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Proceedings of the

National Topical Meeting on Fast Reactors Systems, Materials and Components

April 2-4, 1968 *Cincinnati, Ohio*

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REACTOR TECHNOLOGY (TID-4500)

PROCEEDINGS OF THE NATIONAL TOPICAL MEETING ON FAST REACTOR SYSTEMS, MATERIALS AND COMPONENTS

Cincinnati, Ohio
April 2-4, 1968

Edited By

W.E. Niemuth
and
J.F. Weissenberg

A M E R I C A N   N U C L E A R   S O C I E T Y

SOUTHWESTERN OHIO SECTION

PREFACE

This volume is a compilation of technical papers and discussion of these papers presented at the April, 1968 National Topical Meeting on Fast Reactor Systems, Materials and Components. This meeting was hosted by the Southwestern Ohio Section of the American Nuclear Society.

Five session topics were identified by the program committee for this meeting:

1. Applications for Fast Reactor Systems
2. System Requirements for Fast Reactors
3. Materials for Fast Reactors
4. Components for Fast Reactor Systems
5. Panel Discussion: Review of Problem Areas

Dr. F.G. Foote did not submit a paper for publication, and it has been omitted from this volume. Three additional presentations have been omitted from this volume; they are:

Dr. Hoke S. Greene, Vice President of Research, University of Cincinnati, presented comments at the opening of the meeting regarding the recent changes in the selective service deferment regulations for graduate school students and the effects of these changes on both graduate schools and scientific and engineering programs such as the LMFBR.

The Honorable William M. McCulloch, Representative to Congress, 4th District of Ohio and Member, Joint Committee on Atomic Energy, delivered the banquet address. This address has been printed in Nuclear News, Vol. 11, No. 5, May, 1968, and is not included in this volume.

Walter J. McCarthy, Assistant General Manager, Power Reactor Development Company delivered a luncheon address entitled "The Slow Progress of Fast Breeders," and at his request, his comments are not included in this volume.

The cooperation of the AEC, Division of Technical Information Extension (DTIE), in particular James D. Cape, Assistant Extension Manager for Science Communication, in publishing the proceedings of the conference is gratefully acknowledged.

Our sincere thanks also to Mrs. Eva J. Weber, secretary of General Electric Nuclear Systems Program Reactor Engineering, for her assistance in many details prior to and during the meeting and for transcribing the discussions.

Special appreciation is due to Dr. Robert VanHouten and Charles S. Robertson, Jr. who served as Meeting Chairman and Program Chairman, respectively, and to the session chairmen, local coordinators, authors and participants who provided the information compiled in this volume.

The authors were responsible for editing their individual papers, and we edited the discussion without obtaining approval of the participants. As editors, we attempted to preserve the informal atmosphere of the sessions, however, in some instances editorial changes were made for clarity or to remove comments of minor importance. To those attendees and participants who find their comments deleted, shortened, unintentionally changed in meaning, or could have been expressed more clearly, we offer our apologies for our editorial changes.

W.E. Niemuth
J.F. Weissenberg

Editors


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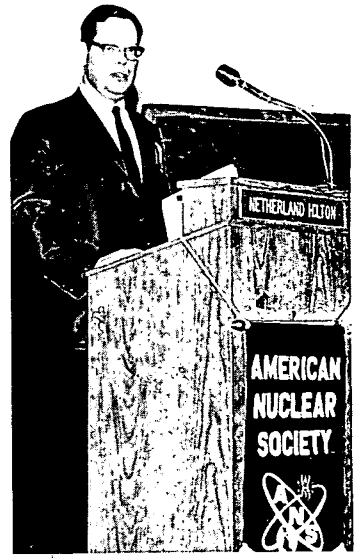
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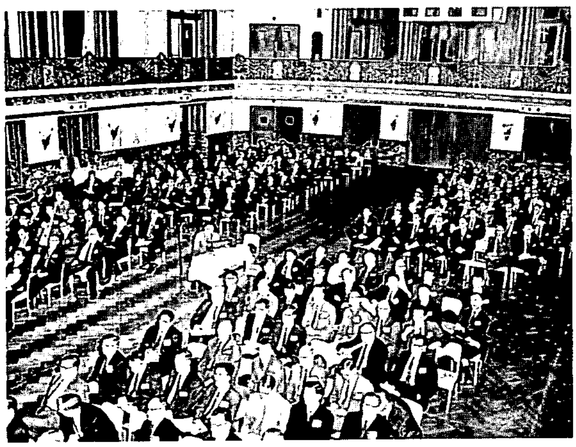


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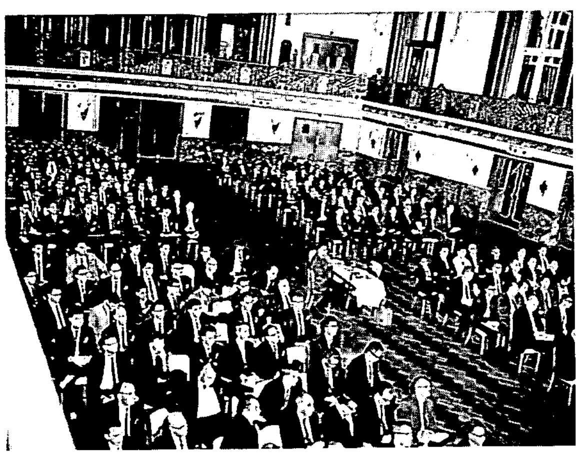


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SESSION I

April 2, 1968

APPLICATIONS FOR FAST REACTOR SYSTEMS

Chairman: Edward C. Pandorf
Cincinnati Gas and Electric Company

Local Co-ordinator: Arthur L. Nagy
Cincinnati Gas and Electric Company

THE OBJECTIVES AND STATUS OF THE AEC-SPONSORED
1000 MWe LMFBR FOLLOW-ON STUDIES

L.W. Fromm, J.P. Burelbach, D.E. Lutz, and D.H. Thompson

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Argonne, Illinois

The AEC program for Liquid Metal Fast Breeder Reactor (LMFBR) development is based upon a division of responsibility between government and industry. The government's responsibility is to develop the technology to support the design, construction and successful operation of practical LMFBR power plants, whereas industry's responsibility is to construct, initially, demonstration plants (with some government supports), and later the commercial plants which are the "target" plants of the LMFBR development program. These commercial plants must be safe, economic, and competitive with fossil and thermal reactor power plants of the period.

One of the first steps required in implementing this policy is to define the technology to be developed, through a series of studies of potential "target" plants. This is one of the main goals of the AEC LMFBR Plant Study program. The specific objectives of this program are:

1. To develop an understanding of the over-all technical and economic aspects of large LMFBR plants.
2. To define functional requirements for plant systems and components.
3. To identify research and development requirements and to assess their relative value to the over-all program.
4. To provide bases for meaningful comparisons of alternative design approaches.
5. To increase the national capability for design, construction and operation of LMFBR plants, by promoting industrial participation in the development program.

The LMFBR Plant Study program is a series of investigations of "target" plants, initially broad in scope, but later narrowing to specific problem areas requiring detailed attention. The 1000 MWe LMFBR Follow-on Studies represent the initial, broad phase of this program.

The history of the Follow-on Study program goes back to an early group of investigations[1] conducted in 1963-64 by four contractors - Allis Chalmers (with participation by Atomic Power Development Associates and the Babcock and Wilcox Company), Combustion Engineering, Inc., General Electric Company, and Westinghouse Electric Corporation. The objective of these studies was to obtain conceptual designs for a sodium cooled fast breeder reactor suitable for a 1000 MWe power plant. The studies were limited to the core, including geometry, physics and thermal characteristics, and materials; the reactor vessel; core instrumentation; control and safety systems; and fuel handling mechanisms. The studies resulted in the definition of four quite different reactor cores, all of which were considered feasible for the application. The diversity of designs indicated that a number of approaches might be used in reaching the eventual design goal.

The current studies are a "follow-on" of the earlier investigations, with the scopes of work expanded to encompass the total plant concept, and with the objectives redirected to emphasize research and development aspects. However, the expanded studies need not necessarily be based upon the cores developed in the earlier work. The primary objectives of the program all concern identification of research and development programs needed, and assessment of their relative value. The objectives are met through development of over-all plant design concepts, optimized through detailed trade-off evaluations and parametric studies.

The specific primary objectives of the Follow-on Studies are:

1. To identify new information needed beyond current technology to permit successful design, construction and operation of the industry-favored reference conceptual designs.
2. To formulate a research and development program required beyond existing programs (including redirection of existing programs where necessary) to obtain the new information so identified.
3. To estimate the probability of successful achievement in each of the areas comprising the research and development program so formulated, and to assess the effect on the various elements of power cost of non-attainment, or only partial attainment of the goals in each area.
4. To assess the relative value of performing the specified research and development in each area, in terms of savings attainable in the various elements of power cost in comparison with research and development costs.

The secondary objectives are:

1. To encourage the development of original approaches to the design of large-scale LMFBR power plants.
2. To gain a better understanding of safety problems attendant in the design and operation of such plants.
3. To supply industry with up-to-date estimates of future LMFBR system functional requirements which will dictate the design of components and specification of materials for such plants.
4. To provide AEC with information to aid in the continuing formulation of the National LMFBR Program Plan.

In the Follow-on Study program currently in progress, five industrial contractors are involved, all under subcontract to Argonne National Laboratory. Combustion Engineering, Inc., General Electric Company, and Westinghouse Electric Corporation are participating as in the earlier studies. The Babcock and Wilcox Company has entered to replace Allis Chalmers, and Atomics International has also been added. All of the subcontracts are based upon the same basic scope of work, but variations exist in the breadth of the studies because of variations in funding allocations made to the different contractors. The contracts include four major tasks, as follows:

<u>Task I</u> covers the selection by the contractor of a basic reference design concept. After performing preliminary trade-off studies and analysis, the contractor selects a "company-favored" concept of a plant which the contractor believes can be marketed commercially in the period 1975 to 1985, and be in commercial operation in 1980 to 1990.

<u>Task II</u> covers the more detailed engineering of the reference concept. In this task the contractor conducts engineering studies on the concept using a system-oriented approach. He then prepares a detailed description of the plant, including design requirements for each of the principal systems, using the AEC-defined "Conceptual System Design Description" format.

<u>Task III</u> covers the detailed evaluation and optimization of the reference concept. This is done by performing detailed trade-off evaluations on various plant features, conducting parametric studies within the limits of the design concept, and optimizing the concept in accord with the results of the studies. Capital and operating cost estimates are then prepared for the optimized concept.

<u>Task IV</u> covers the delineation of the research and development needed to bring the optimized reference concept to commercial fruition. As indicated above in the primary objectives of the program, the new information needed is first identified, the research and development programs to obtain that information are delineated, the probability of success for each program is estimated, and the effect of non-attainment or only partial attainment of each research and development objective is assessed in terms of effects on the reference design and the cost of energy produced by the plant.

The detailed trade-off studies and parametric studies conducted by the contractors as part of study Task III are listed in Tables 1 and 2.

The original plan for conducting the follow-on studies called for concurrent studies by all of the contractors. However, this plan was upset by varying difficulties in contract negotiation, and as a result the five studies began at various times between October, 1966 and June, 1967. The Task I work has now been completed by all of the contractors, and Task I reports have been received from Babcock and Wilcox and from Combustion Engineering. The reports from the remaining contractors are expected in the next few months. Task II and III work by Babcock and Wilcox and Combustion Engineering should be completed by the end of June, with Atomics International, General Electric and Westinghouse completing their work on these tasks by Fall or early Winter of 1968. Task IV work should be completed by the contractors a few months after Task III. The task reports by the contractors will be issued publicly, with the reports on each task being released simultaneously after completion of all work on that task.

The role of Argonne National Laboratory in the Follow-on Study program, and in the series of more definitive studies which will follow as part of the LMFBR Plant Studies program, is one of contract management, technical review and evaluation. A final evaluation report on the Follow-on Study program will be prepared by Argonne after all work by the contractors has been concluded. It is expected at this time that this report will be ready for public release early in the Fall of 1969.

TABLE 1

Trade-off Studies

Primary System Configuration: "Pool" or "pot" type vs. "loop" or "piped" type system.

Refueling System Configuration: Hot cell vs. under-the-plug or through-the-plug system.

Containment Principle: Entire building vs. hot cell type.

Number of Parallel Primary and Secondary Heat Transfer Loops.

Fuel Material Flexibility: Reactor designed for exclusive use of a single type of fuel (e.g. oxide or carbide) vs. reactor designed for initial use of one type with a later change to another type.

Spent Fuel Shipping Cask Cooling Medium: Liquid metal vs. air or other non-liquid metal.

Fuel Reprocessing and Fabrication Location: On-site vs. off-site.

Core Type: Modular vs. non-modular.

Reactor Safety Criteria: Design of plant to meet existing criteria vs. design to meet contractor's expectation of criteria which will prevail in the 1980's.

Fuel Venting: Vented vs. non-vented fuel.

Steam Generator and Superheater Type.

Steam Cycle Principle: Non-reheat cycle vs. reheat cycle, and for the latter case, use of sodium vs. other media for reheating.

TABLE 2

Parametric Studies

Thermal-Hydraulic and Cycle Parameters

- Bulk sodium outlet temperature.
- Sodium temperature rise through core.
- Fuel specific power.
- Maximum fuel and clad temperatures.
- Intermediate heat exchanger and steam generator temperature differences.
- Turbine inlet steam pressure and temperature.

Core Parameters

- Geometry.
- Fuel properties - density, thermal conductivity, diluents, bond, clad material and thickness.
- Fissile and fertile isotope prices.
- Reprocessing and fabrication costs.
- Doppler coefficient.
- Sodium void coefficient safety accommodation.
- Refueling interval.
- Doubling time.

Near the conclusion of the 1000 MWe Follow-on Studies, the LMFBR Plant Studies program will continue with additional definitive investigations, largely design-oriented and based upon the reference concepts developed by the contractors under the Follow-on Studies. The specific studies to be performed are listed in Table 3, in priority groups. Several points regarding the highest priority group of studies are given below.

Plant Safety Analysis

Under these studies, to be initiated in June or July of 1968, detailed safety analyses will be performed on two or three of the Follow-on contractors' reference concepts. The analyses will be similar to those normally carried out by reactor manufacturers in connection with an application for a construction permit, except that they may be limited in some areas by the depth to which the reference concept design has been carried. Preliminary Safety Analysis Reports will be written on the concepts and will be submitted to the AEC Division of Reactor Licensing, and to the Advisory Committee on Reactor Safeguards (ACRS), for comment. The objectives for this study are to provide a better understanding of the effects of safety considerations on plant design, to provide a vehicle for communication between industry and the regulatory agencies to identify problems in licensing such plants and plan means of resolving those problems, and to permit further definition of research and development requirements.

Detailed Pool-Type Primary Systems Study

This is an in-depth engineering study of the "pool" or "pot" type primary system concept, and will include such areas of detail as the design and analysis of structural features, methods of support, thermal distortion effects, dynamic forces, vibration, and seismic problems. The first stage of this study, a survey of current state-of-the-art, has already been performed by the Liquid Metals Engineering Center (LMEC).

Detailed Loop-Type Primary System Study

This study will be identical to that mentioned immediately above, but will consider the "loop" or "piped" type primary system concept.

Refueling Systems

This will consist of three separate studies on the hot-cell, under-the-plug, and through-the-plug refueling system concepts. In-depth studies will be made to explore the problems to be anticipated in detailed design and operation of these systems.

Dynamic Analysis and Control

This is a detailed study to determine the dynamic behavior of two of the Follow-on Study reference design concepts under steady state and transient conditions, initially under normal operation and subsequently under abnormal conditions. Failure modes and effects on design and operation will be determined. Various control methods will also be investigated. The study will include analytical methods development where necessary, and is obviously closely related to the Plant Safety Analysis Studies described above.

