to hear what you have to say, but what is the relative position of fast reactor design *viz-a-viz* water-reactor design as far as seismic stability is concerned? Then, to branch out a little bit, how does the pot type compare with the loop type with regard to seismic effects? I am concerned when I see a 48-ft-high vessel with a core hung from the top and positioned in the middle of the vessel. I can just picture these components swinging 5 in. either way with this small an earthquake. It is an area to which I think some thought has to be given. The structures in fast reactors, because they are low-pressure structures, are generally not as strong if you compare weight or mass per unit length to strength as is the case with water reactors.

Dr. Wirtz, you asked about some way of getting away from the hypothetical word "hypothetical." I think that this has been done by calling it a "design basis" accident, and I think that that is probably the best thing to call it. It doesn't mean you believe it. It means that this is what you have designed for, and I think the term has some utility. It doesn't completely get away from the problem of semantics, but it is a help.

The last comment I wish to make is really an extension of Dr. Wirtz's remark in thanking Argonne for having this series of meetings. It is either the fourth or the fifth, and I have been to all except the one that followed closely after the Fermi fuel affair. I have enjoyed them all a great deal, and I think they have played a very large part in the development of fast reactor technology, very largely due to Dr. Okrent's meticulous Chairmanship of them.

Mr. Okrent: The floor is open for comments on seismic effects on safety of fast reactors. Do we have a volunteer from the Panel or from the floor?

Mr. Wolfe (GE): I would like to respond to some of the questions raised. Certainly seismic effects are considered in this country, and I don't think there is any great secret about how we take care of them.

First of all, we don't put reactors on the tenth floor, so the amplification is somewhat less than you saw in Japan. But specifically with the tank-type design, we have a core-support design which cantilevers the core from the top and provides lateral stabilizers that go through the sides of the tank, thus providing earthquake protection. This was adequate for a 0.3-g earthquake lateral force, which corresponded to our site requirements. There are other designs in which you can cantilever the core from the top when it appears that the tank is stiff enough to take the seismic accelerations without requiring stabilizers down below. I think that the problem is basically the same in the piping system. Having gone through a design very recently with small 10-in. pipes, also with a 0.3-g lateral seismic force criterion, I can say that the problem is not simple, but not impossible to solve. With 48-in. pipes, I suspect it would not be simple, but again not a complete mystery.

With regard to the refueling-cell question that Dr. Nozawa asked, I think that you will find that the GE design has already moved in the direction of more automation. In SEFOR we lower the level of the sodium for each refueling. For the 1000-MWe design, we don't normally lower the level of the sodium. Instead, we index the machine, and the machine goes down and picks up the fuel under sodium, and so is automated in that sense. Of course it is possible to add additional automation. I think the advantage of the cell is that in case something does go wrong, it permits you to lower the level; you may look and see what is happening, and then perhaps find out where to put the rope with the magic knot that Mr. Denielou spoke about.

Mr. Wirtz: I would like to say that if the answer to the alternatives, for instance pool *vs* loop and cell *vs* plug, is that they are both fine concepts, this is an answer. It is a good answer and tells us that there is no big difference. The matter of the alternative as to whether we should have double-wall or single-wall ducts is not the same, and I think we should come to a definite decision between those two possibilities. With respect to oxide and carbide fuel, I think that if we really have carbide fuels at hand, the carbide fuel is much better in a sodium reactor. Reaching the target of the water reactors would be much easier with carbide fuels. The only problem is that we don't have the same confidence and experience with carbides that we have with oxides.

Mr. Okrent: I am glad that you mentioned this pipe question. I wonder if there are members of the audience who would like to comment on the question of double-walled *vs* single-walled pipes, or modes of pipe failure, or other pertinent considerations.

Mr. Harde: I guess someone has to volunteer on this one. I agree with Mr. Denielou that, of course, single-walled vessels and pipes are easier and cheaper than double-wall. But since most people are gradually getting to the point of accepting the engineered safeguards concept, you certainly must do something about the loss-of-coolant accident. One means for doing this is to provide a double envelope. I do not agree with Mr. Denielou in that he said that sometimes that you end up with a situation for the double wall where you are even less safe. My feeling is that if you design and engineer it well, it is safer to have two walls than just one.