The schedule for carrying out these studies is not clear at the present time because of uncertainties in funding availability. However, the goal is to complete

TABLE 3

Additional Definitive Plant Studies

First Priority

Plant safety analysis.
"Pool" type primary system.
"Loop" type primary system.
Refueling systems.
Dynamic analysis and control.

Second Priority

Plant design temperature.
Effects of vented fuel on plant design.

Third Priority

Engineered safeguards: containment and shielding.
Plant maintainability.

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most of the studies in time that their results will assist the designers of the demonstration plants expected to be committed in 1970. As stated above, the Plant Safety Analysis Studies will begin in June or July of 1968. Other high priority items will probably be initiated shortly thereafter, with lower priority studies delayed until one year later.

Reference

1. L. Link, et. al., (ed.), An Evaluation of Four Design Studies of a 1000 MWe Ceramic Fueled Fast Breeder Reactor, USAEC Report COO-279, Reactor Engineering Division, Chicago Operations Office, U.S. Atomic Energy Commission, December 1, 1964.

INCENTIVES FOR DEVELOPMENT OF FAST BREEDER REACTOR POWER PLANTS

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Abstract

The electric power industry is expected to grow at about the same rate as it has during the past 30 to 40 years. By the end of this century perhaps two billion kilowatts of capability will be installed, about one-half of which will be nuclear. Power companies will generally select that type of generation which results in the lowest cost.

The growth and economic potential of thermal reactor concepts presents strong competition to the fast breeder reactor. If nuclear power, however, is to assume an important position in the future generation of electric energy, it appears necessary to develop an economic fast breeder reactor. This concept has the potential of reduced power generation costs, will conserve low cost uranium, provide a premium market for plutonium, and can utilize the large stockpile of depleted uranium.

Most peoples of the world have at least one common objective -- that being a desire to improve standards of living for themselves, their families, and their countries. To this end, we have developed a technologically-oriented civilization willing to risk time, effort and capital in fostering the development of new ways and ideas to benefit mankind. By encouragement of such initiative, we are finding new and better methods of utilizing electric energy to produce the materials and services we want and need.

INCREASING REQUIREMENTS FOR ELECTRIC POWER

Seven years ago the Edison Electric Institute made a forecast of electric generation in this country. This has been checked annually and thus far the resulting error of less than one percent has produced confidence in their forecast. Their estimate indicates that by the year 2000 the electricity generated in this country would be between six and ten trillion kilowatthours a year as compared to about one and a quarter trillion kilowatthours in 1967. To provide this quantity of energy, the electric power industry would need between 1.8 and 2 billion kilowatts of power producing capability, about seven times that installed at the end of 1967. In view of this, we have become keenly conscious of our energy needs and the resources available to us.

Figure 1 shows the U.S. electric utility industry installed capability between 1939 and 1968, and a projection of future capability through year 2000. Also shown on this figure are the present and projected nuclear capability, attaining a level of between 700,000 and 1,000,000 megawatts by year 2000. The nuclear power growth forecast up to year 1980 was based on the present U.S. AEC prediction of between 120,000 and 170,000 megawatts with the best single estimate being 150,000 megawatts. Between 1980 and year 2000, the AEC forecast was used as the lower estimate, and the quantity developed by a fast breeder reactor study group of the Edison Electric Institute was used for the higher number of approximately 1,000,000 megawatts. Certainly, it is difficult to reliably predict that far into the future. However, it is generally agreed that by the turn of the century approximately one-half of the installed capability will be nuclear, and one-half of the electric power generated in the United States will be produced from nuclear power plants.

UTILITY CAPACITY EXPANSION CONSIDERATIONS

The need for additional generating capacity on the system of an electric power company is indicated by the predictions of a load forecast committee, or some comparable group. The decision is then made as to what type of generating equipment should be installed to meet the predicted load demand. There are basically three types available -- base load, intermediate efficiency and peaking equipment. It is generally agreed that a complex including all types is necessary to provide the economics, flexibility and reliability needed in operating a large utility system. There are many factors to be considered when determining the type of capacity to be added. Some of these are the percentage of each type presently on the system, the percentage of reserve capacity -- both at present and after the addition, the extent of interconnections with other systems, the lead time necessary for design, purchase and construction of the equipment, the effect on system stability and reliability, and above all the economic effect of this addition as related to system operation. The present economic advantage of nuclear plants can only be realized if the decision is made to install base load generating equipment.

The cost of power from any generating equipment, regardless of type and whether it is nuclear or conventional, is determined by four main factors: (1) plant investment; (2) carrying charges, which include return on the investment, depreciation, taxes (Federal, state and local), and property insurance; (3) operating, maintenance and nuclear insurance expenses; and (4) capacity factor on the equipment.

An electric power company will endeavor to select the type of generating equipment which results in the lowest overall costs, providing it meets system requirements, assures a service of high quality and reliability to its customers, produces a fair return to its shareholders, and does not create a public nuisance or health hazard. A possible exception to the first requirement, that of lowest cost, is the installation of equipment for research and development purposes or to otherwise advance the state of an art.

The scope of this paper precludes a detailed discussion of each of the cost components and their effect on the total cost of power generation. A few remarks, however, on accual capacity factors and availability appear appropriate.

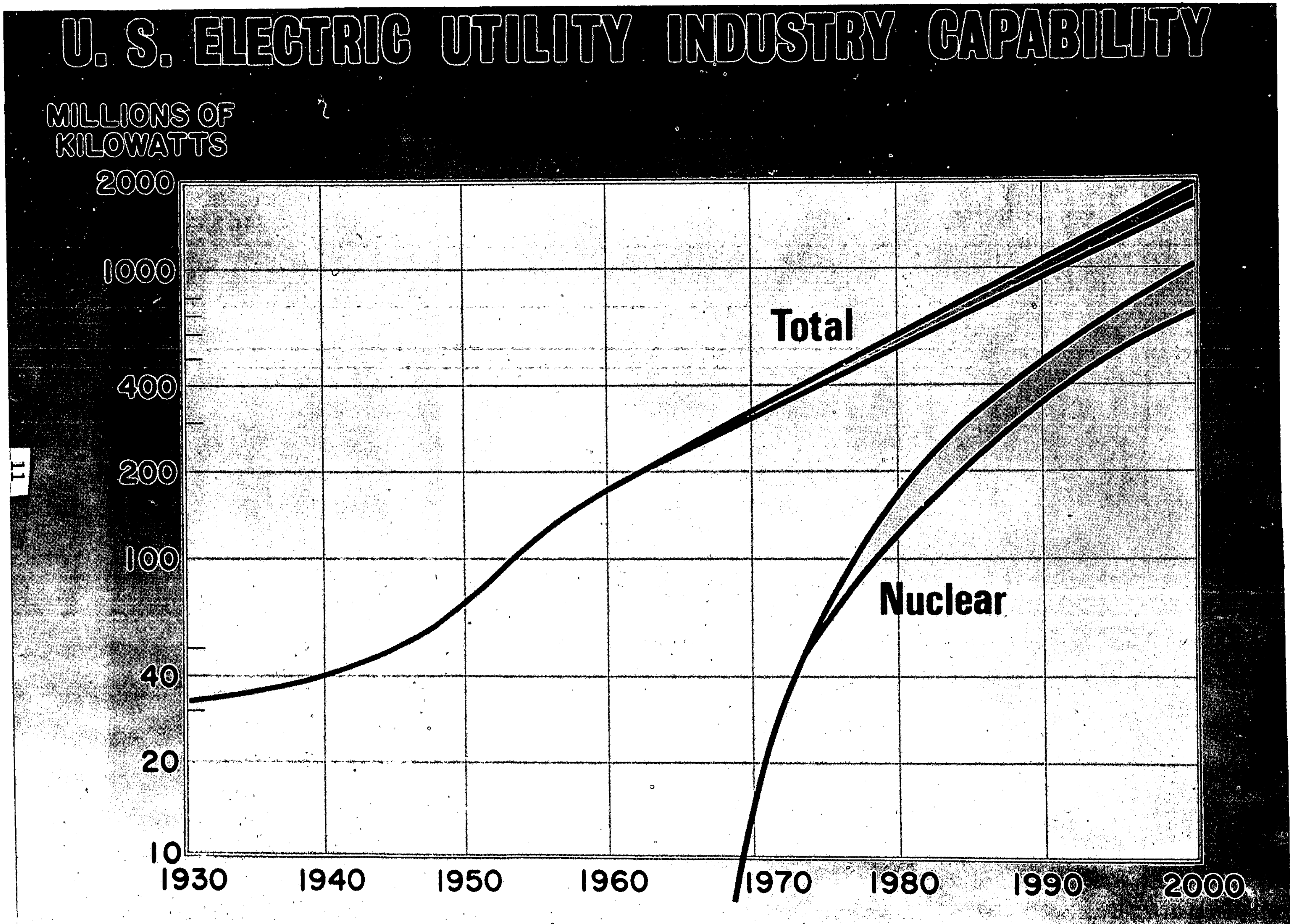

U. S. ELECTRIC UTILITY INDUSTRY CAPABILITY
MILLIONS OF KILOWATTS
2000
1000
400
200
100
40
20
10
1930
1940
1950
1960
1970
1980
1990
2000
Total
Nuclear

FIG. 1

ANNUAL CAPACITY FACTOR -- AVAILABILITY

Capacity factor can be defined as the ratio of the average load on a unit for the period of time under study to the capacity rating of the unit. It is expected that a new, low heat rate, base load unit would have a high capacity factor during its first several years of operation, restricted primarily because of its availability and the operational characteristics of the system. Annual availability is the ratio of the hours per year that the unit is available to produce full load to the total hours per year. Subsequently, as more efficient units are installed on the system, the capacity factor of the original unit will be reduced. Over a 20 year period, it is expected that the capacity factor of a unit will reduce from about 82 to 50 percent. In actual practice a unit may have a capacity factor considerably greater than these percentages depending upon the total number of unit outages, reserve margin, economics and spinning reserve philosophy of a system. Units 30 years and older may have capacity factors ranging from 20 to 40 percent. Hence, equipment originally installed for base load operation may in later years be called upon to operate for load regulation and at reduced capacity factors.

Some nuclear plants, I understand, have been evaluated and selected based on life-time capacity factors of 85 and 90 percent. This does not appear to be justifiable since, again, subsequent improved units will be preferentially loaded on an economic dispatch basis. It does appear reasonable, however, to assume that nuclear plants will enjoy higher capacity factors than conventional plants throughout most of their lives or until nuclear is the dominate means of power generation on a system. This is explained by the considerably lower incremental cost of power generation from a nuclear plant than from a conventional plant and thus the incentive to operate a nuclear plant at maximum load.

In view of the need for load following characteristics, it appears that nuclear plants, regardless of type, should be designed for an instantaneous step-wise change of perhaps ± 10 percent and a rate of change of ± 10 percent per minute. These changes should apply across the operating load range. The load following requirements of a nuclear unit as it approaches the end of core life are dependent upon many factors, such as the size of the system, the portion of the system which is nuclear, and the extent of interconnections with others. The extent and effect of load following restrictions imposed by Xenon build-up in thermal reactors must be evaluated for each reactor design and for each application. Fortunately, this is one problem not encountered in a fast reactor. There may be cases in which an economic penalty is justifiable in order to attain unrestricted load following capability.

The actual availability that can be achieved by a commercial nuclear plant is still unknown. Nuclear plants with significant operating histories are small in comparison to units now under construction or on order and are generally first of a kind. Their availability records from the time of initial operation to the present are not comparable to what could be expected from conventional plants. On the other hand, for certain time periods they have demonstrated far better availabilities than for many fossil-fueled units.

The availabilities of these developmental plants have been penalized by the quest for research and development knowledge, the deficiencies in refueling designs, the operation and maintenance educational procedures, the lack of need to dovetail refueling and maintenance outages with system requirements, and other factors peculiar to this type of unit. Through technological design and operating improvements, future nuclear units are expected to demonstrate dependability, flexibility and availability comparable to modern fossil-fired units.

UPSURGE OF NUCLEAR POWER

The growth, technological development and acceptance of nuclear power as a commercial means of power generation have surpassed even the most optimistic predictions of a few years ago. In 1965 about 20 percent of the generating capacity purchased by investor-owned power companies in the U.S. was nuclear. Nuclear plants accounted for about 50 percent of the new generating capacity ordered in both 1966 and 1967.

The purchase of these light water-cooled and moderated nuclear power plants by power companies during the past two years can be explained on the basis of many reasons. The fact of greatest importance is the economics of nuclear power generation. Figure 2 presents a cost comparison of a coal and a nuclear power plant. Shown are the major items of a plant's capital costs resulting in a total unit cost for coal of $134 per kilowatt and $148 per kilowatt for nuclear. The power generation costs, assuming about an 11 percent fixed charge rate, and a capacity factor that varies from 80 percent in the first year down to 58 percent in the twentieth year, are approximately equal. It should be mentioned that the fuel component for the coal plant in this example was assumed to be about 22 cents per million Btu in a supercritical unit. This does not necessarily represent today's economic stand-off energy value as escalation, productivity, shop loads, and other factors have a great influence on the component costs of power generation.

Another important factor favoring the installation of a nuclear unit is the ever-increasing political, social and health pressures and regulations regarding air pollution in our highly industrialized country, which represent serious problems to coal-fired installations. Certainly this is a challenging situation facing our industries. At the present time there are over 600 organizations and groups in the United States who are studying and analyzing the seriousness and effects of polluting our rivers, lakes and atmosphere.

Although this is a condition which is difficult to express in an economic evaluation comparing nuclear versus coal, it nevertheless must be considered from a public relations and health viewpoint. Utilities, as well as other industries, are spending huge sums of money to minimize stack effluents resulting from the combustion of fossil fuels. The use of nuclear power appears to be one method of controlling and, in the future, materially reducing this problem.

As power systems become more and more interconnected and as a particular system continues to grow, larger generating units can be installed without reducing system reliability or greatly increasing reserve margins. Through the efforts of the nuclear industry, the capital cost of large reactor plants has been appreciably reduced. This improved first cost position coupled with the potentially low fuel cycle cost has enabled nuclear power to be competitive with other means of power generation in many parts of the country. Other factors, such as the more stringent restrictions being imposed to reduce air pollution, the problems associated with handling millions of tons of coal and ash each year, and even the desire to "get on the band-wagon" have certainly played a part in the upsurge of orders for nuclear plants.

The light water reactor concepts still have considerable potential for economic improvements. Capital cost decreases on the order of 10 percent in the next 15 to 20 years appear achievable through volume experience, standardization, optimization of core parameters resulting in smaller systems and components, and increased unit sizes. Likewise, a 30 to 40 percent reduction in fuel cycle cost is

COST COMPARISON-COAL VS. NUCLEAR

PLANT CAPITAL COST - $/KW	COAL	NUCLEAR
STEAM SUPPLY SYSTEM + T.G.	38	61
BALANCE OF PLANT	49	43
SITE WORK	16	11
ESCALATION, CONTINGENCY, ETC.	15	13
INDIRECT CHARGES	16	20
TOTAL	134	148

POWER GENERATION COST - MILLS/KWHR	COAL	NUCLEAR
FIXED CHARGES ON PLANT	2.39	2.66
OPERATION, MAINTENANCE + INS.	0.23	0.45
FUEL	2.14	1.70
TOTAL	4.76	4.81

FIG. 2

predicted as a result of improved fuel performance and reduced unit operating costs. It, therefore, appears that the continually improving economic position of light water reactors presents strong competition to the fast breeder reactor.

In view of this favorable prediction for light water reactors, is there an interest and are there sufficient incentives to develop fast breeder reactors?

INTEREST IN FAST BREEDER REACTORS

A year after the 1954 Atomic Energy Act Amendment, a group of investor-owned power companies submitted a proposal to the Atomic Energy Commission for the construction of a liquid metal cooled, fast breeder demonstration reactor. This was done in the belief that only the fast breeder reactor could provide a high thermodynamic efficiency, an efficient fuel conversion cycle, a reasonable capital and operating cost, and safety in operation. At that time, there were only a few people and organizations actively interested in the development of fast breeder reactors. Today, virtually everyone associated with the nuclear industry agrees that, if nuclear power is to play an important part in the generation of electric energy in the latter part of this century and into the next, it appears necessary to develop an economic, high performance fast breeder reactor. Furthermore, it is agreed that such development should be done promptly, commensurate with technological advancements and safety. Such interest is evidenced by the fact that:

1. The U.S. AEC is now allocating a major part of its civilian power reactor development budget to the liquid metal cooled fast breeder reactor. In fiscal year 1967, the LMFBR represented about 40 percent ($42.6 million) of the Civilian Power Reactor Development expenditure. This is expected to increase to about 55 percent ($66 million) in FY 1968, and to 62 percent ($82 million) in FY 1969. It is interesting to note, without comment, that the AEC's FY 1969 budget provides only $1.9 million for development work on the alternate fast breeder reactor coolants -- gas and steam.

2. The United Kingdom, France, Germany, Russia, and other countries associated with them have well organized, financed and staffed programs with specific objectives in the development of this concept. Figure 3 shows some of the major milestones of these national programs. Belgium and Holland are participating with Germany in its program. Fast breeder reactor programs in Italy and Japan have been more recently established but include similar objectives, perhaps five years later than France and Germany.

3. Most of the U.S. reactor manufacturers have formulated development programs. These vary in scope, progress and extent of financial support.

   Sodium cooled demonstration plants in sizes from 200 to 500 MWe are being developed by Atomics International, Babcock & Wilcox, and General Electric and Westinghouse. Babcock & Wilcox and General Electric have also studied and are developing the steam-cooled reactor concept, while Gulf General Atomic has been the leading industrial proponent of the gas-cooled fast breeder reactor.

   The electric power industry has provided or committed about $137 million to the development of the fast breeder concept. A major portion of this has been committed for the Fermi and SEFOR projects. The remaining amount of approximately $16 million is being spent at a rate of about $5.5 million per year in support of the sodium-steam and gas-cooled development programs.

FAST BREEDER REACTOR PROGRAMS – MILESTONES

(MUCH OF THE DATA PRESENTED HAVE BEEN ESTIMATED)

	U.S.A.	U.S.S.R.	U.K.	FRANCE	GERMANY -BELGIUM -HOLLAND	ITALY	JAPAN
START INITIAL STUDIES	1945	1949 - 1950	1951	1956	1956 (BELGIUM) 1959 (GERMANY)	1962	1962
EXPERIMENTAL REACTOR CRITICAL	1946 (Clementine) 1951 (EBR-I) 1963 (EBR-II) 1963 (EVESR)	1956 (BR-2) 1958 (BR-5)	1959 (DFR)	1967 (RAPSODIE)	1970 (KNK-Na) 1968 (HDR-St)	—	1973 (JEBR)
5% BURNUP IN FBR PINS FAST FLUX	1967	1961	1964	1968	1968	1968	1969
TEST REACTOR CRITICAL	1968 (SEFOR) 1972 - 1973 (FFTF)	1968 (BOR)	—	—	—	1972 (PEC)	—
DEMONSTRATION REACTOR CRITICAL	1963 (Fermi) 1974 - 1978 (No FBR)	1969 (BN 350)	1971 (PFR)	1973 (Phenix)	1974 (Na) 1974 (Steam)	1978	1977
COMMERCIAL REACTOR CRITICAL	1981	1976	1977	1979	1979 (Na) 1979 (Steam)	1982	1987
PROFESSIONAL STAFF 1966 - 1967	1000	?	650	700	200-250 (GER) 150-200(BEL. +HOL)	100	150
R & D MILLIONS $ -through 1967	400	?	—	200	60 (GERMANY) 10 (BEL & HOL)	15	—
-1967 annual	70		35	30	40 (+15 B&H)	10	10
REACTOR CONSTRUCTION MILLIONS $ -Already built	90 EBR I, II LAMPRE FERMI	?	24 — DFR	45 - RAPSODIE	—	—	—
-Being built or discussed	330 SEFOR FFTF 3 Demos.		70 — PFR	100 — PHENIX	300 KNK HDR Na. Demo. St. Demo.	140 PEC Na. Demo.	150 JEBR Na. Demo.

FIG. 3

INCENTIVES TO DEVELOP FAST BREEDER REACTORS

The basic incentive to develop fast breeder reactors is the potential of lower system power generation costs. The four major needs for its development, supporting the basic incentive are:

a. The more efficient use of light water reactor produced plutonium.

b. The economic potential of fast breeder reactors.

c. The protection or hedge provided against rising uranium costs.

d. The effective utilization of depleted uranium resources.

Plutonium Production and Utilization

Plutonium production is not only a function of operating power level but also of the type and design characteristics of the reactor. For the purpose of this data, it was assumed that all light water reactor capacity consisted of equal amounts of boiling water and pressurized water reactors. The designs and fuel exposure warranties of present-day light water reactors are such that plants would have an average total plutonium production of about 350 grams per electric megawatt year. Assuming an 80 percent capacity factor and a 70 percent fissile composition, this would result in about 200 grams fissile plutonium per megawatt per operating year. Based on this unit production and projected nuclear growth forecasts, something on the order of 700 metric tons of total plutonium will have been recovered by 1990, as shown in Figure 4. At the present value of $9.28 per gram of fissile plutonium, this amounts to about $4.5 billion.

Plutonium is a valuable fissile material and capital of this magnitude represents a challenge to the nuclear power industry in determining the best ways of utilizing it. All indications are that the value of plutonium in fast breeder reactors is substantially greater than in thermal reactors due to its higher neutron yield. For a low specific inventory--high breeding ratio fast reactor, this value might be as much as 50 percent greater than the cost of fully enriched uranium. If fast reactor utilization of plutonium can increase its value by $5.00 per gram over its use in thermal recycle, a reasonable amount, the saving that will accrue to the electric power industry would be about $2.5 billion.

Economic Potential

It appears that fast breeder reactors have the potential of ultimately reducing the cost of power generation by one mill per kilowatthour or more. Shown in Figure 5 are the estimated light water reactor and fast breeder reactor unit costs for the period 1970 to year 2000. The reduction in light water plants reflects the experience, standardization and size factors previously mentioned. The data, however, do not consider the cost increases which will probably occur due to the increased requirement for cooling towers to minimize thermal effects in lakes and streams, additional engineering safeguards as size increases, increased contingencies as a result of scheduled delays or operating experience, and, of course, escalation. Similarly, and perhaps more pronounced, a decrease in the capital costs of fast breeder reactors is expected. The cost of $200 per kilowatt for a fast breeder reactor in 1975 is based on 300 to 500 Mw demonstration type plants. As fast reactors are built in larger sizes and more experience is gained, a reduction is expected in their unit capital costs. It is assumed here that this cost will continue to decrease to something on the order of $130 a kilowatt, perhaps 5 percent higher than light water reactors in the year 2000.

TOTAL PLUTONIUM AVAILABILITY FROM COMMERCIAL POWER REACTORS-CUMULATIVE

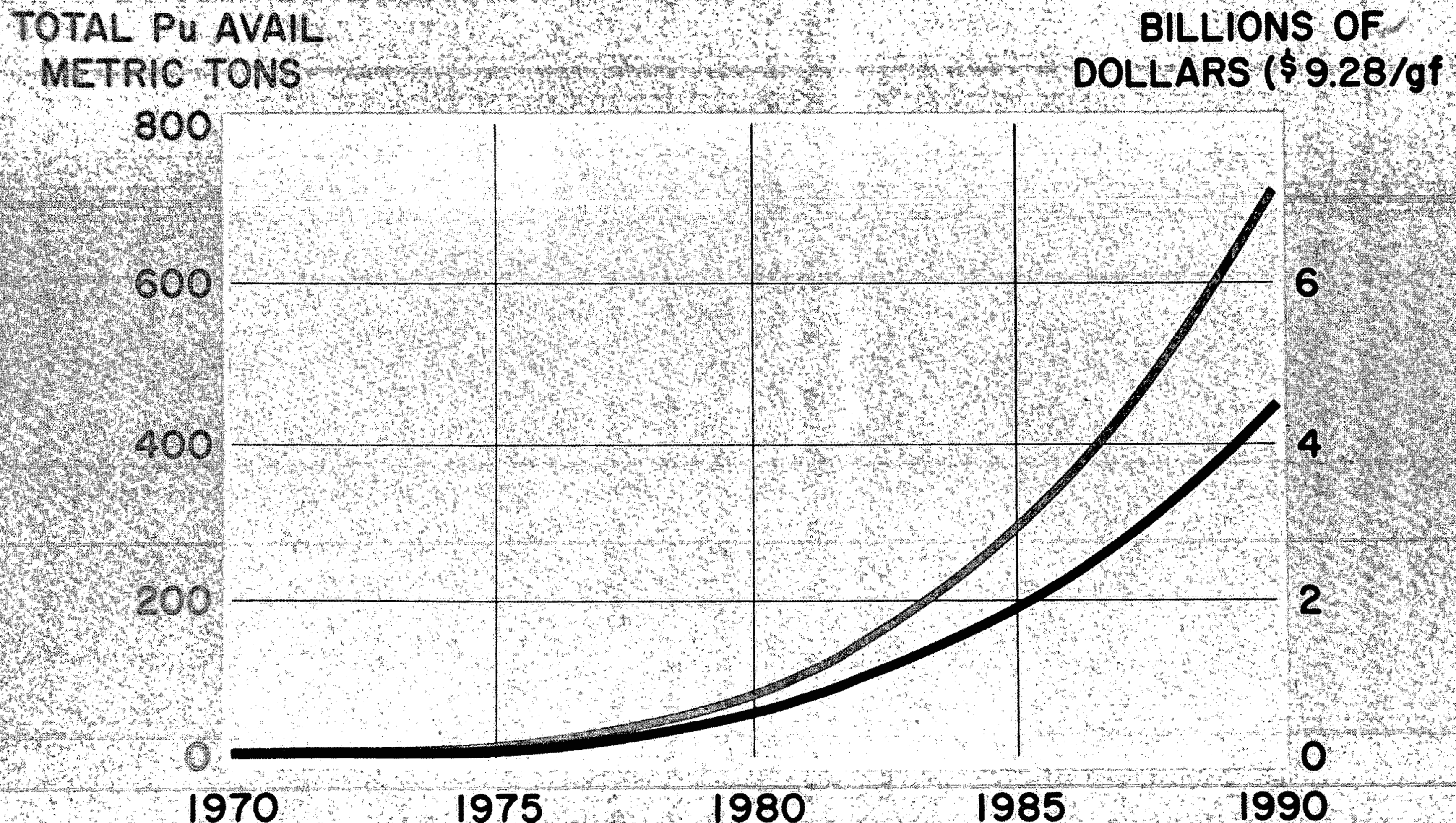


FIG. 4

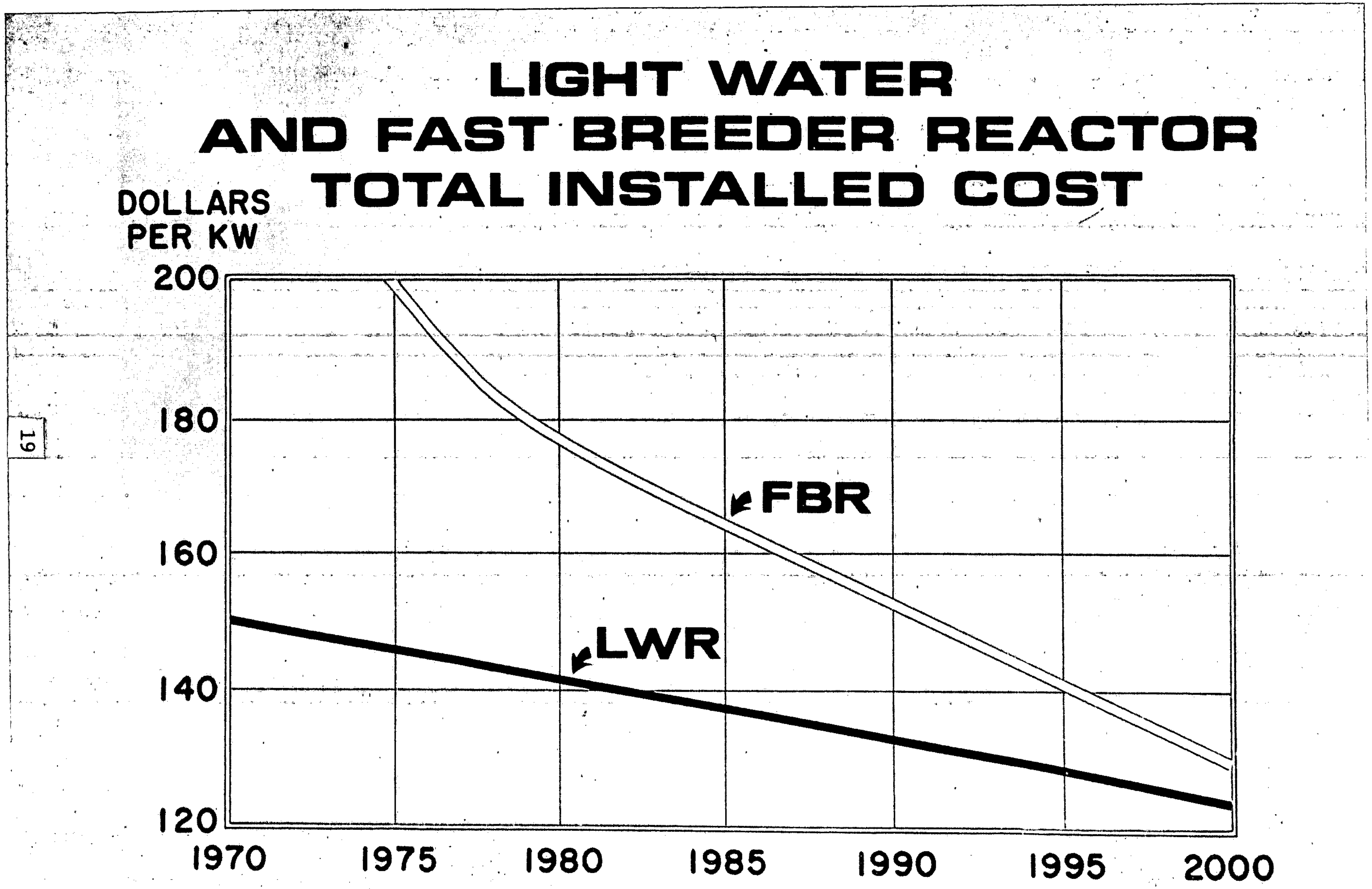

LIGHT WATER
AND FAST BREEDER REACTOR
TOTAL INSTALLED COST
DOLLARS
PER KW
200
180
160
140
120
1970
1975
1980
1985
1990
1995
2000
FBR
LWR

FIG. 5

A comparison of light water and fast breeder reactor total power generation costs are presented in Figure 6. Certainly the data used in compiling the light water reactor is better grounded than that of the fast reactor. The data reflected by the light water reactor curve include the decrease in unit capital costs and the changing light water reactor fuel cycle costs previously mentioned.

Fuel cycle costs for fast breeder reactors are difficult to assess at this time. For purposes of this analysis they were assumed to be 1.4 mills per kilowatthour in 1975 and to reduce to 0.3 mills per kilowatthour by year 2000. It is realized that considerable developmental and research work will have to be accomplished successfully if values such as these are to be attained. However, in view of the present status of technology and the overall development programs for fast breeder reactors, there appears to be reasonable assurance that fast reactors and light water reactors will attain an economic stand-off in 1985.