Double-walled structures are more complicated, and certainly we don't like it, but you should not forget that some advantages really go along with the double-wall feature. For instance, a credible incident with a pipe system or any system is that very small leakages of sodium can occur. These normally go unnoticed below the insulation of a pipe system. Now, if humidity, which to a certain extent can be assumed always to be present, gets to this leakage, sodium hydroxide will develop and, in turn, heavy corrosion may occur, even on a large fraction of the circumference. As a consequence of this, if the pipe happens to be under stress, one of those hypothetical, large-size, guillotine-type ruptures may occur.

The double-wall feature is good in that it allows you to detect small initial leakages, because you can detect very tiny amounts of sodium in the annulus by ventilating and checking for active sodium. Another advantage that goes along with this feature is the preheating situation with very large pipes. If you have electrical trace heating, severe problems may be encountered.

I would mention another problem that hasn't been discussed in THIS CONFERENCE. This is: if a small leak or an intermediate-sized leak, such as the shearing of a thermocouple well, occurs in a one-walled system, you don't get a maximum credible accident, but you do get quite a mess in your heat-transfer cells. This mess would also be hazardous because it would contain radioactive sodium and may contain quite a bit of fission products. If, however, you have a double wall that can be drained, your overall repair work may more than pay for the extra costs incurred with the double wall.

Mr. Denielou: May I make a comment? You know that on the Rapsodie-type reactor we have double-wall containment. We found that although double-wall containment is safer, it is also more difficult to engineer. The quality control is more difficult to achieve, too. This is why I made the remark that it is more difficult to achieve good engineering with a double wall.

In addition, you cannot exclude the rupture of both envelopes. We have not been able to exclude it. We had to make special syphon provisions because we could not exclude the possibility of breaking both envelopes. But, if one attempts to prevent failure of both envelopes, one must go to a strong envelope, and then you come into real engineering problems.

Mr. Harde: I agree it takes quite a bit of engineering care and inspection care during construction, and, of course, one has to learn this experience. Incidentally, we are doing that right now. In addition, I agree that it takes some experiments to demonstrate that conceivable modes of failure of the primary system do not necessarily lead to failure of the secondary system.

Mr. Charak (ANL): I would simply like to suggest that if the question of double-walled piping really becomes an important one, this may well tip the scales in favor of the pool concept since the pool concept is specifically designed for the purpose of containing small leaks.

Mr. Denielou: This might have been one of our main reasons for shifting from the loop concept to the pool concept.

Mr. Okrent: I wonder if I might ask a question that relates to fuel handling? As some of you may know, there has been an attempt to determine the feasibility of identifying or labeling each and every subassembly in a vented-to-plenum fuel element. Labeling each and every subassembly with its own label would be done so that if a failure occurs, you would know about it within some minutes and know which subassembly, or at least which of a few subassemblies, had failed. My question is: if such a system were perfected so that it was practical to use on a commercial reactor, and if vented-to-plenum fuel elements prove to have reasonable performance characteristics, would this combination of events move people toward rapid fuel-handling systems? Let me elaborate a bit. With this system when a fuel-element failure occurred, you would presumably know which subassembly it was that had failed. If you had a rapid fuel-handling system, you could go down on the next weekend, or whenever your low-load period occurred, quickly remove the offending subassembly and replace it with a new subassembly.

Mr. Denielou: I don't know exactly what you mean by labeling.

Mr. Okrent: Well, for example, one scheme that is being looked at is putting a different combination of xenon isotopes into the fuel elements of each subassembly.

Mr. Denielou: I understand. We are looking at this technique also, provided that the number of labels

can be fairly small. Statistically speaking, the importance of the first label is very great, because it prevents removal of half of the core. The last label which allows you to make the choice between the two last possible offenders is not very important. You can combine these xenon markers, by combining them in the same kind of detective attitude that the EBR-II speaker referred to. In short, you can assume that either the oldest fuel or the newest fuel is to be suspected. Generally speaking, you have no great chance of error. But my comment is, I don't know how this labeling system can work with vented fuel.

Mr. Okrent: That is why I specifically framed the question in terms of vented-to-plenum fuel. Again, the question is: if one could detect failure with confidence, would it provide sufficient incentive for rapid fuel handling that people would move in this direction?