Since, in preparing these curves, it was felt that the data used for light water reactors is somewhat optimistic and it was intended that the fast reactor data be conservative, the actual crossover point may well occur before the year 1985. In any case, based on these curves, a 1,000 megawatt fast reactor plant installed in 1980 would be competitive over the lifetime of the plant with the light water reactor of the same size.

Protection Against Rising Uranium Costs

If the nuclear power growth forecast shown earlier is to be met, considerable quantities of uranium will be required. Figure 7 shows the cumulative U.S. uranium requirements prior to year 1990 as well as the present AEC estimated reserves within the $15.00 per lb price range. It should be noted that of the 200,000 tons reasonably assured in the $5.00 to $10.00 per lb price range, about 50,000 tons are now in the AEC stockpile and are expected to be made available to industry at some future time; 25,000 tons are yet to be delivered to the AEC; and about 40,000 tons have been purchased already by the power industry. Based on an installed nuclear capacity of 390,000 megawatts in 1990, nearly one million tons of uranium will have to be supplied to provide fuel up to that time.

The mining industry is optimistic, based on past experiences, that substantial additional low-cost uranium deposits will result from the major exploration drilling now under way. Nevertheless, it appears that, if the nuclear growth forecasts are realistic, higher uranium costs will be incurred in future years. The extent, timing and effect of such increases on the competitive economics of nuclear power cannot be assessed at this time because of the many unknown factors.

Light water reactors require over a 30 year life about five tons of uranium per electrical megawatt, if plutonium is recycled. Without plutonium recycle, this amount increases to about seven tons per megawatt. Using the lower consumption, the amount of uranium needed to fuel the nuclear capacity added between now and 1980 for 30 years of operation would be 750,000 tons. Applying this unit consumption to an installed nuclear capacity in year 2000 of 700,000 megawatts would result in a total uranium requirement of 3,500,000 tons.

The introduction of fast reactors in 1980 and assuming that all nuclear capacity added after 1990 was of this concept would result in reducing the cumulative nuclear uranium requirement by 35 percent. It is, therefore, in the interest of the industry to develop the fast breeder reactor in order to substantially reduce U_3O_8 requirements, thereby delaying, if not offsetting, future uranium price increases.

LIGHT WATER AND FAST BREEDER REACTOR POWER GENERATION COST

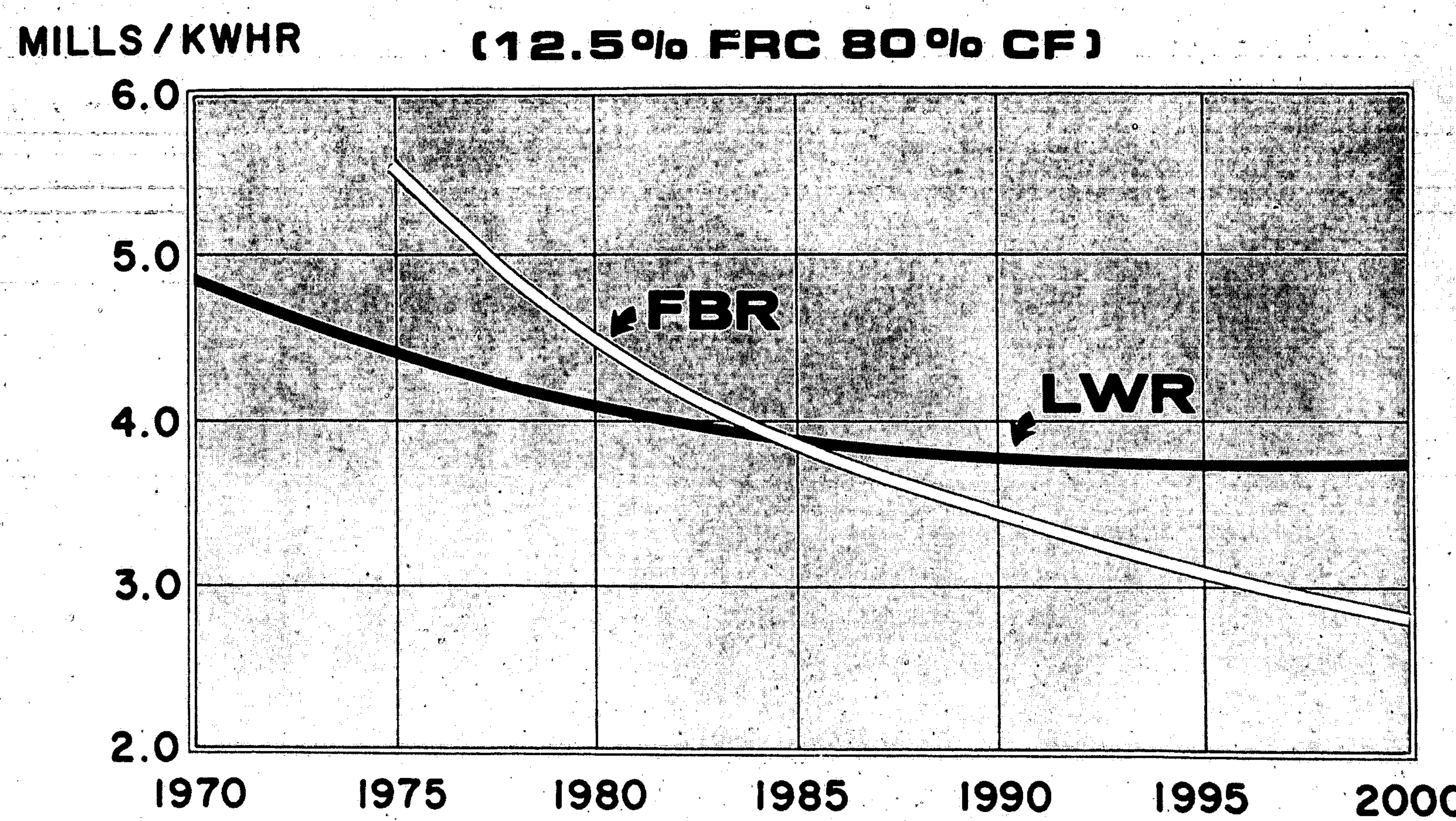
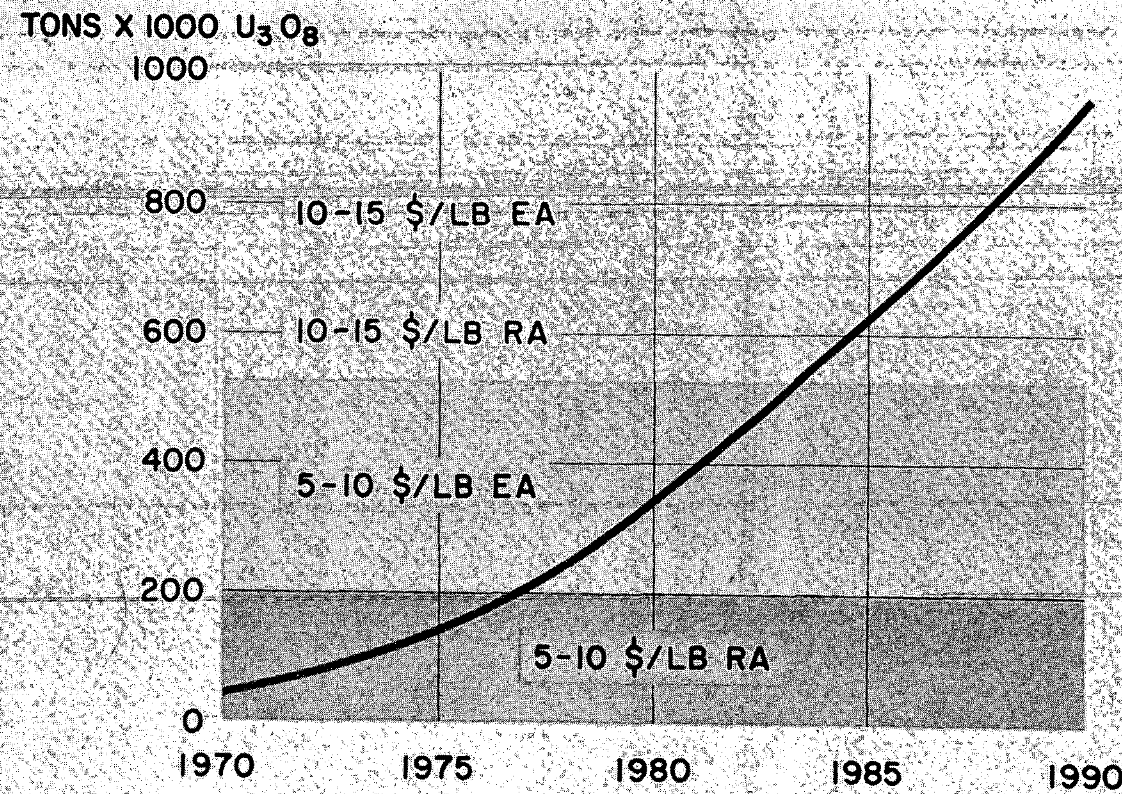


FIG. 6

CUMULATIVE U.S. URANIUM ORE REQUIREMENTS AND AEC ESTIMATED RESERVES

(150,000 MW INSTALLED NUCLEAR IN 1980)



RA = REASONABLY ASSURED
EA = ESTIMATED ADDITIONAL

FIG. 7

Full Utilization of Natural Resources

Only by the use of fast breeder reactors can we effect full utilization of our natural resources. Today's light water reactor industry can release only about one percent of the potentially available energy from uranium. Most of the uranium mined ends up in the depleted uranium stockpile, which is of little or no value to light water reactors or the nation's economy.

The Atomic Energy Commission has stated that cumulative U_3O_8 delivered to them from all sources between 1942 through 1970 will be approximately 330,000 tons. If the 50,000 tons presently stockpiled and the 25,000 tons yet to be delivered are deducted from this amount, and it is assumed that the remaining 255,000 tons have been processed through the diffusion plants as slightly enriched uranium, perhaps 170,000 tons of uranium are now stockpiled. If the nuclear growth forecast projected herein materializes, an estimated 340,000 tons of U_3O_8 will be required by 1980 to meet the civilian power reactor demands. This will result in an additional amount of about 230,000 tons of depleted uranium metal.

Therefore, by 1980 there will be a stockpile of about 400,000 tons of uranium. This material can be used as a diluent, fertile material in fast breeder reactors. By such use, most of its potential energy can be released through the conversion of uranium 238 to the fissile material plutonium 239, appreciably increasing the world's energy resources.

CONCLUSIONS

In conclusion, for some years to come the power industry is expected to grow at approximately the same rate experienced during the past 30 to 40 years, that is, doubling about every ten years. By the end of the century, perhaps 2 billion kilowatts of electric generating capacity would be installed, and about one-half of this probably will be nuclear. To meet this tremendous growth, more ore will be required than is presently available, and presently estimated to exist. However, the mining and milling industries are now undertaking large explorations in this area, and it is expected that sufficient uranium ore can be recovered at reasonable prices to sustain this growth for some period of time.

The electric power companies are continually striving to improve service to their customers and provide their product at the lowest possible cost. The present status of the light water reactor concept and the probable future improvements present a worthy challenge to the development of fast breeder reactors.

If nuclear power, however, is to play an important part in the generation of electrical energy during the latter part of this century and into the next century, it appears necessary to develop an economic, high performance fast breeder reactor.

Substantial savings resulting from the development and commercial application of fast breeder reactors are predicted for the industry. Present worth industry savings of well over a billion dollars appear to be attainable within 15 years after introducing a competitive fast breeder reactor. Assuming a fast breeder reactor plant and a light water reactor plant have equal power generation costs at the time of initial operation, a $50 million saving may be realized over a 20 year period by the installation of the fast breeder. To realize such savings, large amounts of time and money must be spent on research and development in bringing the fast reactor to fruition. The reward certainly would seem to justify such an effort.

It is felt that through the reactor manufacturer's programs, the effort supported by the AEC in the development of basic technology, and the growing interest and support of the electric power industry, fast breeder reactors will be attained providing abundant and inexpensive electrical power for generations to come.

DISCUSSION

F. O'Hara (University of Cincinnati) - Mr. Beekman, I wish you would comment on the fact that nuclear power is not now competitive with fossil fuel until approximately 800 megawatts electrical compared with maybe 500-600 megawatts a few years ago, and what effect this will have on the future of nuclear power. This was reported in the Nuclear News and brought out in the JCAE hearing.

M.C. Beekman - If I heard the question correctly, you were saying that nuclear power is now competitive with roughly 800 megawatts whereas a few years ago it was some lower megawatts. I think there are many other people present who are better qualified to answer this than I am. I am speaking of the reactor manufacturers. I think that I would have to agree with Mr. Sporn that the development or the progress in the costing of nuclear plants and the costing of conventionally fired plants has gotten a little bit out of hand. I can readily appreciate this after having spent only two months in the cost-control department of the company and seeing what is happening to our productivity/labor. I can't answer the question exactly or directly. I am sure that there is something in here that affected manufacturers who presumably went along on a shoe string with nuclear power for a long time and perhaps now they are trying to regain some of their losses or at least to earn some money for a change. Perhaps the manpower, the productivity of the labor involved in the nuclear plant has more severely hurt them than it has that of a fossil fueled plant. I would appreciate any comments that anyone else might have on this subject. I think you are right; I am just inadequately fortified to explain why.

E.C. Pandorf - Apparently everybody else is inadequately fortified in trying to explain why. One thing that I would like to comment about, Myron, is that we are constantly worrying about the known U.S. reserves or the predicted U.S. reserves of uranium in some form or another. It would seem to me as though we are taking an awfully narrow viewpoint when we talk only about what is available here in the United States. It is perfectly obvious that we are importing from other countries many materials for many purposes and one of the things that irritates me tremendously, having visited a year or so ago in Southern Africa, is the fact that the United States which used to buy 75% of the chromium from Rhodesia is no longer buying chromium from Rhodesia because of the ban in exports from Rhodesia. For this reason we are not buying it, they are selling it to the Russians and then we turn around and buy it from Russia. We don't have chromium in this country; maybe we don't have enough uranium, but if it is available in other places like in South Africa, why do we constantly worry about the domestic uranium supply?

M.C. Beekman - I think this comes about for a couple of reasons. Compared to the rest of the world we have, of course, in this country a large uranium deposit as does Canada. We also know that the rest of the world, although they have substantial quantities of uranium, also have a nuclear power program of their own. It is generally assumed that the growth rate of overseas nuclear power programs as compared to their total uranium deposits are roughly in proportion to our growth rate compared to our uranium deposits. It is very possible that the embargo will be lifted, I'm sure it will be in the future, and we could import uranium from overseas countries. I don't know how this will compare with the price that is available in this country. I am not trying to say here that we are going to run out of uranium - there is no question in my mind there is plenty of uranium in the world to last us for as long as we want - the only question is how much are you going to have to pay for it. The possibility of running out of low cost uranium ore (maybe $10 or $15 per pound) on a light water reactor only expansion appears quite probable sometime in the future. Certainly the mining and milling industries have a very active and progressive program of exploration of drilling, and they will come up with new quantities. So, it is only a question of time and it is a very difficult thing to say at this time.

G.L. Wensch (USAEC) - From the standpoint of accuracy, Myron, I think your slides showing the experience of the American program in sodium doesn't lay it out in proper perspective. It also makes a comparison of reactors which have different values. For example, you have EVESR down there and you omitted SRE and Hallam. I also noticed that you eliminated LAMPRE. Frankly, I think that if one wishes to add the proper worth to these facilities in terms of the contributions they have made to sodium systems, one could easily eliminate Clementine and put down SRE and Hallam. I think by the same token EVESR has not added as much to our experience in terms of sodium systems. That is point #1. Point #2 - I am not sure there is an implication here that the U.S. program with all this experience has a slower program than the other countries with a lesser experience. I would like to have (1) your views on my clarification of your slides and (2) what you meant by making comparisons of schedules here and abroad. Are you raising an implication by stating that in view of the vast American experience in fast reactors, our schedule has slipped or is behind those of the other national programs?

M.C. Beekman - By the use of that slide, and I will mention again that it is a greatly condensed version over the data that are in the report, I was not trying to imply that the United States was not fulfilling their obligations in fast reactor development or in progress; I was not trying to be critical. From these data developed in the EEI fast reactor study group, I was trying to point out to the best of my knowledge for these countries, when the initial studies were started, what experiments we had in operation, the current amount of funds, and recognize that when you are trying to guess in the millions what Russia, France, Germany, etc., have spent for research and development on fast reactors, it is somewhat arbitrary, including our own. I was just trying to point out that these are the data that we have collected in the EEI fast reactor study report. By my presentation this morning, as I say, I was not trying to be critical but to the best of my knowledge, the data reported in that slide are correct.

BREEDER REACTORS FOR DESALTING

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Abstract

In choosing an energy source for desalting seawater, the economic criterion is solely that the cost of a thousand pounds of low pressure steam shall be as low as possible. A method is shown for comparing widely differing steam sources on an equitable basis, and this method is applied to four types of advanced breeder reactors. A new concept using unclad metal fuel in a sodium coolant is shown as promising the lowest energy costs of the reactor types studied so far.

A source of heat energy for seawater evaporators is subject to a different group of constraints and is valued in a different type of unit than is a source of heat energy for producing electric power. The seawater evaporator can utilize heat at a relatively low temperature only, because of the chemical properties of seawater, while for power production the higher the temperature, the better the thermal efficiency. In comparing different heat sources, the economics of water production are determined by the cost of a million Btu's of heat (or a thousand pounds of steam) at the evaporator inlet temperature. For power production, the economics are determined by the cost of heat and by its electric yield, i.e., its temperature. The temperature may vary widely with different sources, so that steam cost alone does not determine economic merit. Thus, if the cost of prime steam is S, the value for power production is measured by S/ε, where ε is the net thermal efficiency. But S alone is the criterion for seawater use in a water-only station.

In a dual-purpose plant built primarily for power production, the value of partially expanded evaporator steam obtained from a back-pressure turbine must be something less than S. As discussed in another paper,[1] there is an equitable non-subsidy method of assigning a value, H, to such exhaust steam based on the loss of potential electric output. Without repeating the derivation of this method of cost allocation, I present here the final results as follows:

$$H = S\left(\frac{\varepsilon - \beta}{\varepsilon}\right)\left(\frac{1}{1 - \beta}\right) + C\,\frac{\beta}{\varepsilon}\left(\frac{1 - \varepsilon}{1 - \beta}\right).$$

In the above equation S, H, and C are the economic values, or costs, or prime steam, back-pressure exhaust steam, and steam entering a waste condenser, respectively. C is usually negative in value. The quantity ε is the net thermal efficiency of the normal power cycle, using a condenser, and β is the net thermal efficiency using the back-pressure turbine.

In the first term of the equation, the quantity $(\varepsilon - \beta)/\varepsilon$ represents the fraction of the electrical output that is sacrificed by diverting steam to the water plant instead of expanding it fully, and the quantity $1/(1 - \beta)$ corrects for the fact that H and S do not contain the same number of thermal units.

In the second term, the quantity β/ε represents the portion of the cost of condensing steam that is assignable to power, the quantity $(1 - \varepsilon)$ corrects for the fact that the number of thermal units delivered to the condenser of a condensing turbine is less than the number that enter the turbine throttle. The denominator serves the same function as in the first term.

This analytical expression is an abbreviated summary of a much more detailed and specific method of cost allocation applicable to real situations. This method is spelled out in the reference given. Here I wish only to make use of the ability it provides to translate a steam cost at one condition to an equitable value at another condition, under the circumstances applicable to desalting. Using this method, we can compare several reactors, producing steam at widely different temperatures, pressures, and costs, and get a correct picture of their relative attractiveness as energy sources for desalting.

The program of the Desalting Branch of the AEC has included a search at Oak Ridge of reactor types potentially suited for low cost desalting. This study has had many interesting aspects and has covered a wide variety of types; it is still under way. Today I will report in a very preliminary way on one portion of the studies we have made of breeder reactors of the dual-purpose type. It is clear that under certain circumstances a single-purpose reactor, one that produces low temperature steam only, might be preferred, but so far there has been much greater interest in producing power as well as water.

In Table 1, four advanced breeder reactors are compared as to the cost of evaporator steam which they could furnish at a temperature of 300°F.* Since not all of these systems are equally well developed, this comparison can only be tentative. Hence, Table 1 is more illustrative than determinative; all are compared at a size of 5000 MW(t). Adjustments in size from the original cost estimate were made by interpolation from larger and smaller sizes. Steam costs were adjusted to 300° by the method cited in Reference 1.

The CEFBR is a design prepared for the AEC by Combustion Engineering Corporation.[2] It uses a carbide fuel clad in stainless steel and operates at a prime steam temperature of 1000°F.

The VLFBR[3] was designed by Argonne National Laboratory with the assistance of Westinghouse Corporation. This work was performed as part of the desalting program at ORNL. The original size was 10,000 MW(t), and we have scaled it

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*The temperature at which present-day evaporators can accept steam is about 260°F for flash evaporators and somewhat higher for vertical tube plants. Developments now under way in the OSW program should raise this temperature substantially.

TABLE 1

Breeder Reactors Compared as Evaporator Heat Sources

	CEFBR	VLFBR	MSBR	UMBR
Prime Steam Temp., °F	1000	900	1000	690
Net Thermal Efficiency (ε)	.425	.372	.449	.366
Prime Steam Cost,† S (¢/MBtu)	10.3	9.4	11.2	5.6
Power Index, S/ε	24.2	25.3	24.9	15.3
Value of Exhaust Heat, H (¢/MBtu), at 300°F	4.0	4.2	3.8	2.5

†Municipal financing.

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down for comparison. The core fuel is carbide, clad in stainless steel. The unique feature of the design is its use of uranium metal in the blanket.

The MSBR[4] is an advanced concept using a liquid fuel containing ^{233}U. The fission neutrons are thermalized by graphite. The MSR experiment at Oak Ridge has been performing well this past year, and recent developments indicate that a one-region breeder, less expensive and complicated than the two-region version shown here, may be feasible. The molten salt breeder produces very high thermal efficiency.

Finally, the UMBR[5] is a new concept which achieves very efficient nuclear performance because of the use of unclad metal fuel. Thorium metal is used as part of the fuel alloy to provide greatly improved metallurgical and radiation effects properties compared to uranium-based alloys.

In Table 1, it is of interest to note the relationships between the unit cost of prime steam (S) and the cost of steam per unit of power produced (S/ε). This quantity is the boiler portion of power cost. Adding the turbine generator and condenser costs would give the total power cost, but these latter components are not very sensitive to the type of reactor. Thus the power index (S/ε) is a very useful criterion of merit in comparing reactors as power sources. The quantity H, computed from S by the method referred to, is the economic criterion needed to compare these same reactors as desalting sources.

The comparison in Table 1 is made at a reactor size of 5000 MW(t) and uses the public financing fixed charges appropriate to desalting economics. It is of interest to compare prime steam and exhaust steam costs as a function of size, as is shown in Figures 1 and 2. You will note that the unclad metal breeder,

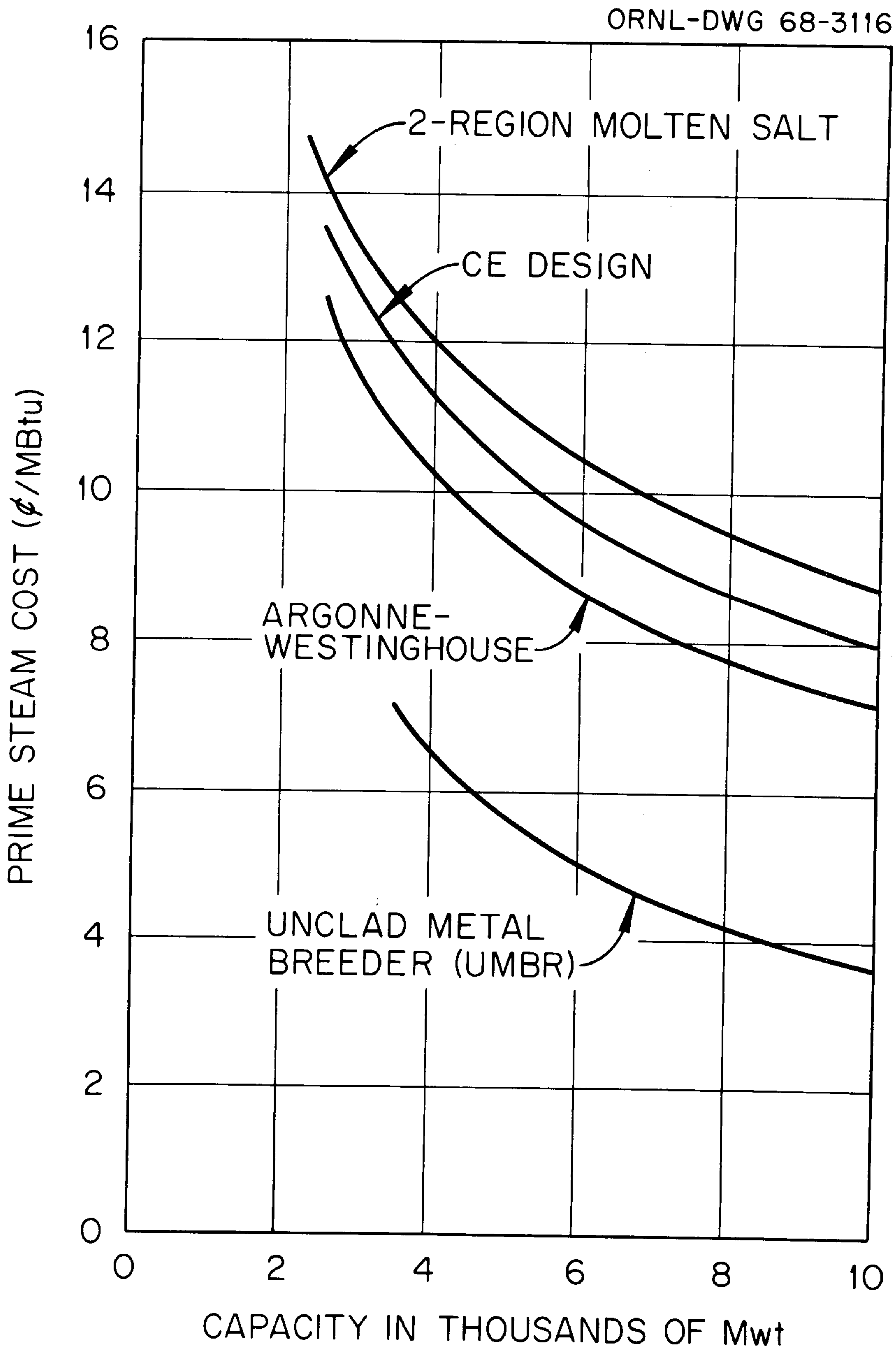


Fig. 1. Prime Steam Costs

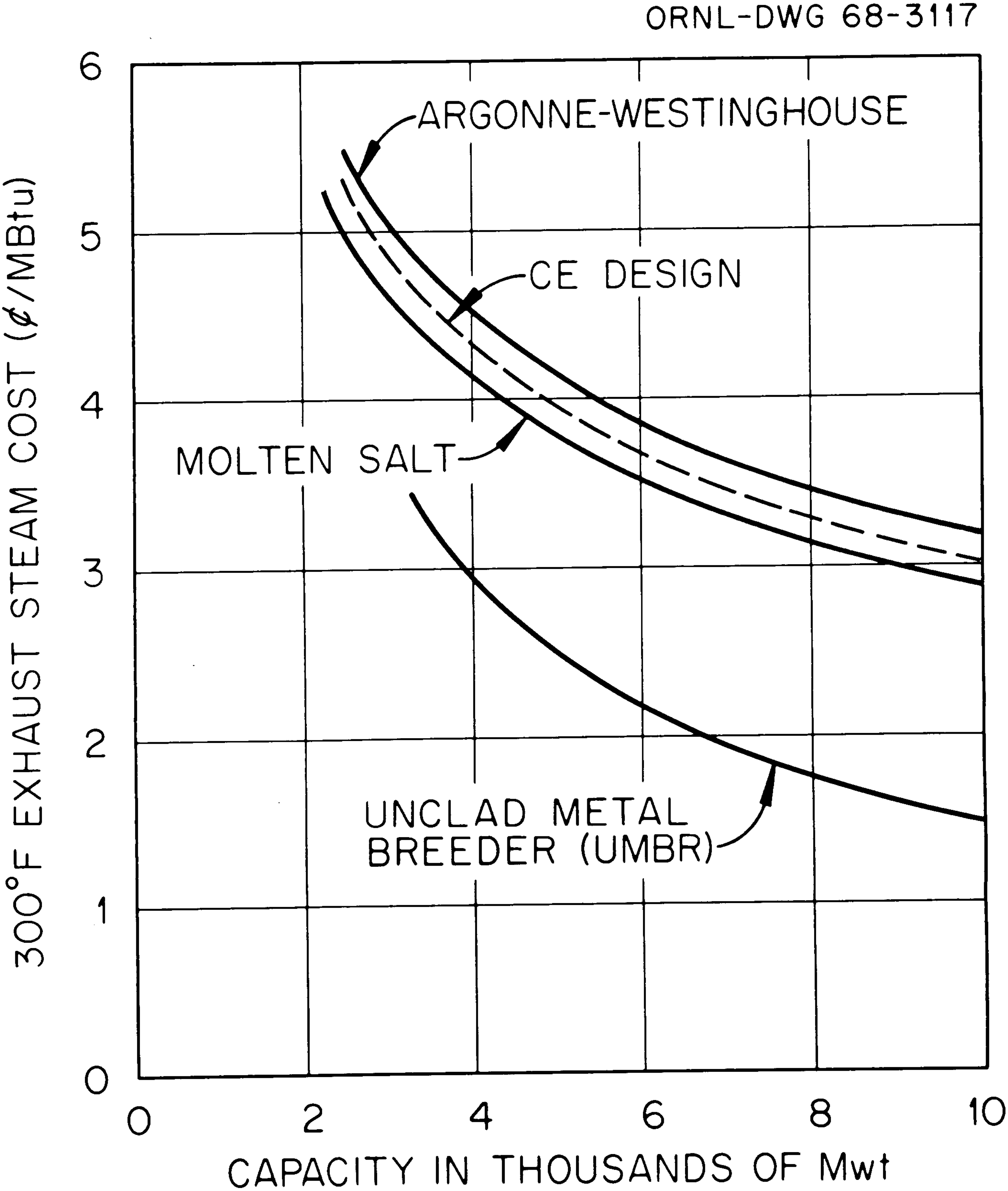


Fig. 2. Exhaust Steam Costs

UMBR, shows a relatively greater advantage at the larger sizes, approaching a steam cost only half that of the other types. This concept is, of course, not yet on as firm a basis as the others, so the indications so far can only be taken as promising. In the past three years, however, a considerable amount of information has been collected bearing on the general feasibility of the UMBR concept. This information is now being reviewed by the AEC. It is certain that some additional experimental data will be needed before reaching a final evaluation of the concept.