Mr. Denielou: My answer is no.

Mr. Okrent: Are there any other opinions in this area?

Mr. Cobb (Nuclear Power Group, UK): Surely, the point is this. We already have delayed-neutron monitors which provide us with a means for quick detection of severe failure in the core. The only advantage of your tracer technique would be that when you have decided that it was severe enough to warrant full removal you could save time in actually locating it on the shutdown. On the PFR reactor in the UK we have a location system built into the reactor which actually takes a sodium sample from each subassembly in order to detect the zone of fission product activity, thus reducing shutdown time for location and removal of failed fuel.

Mr. Okrent: I agree, but I believe that yours is the only reactor now incorporating this feature.

Mr. Cobb: Yes. We do this in PFR by using a one-in-seven arrangement of the control rod to every six subassemblies. It may not be possible to engineer this on a commercial reactor.

Mr. Okrent: The impression I get is that there is really no decided preference to move to rapid fuel handling, but instead to use something slow and reliable.

There have been conflicting opinions from the Panel on instrumentation and how much one should have. I wonder if members of the audience would care to discuss the question "What is enough instrumentation?" Perhaps the General Electric group would care to state what they think would be adequate criteria for enough instrumentation to support the "advanced concept."

Mr. Murphy (GE): I think as a minimum we would require quick-acting flow instrumentation,

and as a backup a good thermocouple system on each channel. This would probably have to have a more rapid response and be more reliable than are present thermocouples, or else provide a great deal more redundancy. This would be in addition to the flux-monitoring equipment, the delayed-neutron detection system, and the cover-gas system.

Mr. Cushman (ANL): I have a question for GE in regard to the fast-response flow-detection equipment. How small a variation in flow must you detect in order to be sure that you don't have flow blockage in a small but significant portion of the core, but still small in terms of detection and in terms of flow measurement?

Mr. Murphy: A 5% variation in flow in one channel would probably be satisfactory, although in any one channel we could take a 50% drop in flow without requiring a scram, if you had no other reaction from the scram system. We could take 80% if you had a zero time-delay in the scram system. Thus a drop in flow in any one channel between 50% and 80% is covered by the reasonable range of reaction you would expect from your detection system. On that basis, the detection of a 5% change in flow level would certainly give you plenty of margin.

Mr. Cushman: But may you not already have failed an element at least, or several elements, in order to get this 5% flow reduction?

Mr. Murphy: Yes.

Mr. Wirtz: I wonder whether this is the full answer. My feeling is that we are still missing a comprehensive theory or analysis as to what information you need from your instruments or what results you want from that information. This means that you have to have a theory that combines one or several signals into the answer you are looking for. This may in one case be a 5% difference in flow-meter indication. But I could imagine that the real instrumentation for a fast core consists of information from several instruments. You need a fairly elaborate analysis or theory to evaluate this information and get the answer you need. I don't think that it is easy or even possible to say what is the minimum or maximum information that you need from any single instrument in order to get an answer. I think that the field is too complex for this. In this respect also I disagree a little with Mr. Denielou's position. I think that thus far we do not have at hand a good analysis or a good theory as to how to instrument fast cores in order to get the information you need.

Mr. Okrent: I would like to read a question that was given to me which relates to this. Perhaps you can incorporate a comment on this question while you are responding to Professor Wirtz.

“It is obvious that to be economically competitive a large LMFBR will have to be capable of operating with at least a small fraction of its fuel elements leaking fission products to the coolant. In your opinion, how important is it, and how practical is it, to develop a capability for sensing and evaluating, during operation, the nature, extent, and rate of change of the existing fuel-element damage so as to be able to know that excessive propagation of fuel-element damage is not imminent?”

Mr. Murphy: Well, first of all we agree with Professor Wirtz that there is a problem and there is work to be done if the reference plant that we were talking about is to be committed in the 1980 period. What we are discussing here are the problems that stand between now and then.

With regard to the question, I think that it should be put the other way around. You have to know when it is imminent, and the detection equipment then has to rely on you getting the information that the failed fuel is not just a pin-hole leak, but is rather some structural damage in the cladding which can cause debris to form which then gives a mechanism for causing the damage to propagate. As I noted this morning, this mechanism and the way in which you detect it are the most difficult problems that we have. The signals the equipment gives are not unequivocal. This is an area where much more work will have to be done.