The irradiation data and other information available are presented in detail in ORNL-4202, now in process of publication, and I will not repeat it here. In Figures 3-7 and Tables 2-4, however, I present some of the more important properties of the system as now envisioned.

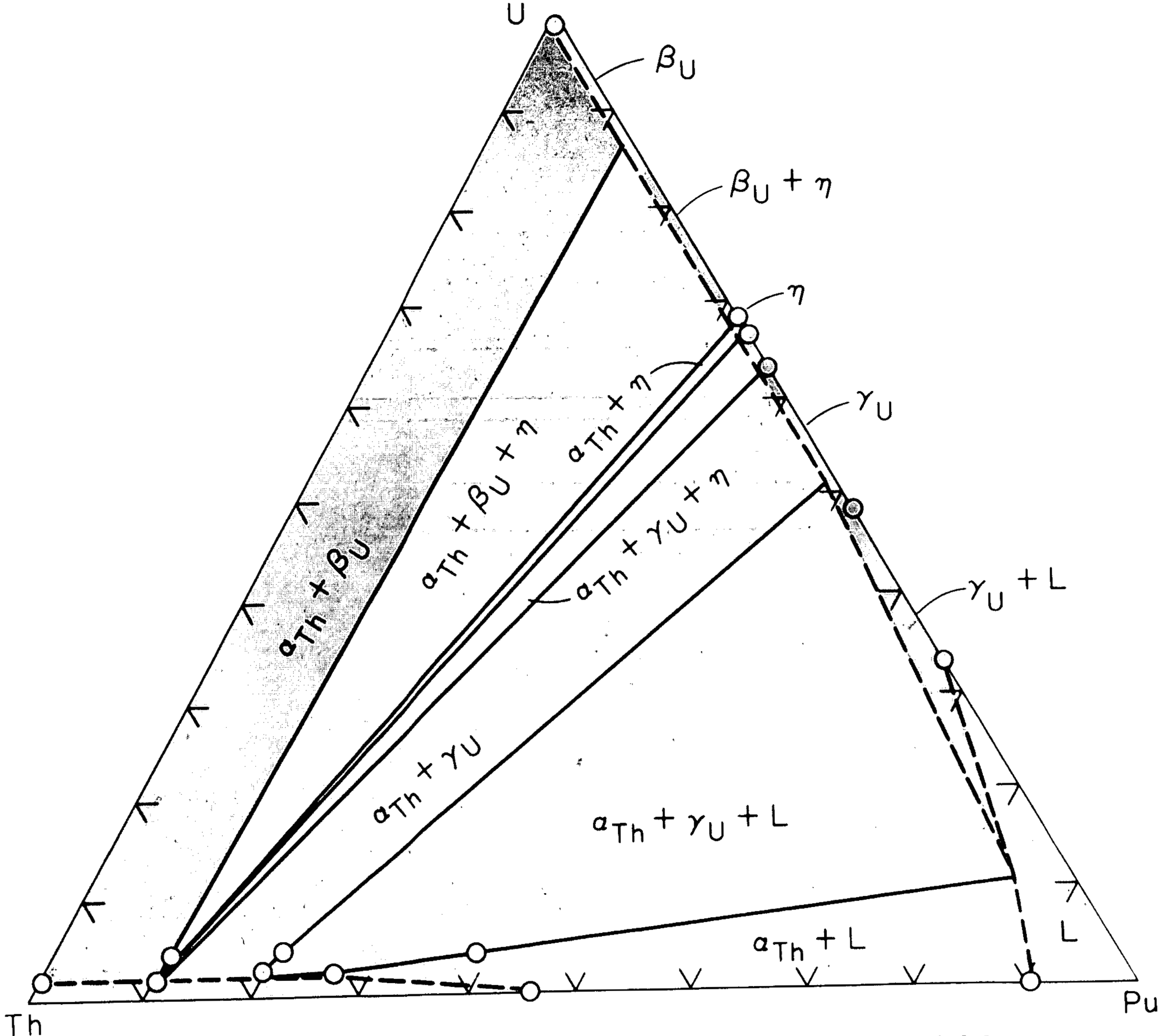
You will note that the fuel in the core is primarily thorium metal. The strength, conductivity, and irradiation stability of thorium appear to be maintained if the alloy is sufficiently dilute in other metals. Figure 3 shows the phase diagram of the Th-U-Pu system, at 700°C, as obtained by Argonne National Laboratory.

Figure 4 shows a sketch of the cast fuel shapes of depleted U which might be used in the blanket. A very similar configuration is proposed for the core, using a built-up compact made from hexagonal tubes having half the wall thickness shown here. The tubes are made from alloy powder by extruding, sintering, and drawing to final size, cutting to the core length of 18", and stacking together to form the final core element, which is about four inches across. The element is placed in a refractory metal can with the upper and lower axial blanket portions and steel reflector sections. The can is primarily a handle to lift the assembly in and out of the core. Sodium coolant enters a nozzle at the bottom of the can and flows up through the openings in the element. Figure 5 shows the assembly.

The most important characteristics of this reactor stem directly from the ommission of the fuel cladding. First, fuel fabrication techniques which would not be applicable to a clad fuel can be used. The neutronic efficiency is greatly enhanced, since about 30% of the core volume, normally occupied by cladding, is replaced by fertile material. This means that the spectrum is harder, the breeding ratio is higher, and the specific power is higher than for an equivalent clad fuel without any corresponding penalty in heat transfer parameters. However, the higher heat release per liter means that the active core length must be relatively short, in this case 18".

The high heat conductivity and high melting point of thorium are, of course, advantages. The properties of thorium and the shape of the core combine to give a negative sodium void coefficient. Other design characteristics are shown in the figures.

In summarizing our work so far on low cost energy sources for desalting, we find that breeder reactors in general offer prospects for much lower cost heat than do today's reactors and that the unclad metal breeder, if it can be made to work, may offer still another significant advance.