Mr. Wolfe: May I add something to what has been said? I think there are two approaches that have been stated at this Conference. One is, essentially, we will assume that things just void and then we will try to protect the world from this. As I indicated, I think that when people take this approach seriously it is quite a challenge. The other approach is the approach that was stated this morning. I think that what this does is narrow down the problem.

Let's start by saying, first, that with even very simple instrumentation you can eliminate concern about a complete flow blockage. Thus you don't have to assume a case for complete flow blockage. You narrow it down to the point where you start being concerned about rather strange mechanisms that you can dream about but which intuitively you don't think are really probable. Then you can start thinking about experiments which will ultimately allow you to exclude them completely later on.

For instance, let's take the case of fission gas release from the fuel pin and the formation of a bubble which propagates to other assemblies. If you eliminate the hypothetical or *a priori* assumption that the channel is going to plug from unknown means, then you can start working on the mechanisms that might

have a real bearing on the fuel design. Once you know the mechanism, you can definitely determine the need for venting or some other means for reduction of internal pressure. In this manner, then, you learn and develop additional information.

Let me say a few words about vented fuel. The venting that we are talking about, Dr. Wirtz, assumes that the fission gas will be held up for days, so that there will be a different fission gas released from an element which is venting normally and from an element which has failed. One can pick up this signal, namely, detection of the short-lived isotopes in the cover gas, rather quickly. Thus venting does not necessarily preclude finding failed fuel rapidly.

Dr. Okrent, I can't help answering your question as to whether we would refuel as quickly as we could with our present design, regardless of whether we have labels or not. I think we share the view that reliability is the main point. I think that everyone is trying to refuel as quickly as possible.

Mr. Okrent: Are there any further comments? If not, the floor is open for any topic. Mr. Fauske, did you have a question on sodium boiling?

Mr. Fauske (ANL): I don't really have a question. I would like to express my appreciation to Professor Smidt (Karlsruhe), who was kind enough to review the working meeting on sodium boiling held at Ispra. I am also sorry to say that we couldn't attend the meeting.

However, there were two points that he made at the review of this meeting that I would like to bring up at this time. One is with regard to the pressure-temperature history effect on superheat. I want to point out that in the United States extensive data have been taken that show that this temperature-pressure history agrees in the area where Holtz's model does apply to this problem. The problem of pressure-temperature history has not only been observed in liquid metals, but has been clearly shown in water. It is a well-known fact that if you pressurize a system to high pressure before you start the heating period, you can increase the incipient superheat. Second, with regard to the expulsion model that has been adopted in Europe, I think I agree that the main mechanism is a slug-type expulsion. But, I think there is a second stage to the slug-type expulsion: the two-phase mechanism that occurs at the lower interface of the vapor slug.

Mr. Smidt (Karlsruhe): I think it will be necessary to review the experimental results concerning this particular question before we can discuss it. With respect to the general problem of boiling in fast reactors, I have noted in this meeting that the importance of the void effect on fast reactors is slowly

decreasing. I think that during the next one or two years the experimental evidence will show even more clearly that the question of superheat really is not as important to fast reactors as was once thought. I think that from the practical side, no great amount of superheat will be found. We don't know why yet, but we know that it is so. I think that from the practical standpoint this is enough. Probably these cases of the magic number of \$60 per second may be changed also.

Mr. Wirtz: Being from the same laboratory, I would like very much to agree with my colleague, but I think that it is very easy to get 400°C superheat. I think the question is, in a way, open.

Mr. Fauske: I would like to summarize by saying that I think most of the people in the audience be-

lieve that there is not a problem of superheat under reactor conditions. I tend to agree for the following reasons, namely, gases exiting from solution due to temperature and pressure changes, entrainment of gases, and the possibly ideal condition for entrapping gases due to spacers. But I would like to say that this may be wishful thinking. We really don't have scientific evidence to date that there isn't really a large superheat.

Mr. Smidt: I think that I agree with Dr. Fauske that there is evidence from the measurements to support this opinion, and I think that maybe sometime I will be able to convince even my colleague, Dr. Wirtz.

Mr. Okrent: Thank you, gentlemen, for coming. Bon voyage.

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