Th-U-Pu Ternary Phase Diagram at 700°C

Fig. 3. Phase Diagram

ORNL-DWG 67-10461

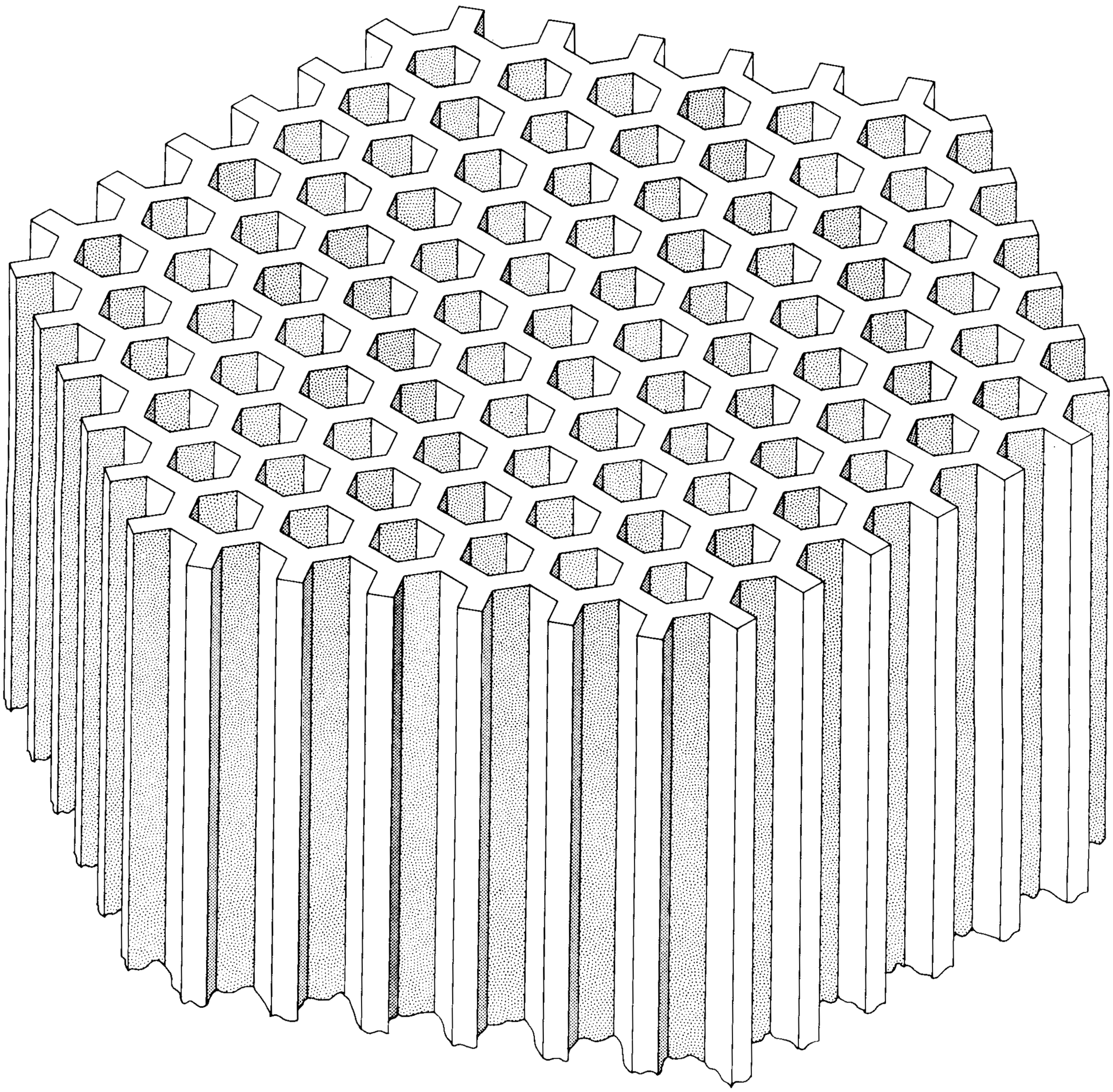


Fig. 4. Basic Fuel Block

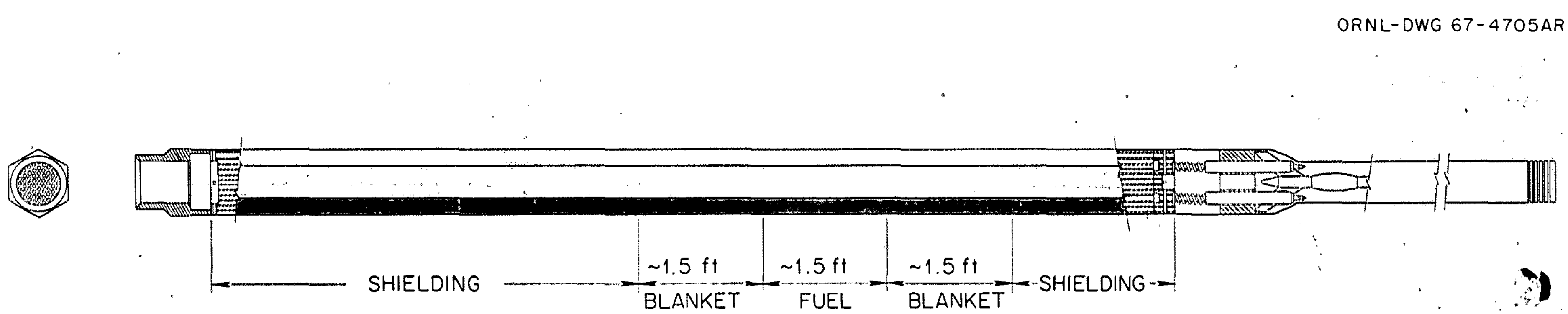


Fig. 5. Fuel Element Assembly

ORNL-DWG 67-9578R

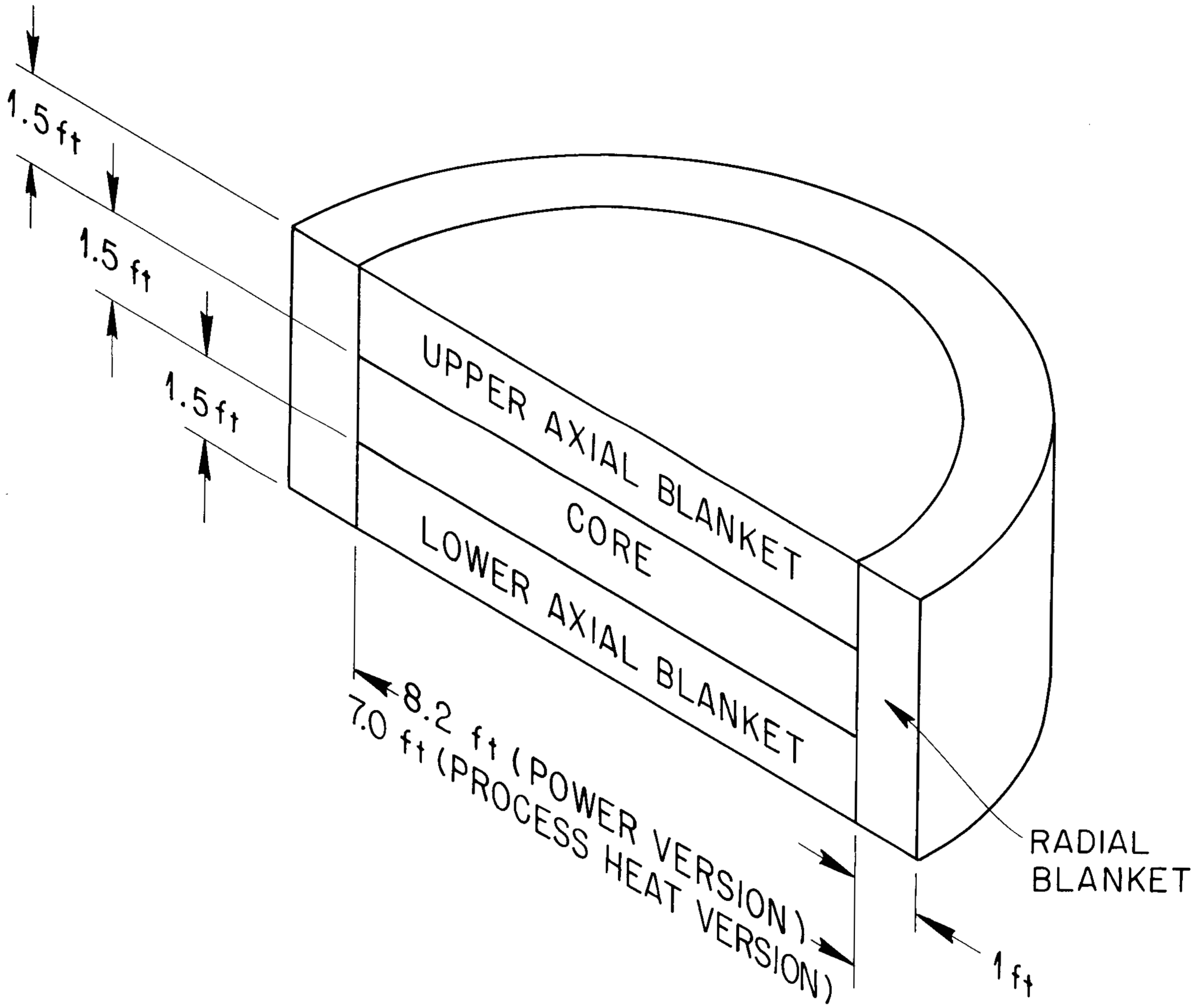


Fig. 6. Schematic of Core Arrangement

ORNL-DWG 67-4704A

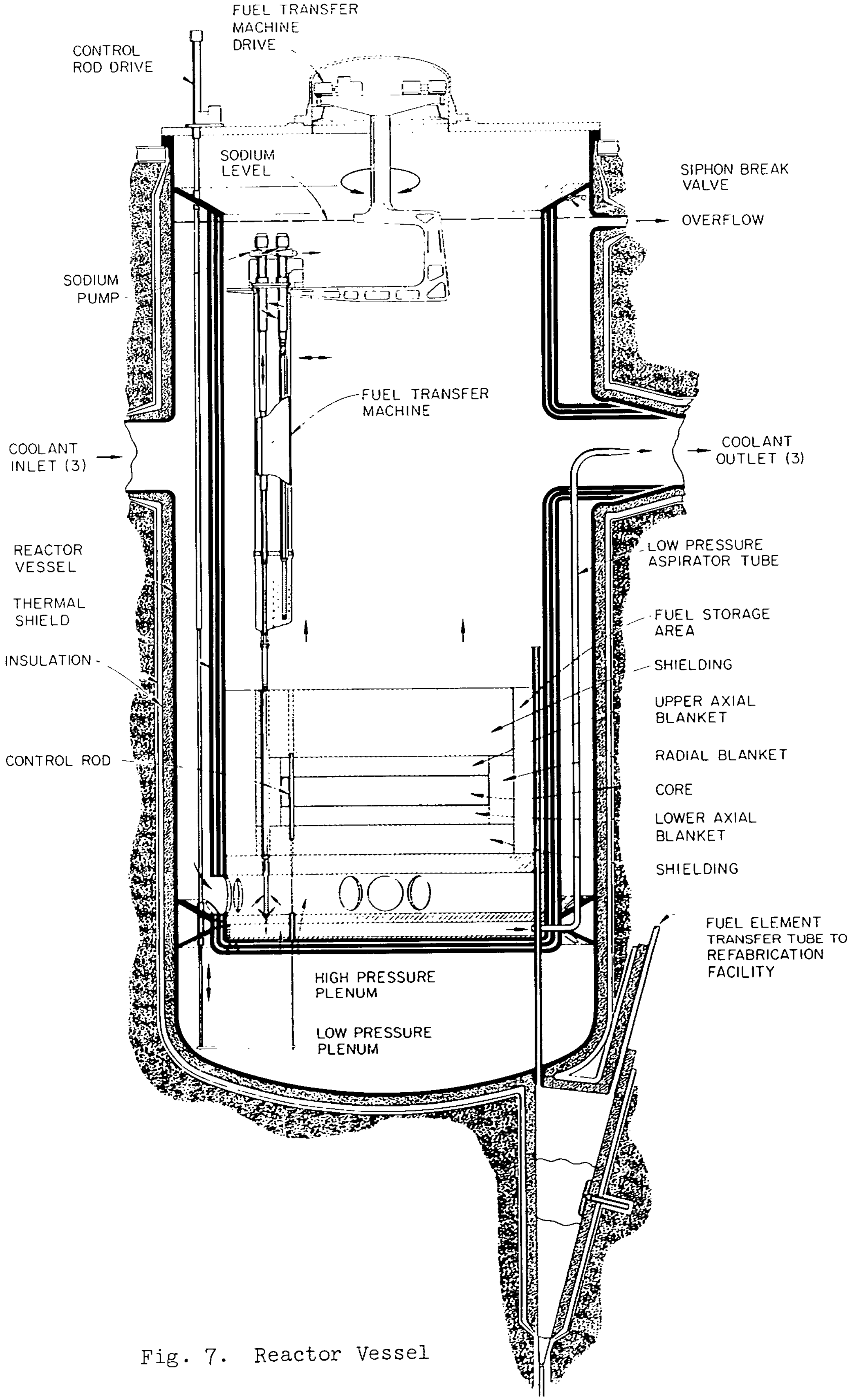


Fig. 7. Reactor Vessel

TABLE 2

UMBR Fuel and Blanket Composition
(Mixed Cycle, Power Version)

Core Fuel Elements	
Thorium	73%
Uranium	16%
Plutonium	6%
Fission Products	5%
Radial and Axial Blanket Elements	
Uranium	99%
Plutonium	1%

TABLE 3

UMBR Design Characteristics
(Mixed Cycle, Power Version)

Reactor Power, Mw(t)	3500
Core Diameter, ft	8.2
Core Height, ft	1.5
Blanket Thickness, ft	1.5
Maximum Fuel Temperature, °C	650
Primary Sodium Temperature, inlet/outlet, °C	345/525
Secondary Sodium Temperature, inlet/outlet, °C	324/473
Steam Temperature/Pressure, °C/psi	366/1450
Net Thermal Efficiency, %	36.6
Average Core Specific Power, kw(t)/kg heavy metal	220
Average Core Power Density, kw(t)/l liter	1284
Maximum Core Burnup before Refabrication, Mwd/T	40,000
Average Core Burnup before Reprocessing, Mwd/T	100,000
Core Enrichment, %	12.5
System Specific Power, kw(t)/kg fissile	1037
Total Breeding Ratio	1.64
System Doubling Time, years	3.4

TABLE 4

Reactivity Worths (Mixed Cycle, Power Version)

Core Doppler Coefficient at 535°C, $\delta k/°C$	-2.4×10^{-6}
Fuel Expansion Coefficient, $\delta k/°C$	-0.7×10^{-6}
Sodium Loss from Total Core and Blanket, δk	-0.004
Fuel Element Insertion, Maximum δk	+0.002

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4. Paul R. Kasten, E.S. Bettis, Roy C. Robertson, Design Studies of 1000-Mw(e) Molten-Salt Breeder Reactors, USAEC Report ORNL-3996, Oak Ridge National Laboratory, August, 1966.

5. R.P. Hammond, C.C. Burwell, R.S. Carlsmith, J.G. Delene, L.C. Fuller, D.B. Lloyd, J.E. Savolainen, and R.P. Wichner, High Gain Breeders for Desalting or Power Using Unclad Metal Fuels, USAEC Report ORNL-4202 (to be published).

DISCUSSION

K.A. Trickett (USAEC) - Would you like to tell us what are some of the problems you have to successfully solve to make the bare metal reactor into a power plant?

R.P. Hammond - Well, that is not really the topic of my paper but it is the subject of a forthcoming report at Oak Ridge, ORNL-4202, which is not quite finished yet. However, briefly, there are problems more of verification than of basic concern; the thermodynamic stability of uranium-thorium in sodium is well known. The problems have to do with impurities in sodium. It is quite sure that this unclad uranium blanket will primarily be the sink for most impurities in the system and so the rate at which impurities leak into the sodium has to be compatible with the rate at which fuel is taken out of the blanket for processing. The fuel in the blanket is not recycled so it will be the main getter, and our calculations indicate this is a very tolerable situation. There are a few other problems having to do with improvement of the gas clean-up system. These are matters of degree compared to vented fuel rather than economy, and I don't think it is a question of feasibility there, it is more the question of cost. There are certainly problems having to do with how you fabricate thorium alloys cheaply enough. We have only begun to explore that area, and I can't give you any final answer.

P.F. Gast (ANL) - I am a little bit mystified about where these cost savings come from as compared with the more conventional reactor design. You have a very complex fuel shape. You are going to reprocess or at least refabricate after 40,000 megawatt days per ton, whereas in the conventional designs people usually postulate 100,000. You have a low-temperature reactor with fission products normally in the coolant and low temperature steam which implies large sizes for your equipment. I wonder if you could explain a little bit just what the sources of the cost savings are.

R.P. Hammond - Everything you say is true - the fission products are in the sodium and those fission products raise the radioactivity of sodium by perhaps 10% over the radioactivity that is already there. This is an economics problem but not one of feasibility. The experience in sodium graphite reactors show that fission products, carbon and many other things in sodium can be successfully cleaned by trapping and experience at Los Alamos confirms this also. As to the question of cost of fabricating compared to the burnup, the economic criteria is the ratio of dollars per kilogram to megawatt days per kilogram which is dollars per megawatt day, and so if we have about 1/3 of the burnup of clad fuel, we must find a fabricating method that will cost about 1/3 as much. It appears to be feasible to do so because it is clear to almost anyone that at least 2/3 of the cost of the fuel is in the cladding and the opportunities we have in the methods of making unclad fuels in very efficient shapes appear quite good. For example, this complex shape you saw there is actually made by extruding powder to form hexagon tubes with half the wall thickness and these are just stacked together to form the hexagonal fuel element so that the fabrication consists of extruding the single tubes. Well, it is this ratio that is an important economical factor and we think it is very promising.

FAST REACTOR SYSTEMS FOR SPACE POWER

Carl E. Johnson

U.S. Atomic Energy Commission
Washington, D.C.

Abstract

It appears that tens-of-kilowatts of electric power will be needed in space in the 1970's and hundreds-of-kilowatts in the 1980's. The Atomic Energy Commissions' reactor space power development program to meet these needs emphasizes three reactor concepts: the zirconium hydride thermal reactor for use with thermoelectric or dynamic conversion for the tens-of-kilowatts range in the 1970's, and the thermionic and advanced liquid metal cooled reactors for use with a variety of power conversion systems by around 1980. The paper discusses the development objectives of the space power reactor program, and describes an irradiation capsule and a liquid metal corrosion capsule which might be useful in other fast reactor projects.

INTRODUCTION

As we make use of our near-earth space capabilities and further explore the solar system, increasing amounts of electric power will be needed. As shown in Figure 1, we expect that long-lived space power supplies in the tens-of-kilowatts range will be needed during the 1970's, and that hundreds-of-kilowatts will be needed in the 1980's.[1] At these power levels, reactor power systems are attractive.

The objectives of the Atomic Energy Commission's space power reactor development program to meet these anticipated needs are illustrated in Figure 2. For the tens-of-kilowatts range in 1970's, we are developing a zirconium hydride thermal reactor. This reactor can be used with thermoelectric power conversion to provide 10 to 25 KWe in the early to mid 1970's. This same reactor can be used with NASA's mercury Rankine cycle, SNAP-8 system and possibly with Brayton or organic Rankine cycle power conversion when they are developed to provide up to 100 KWe.

The second range of power shown on Figure 2 illustrates where fast reactors are expected to play a role. This is in the 50-300 KW range during the 1980's. These advanced power plants are intended for use as advanced secondary power sources and for unmanned electric propulsion.

SPACE ELECTRIC POWER NEEDS

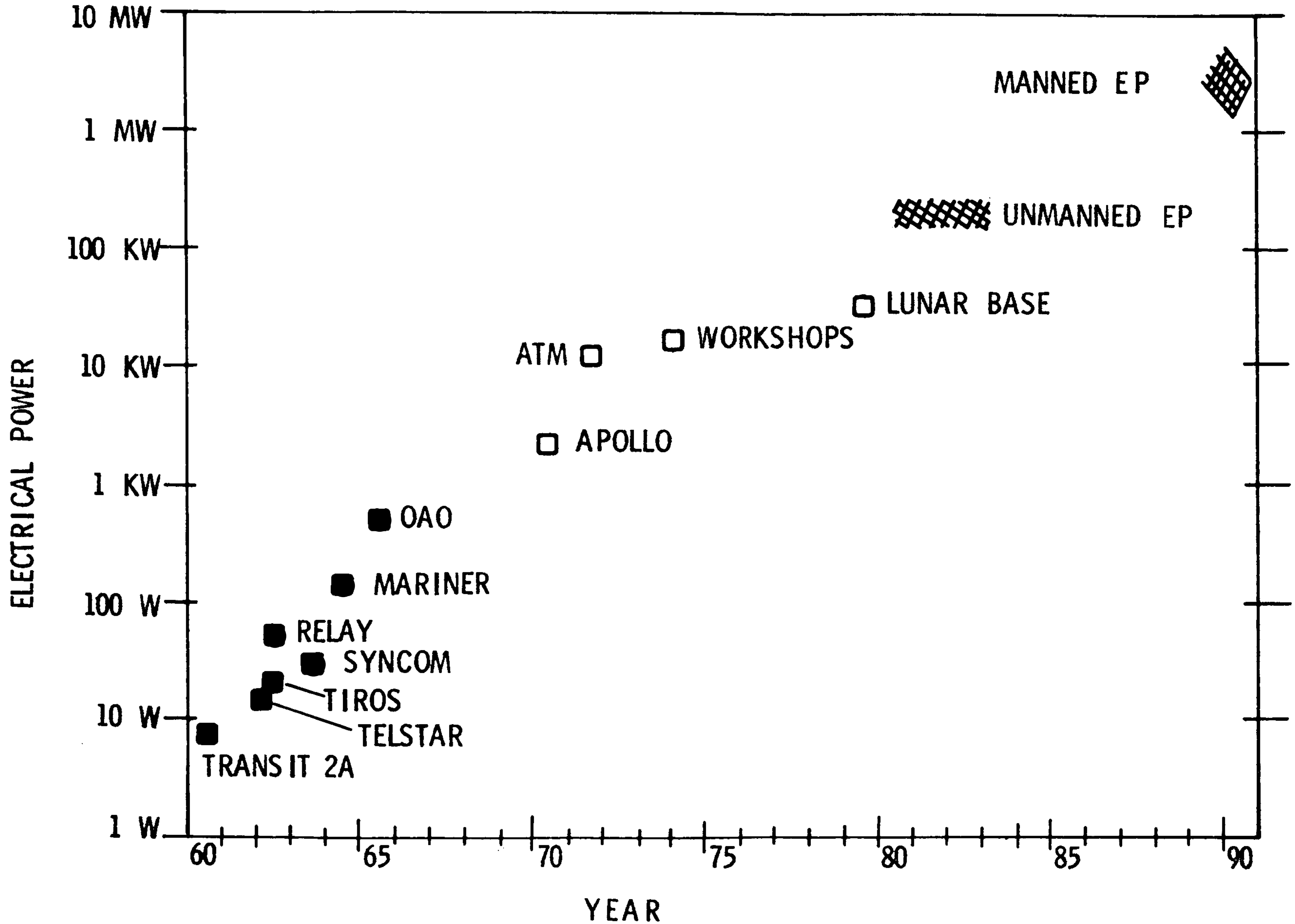


Figure 1

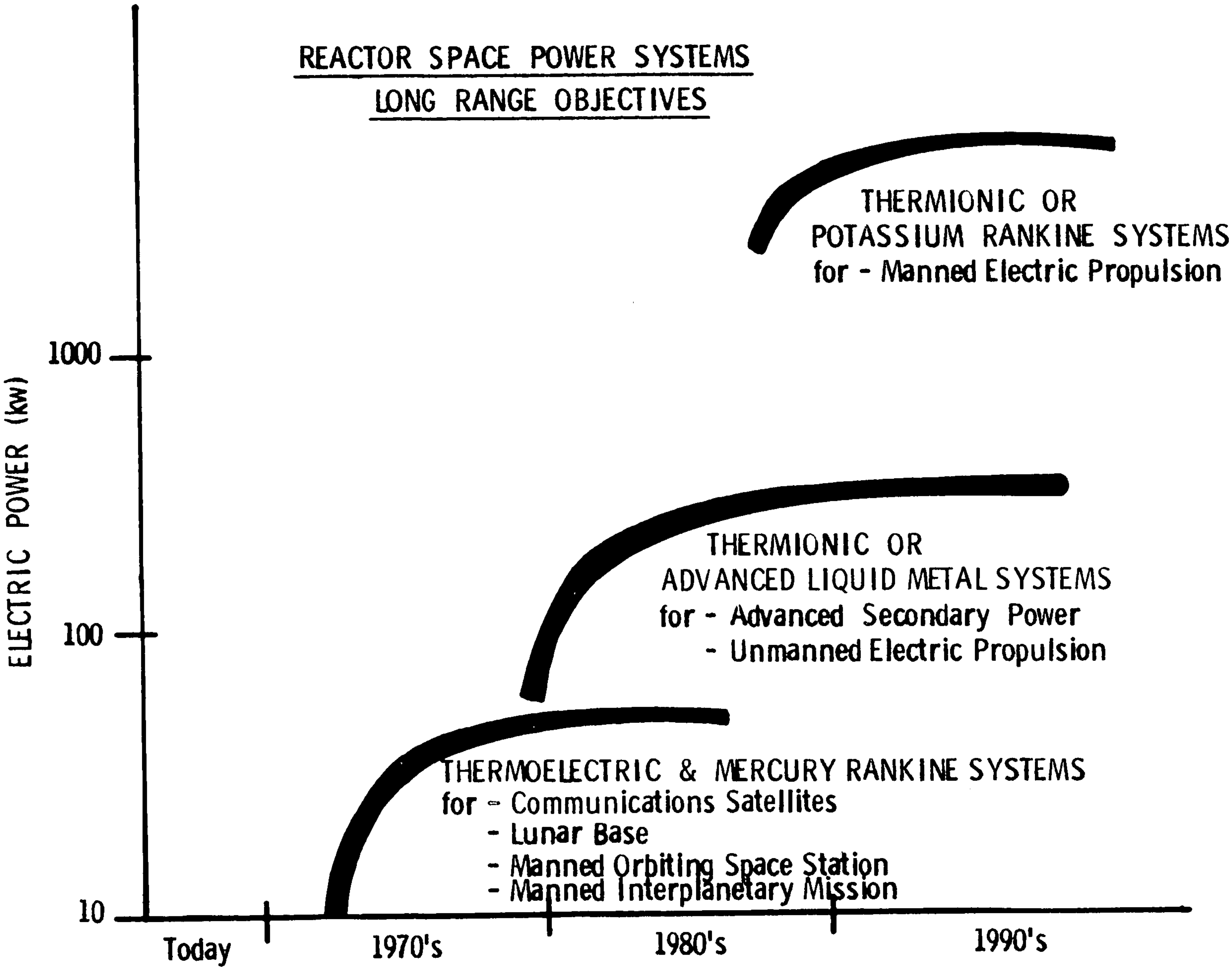


Figure 2

REV.97 S-214

The third category of reactor power systems is in the megawatt range for manned electric propulsion in the late 1980's or 1990's.

Our approach to developing the advanced, i.e., fast reactors, is shown in Figure 3. The initial objective for the advanced reactors is to develop a 50-300 KW power plant for secondary power and unmanned electric propulsion. In addition, we want to use this technology as a stepping stone to the megawatt-range power plants. The long term objectives for megawatt-range systems is for manned electric propulsion. Therefore, the power plant concepts which we plan to develop at the 50-300 KWe range for the 1980's should have the performance potential for reaching very low specific weight. These two advanced reactor concepts are the thermionic reactor and the advanced liquid metal cooled reactor primarily for use with a potassium Rankine cycle.

PERFORMANCE OF ALTERNATE POWER SYSTEMS

There are two basic reasons why fast reactors have been selected for primary emphasis in our advanced reactor program. These are: requirements for high energy densities and high temperatures. These temperatures for the advanced reactors must be a sufficiently large increase over the 1300°F zirconium hydride reactors to make the advanced technology worth working on. In order to achieve high energy density at these temperatures without excessive fuel burnup, most of the core volume should be composed of fuel such as UO_2, UN or UC.

The incentive for high temperatures for space power plants is illustrated in Figure 4. This graph[2] shows net specific radiator area required to reject waste heat from the power plant cycle, (i.e., total radiator area including power conditioning divided by net conditioned power output) as a function of fuel element surface temperatures in the reactor.* Figure 4 shows a clear trend toward reduced radiator area as the cycle top temperature goes up. This occurs because in order to increase the radiator temperature and still maintain a reasonable cycle efficiency the cycle top temperature must also go up. Figure 4 also shows the approximate temperature objectives of the three reactors which the AEC is developing. These are: the zirconium hydride reactor with a 1300°F coolant outlet temperature, the advanced liquid metal cooled reactor with a coolant outlet temperature around 2200°F, and the in-core thermionic reactor operating with an emitter-clad temperature over 3000°F, but with a coolant temperature around 1300°F.

Reactor energy density is important because reactor size affects the shield weight for given mission constraints. Figure 5,[3] for example, shows an approximate relationship between reactor core volume and fuel volume fraction.† The

---

*Radiators are assumed radiate to a sink temperature of -20°F with a product of (emittance) • (fin effectiveness) of 0.81. Power conditioning is assumed to be 85 percent efficient for the direct-current thermoelectric and thermionic power-plants and 90 percent efficient for turboalternator systems. Power conditioning waste heat is assumed to be rejected at 100°F.

†The minimum critical core volume shown in Figure 5 is computed with one-group cross sections[4] for a U^{235} fueled reactor, and assumes a two to three inch reflector savings.

ADVANCED REACTOR DEVELOPMENT APPROACH

EARLY OBJECTIVES

- 50-300 KW FOR SECONDARY POWER AND UNMANNED ELECTRIC PROPULSION
- TECHNOLOGY AS A STEPPING STONE TO HIGHER POWER

LONG TERM OBJECTIVES

- MEGAWATTS FOR MANNED ELECTRIC PROPULSION
- VERY LOW SYSTEM WEIGHT

Figure 3

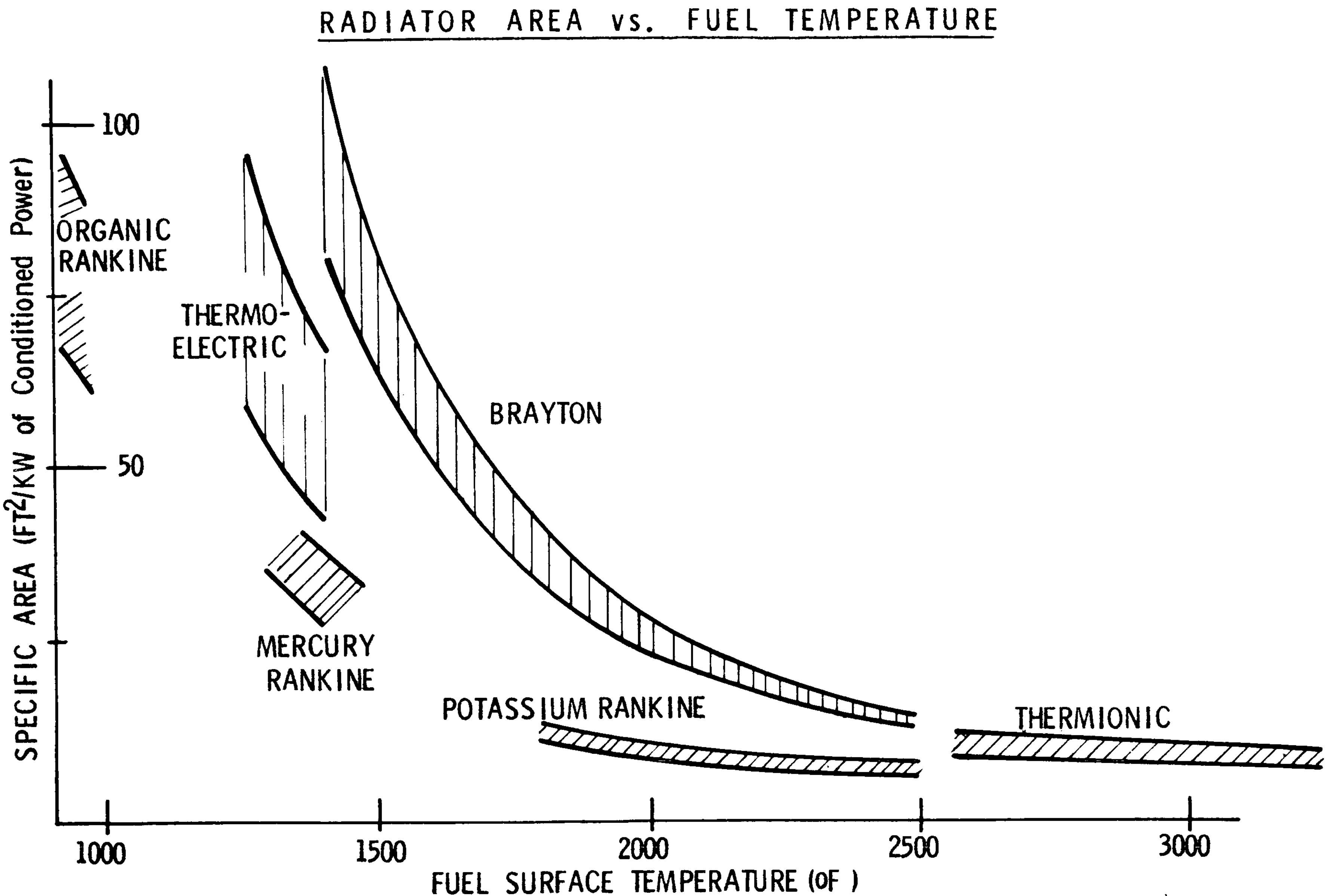

RADIATOR AREA vs. FUEL TEMPERATURE
100
50
ORGANIC RANKINE
THERMO-ELECTRIC
BRAYTON
MERCURY RANKINE
POTASSIUM RANKINE
THERMIONIC
SPECIFIC AREA (FT2/KW of Conditioned Power)
1000
1500
2000
2500
3000
FUEL SURFACE TEMPERATURE (OF)

Figure 4

CORE VOLUME vs. FUEL VOLUME FRACTION

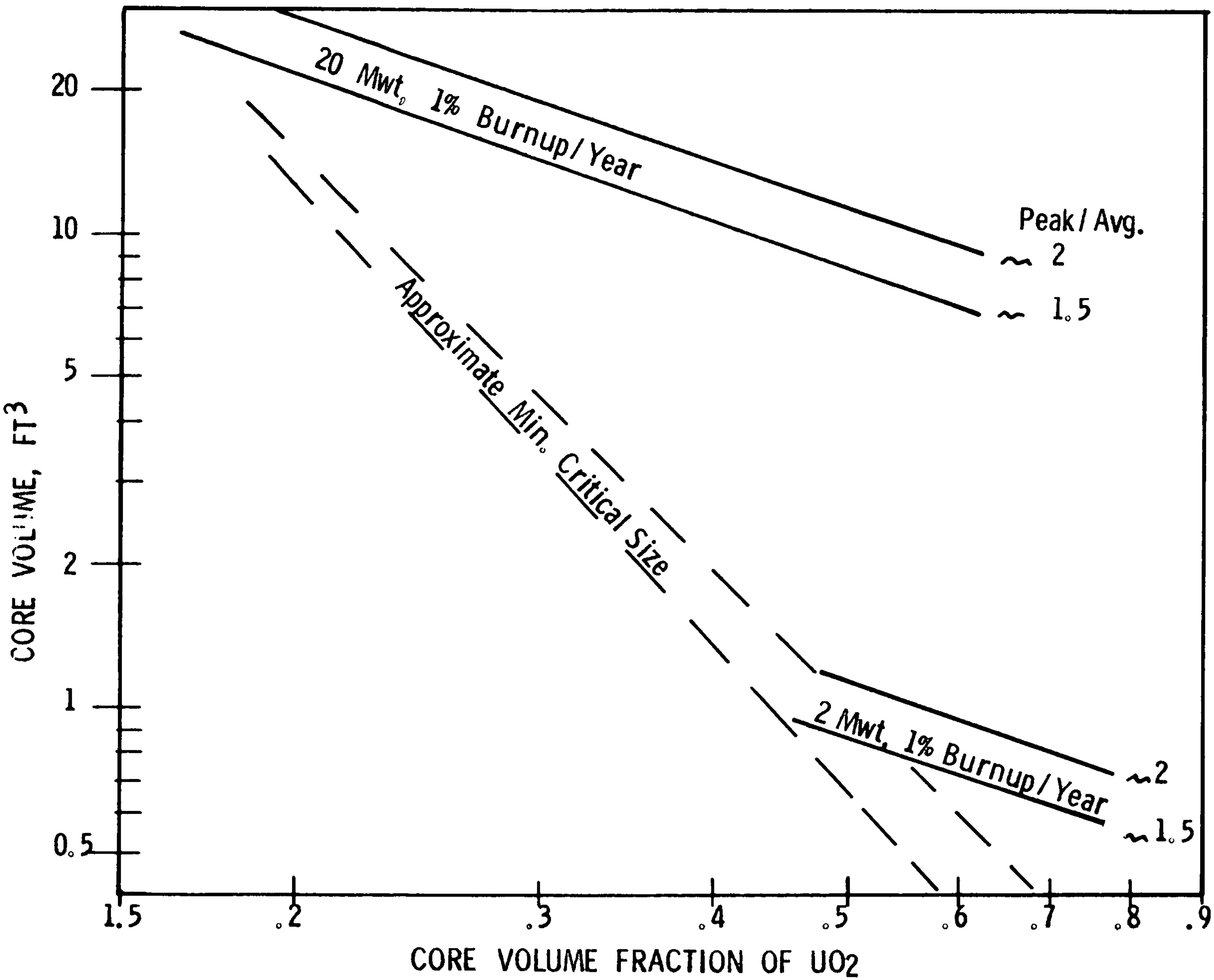


Figure 5

fast reactors we are planning on for the 50-300 KW range generally fall in one of two places on this graph. For example, a two MWt advanced liquid metal cooled reactor design typically has a fuel volume fraction around 55 to 60 percent, and generally falls on Figure 5 near the intersection of the criticality limit with the one percent burnup per year line for a 2 MWt reactor. In-core thermionic reactors on the other hand, generally have a fuel volume fraction around 30-40 percent and, therefore, tend to have a minimum critical core volume about three to four times larger than an equivalent liquid metal cooled reactor.

The effect of reactor core size on shield weight is illustrated in Figure 6 for two sets of arbitrarily assumed manned shield criteria; i.e., a dose rate of 10 mr/hr at a separation distance of 100 feet over a dose plant 100 feet in diameter and 30 feet in diameter.[5] For the core volumes in the two reactor examples discussed above, Figure 6 indicates that the larger diameter, manrated shield for a typical 2 MWt liquid metal cooled reactor would weigh about 17,000 pounds and an equivalent shield for a typical incore thermionic reactor would weigh about 22,000 to 25,000 pounds. In general, the weight of a shadow shield would be expected to be approximately proportional to reactor volume raised to the 1/3 to 2/3 power. For a criticality-limited reactor, therefore, shadow shield weight is approximately inversely proportional to the first or second power of the fuel volume fraction. For burnup limited reactors, shadow shield weight is approximately inversely proportional to fuel volume fraction raised to the 1/3 to 2/3 power.

The two items just discussed, i.e., the shield and the radiator, are typically the heaviest components in a reactor space power system. An estimate of the total shielded weight of various reactor power systems is illustrated in Figure 7.[2]* The weight advantage of the advanced reactor power plants appears particularly attractive in the power range above 50 to 100 KWe.

THERMIONIC REACTOR PROGRAM

The current objectives of the thermionic reactor program are to develop and demonstrate a long-lived thermionic fuel element and to demonstrate operation of a thermionic reactor experiment using this fuel element. A typical minimum critical size, U^{235} fueled thermionic reactor might be composed of about 1000 thermionic diodes. These diodes might be arranged as shown schematically on the right hand size of Figure 8. A dozen or so diodes might be wired together in series axially as in the so-called flashlight concept; or the diodes in each layer might be wired together in a parallel-series network as in the so-called unit-cell concept.

The steps in developing thermionic fuel elements are also illustrated in Figure 8. The initial experimental diodes are shown on the left hand side of Figure 8. These experimental diodes are intended to measure the performance of

*In Figure 7 the top of the weight range for each concept is for a manned shadow shield, for 10 mr/hr over a 75 foot diameter dose plane, 150 feet from the reactor. The bottom of the weight range is for an unmanned shadow shield for 10^{12} fast nvt and 10^6 rads of gamma rays over a 20 foot diameter dose plant 100 feet from the reactor.

SHIELD WEIGHT vs. CORE VOLUME

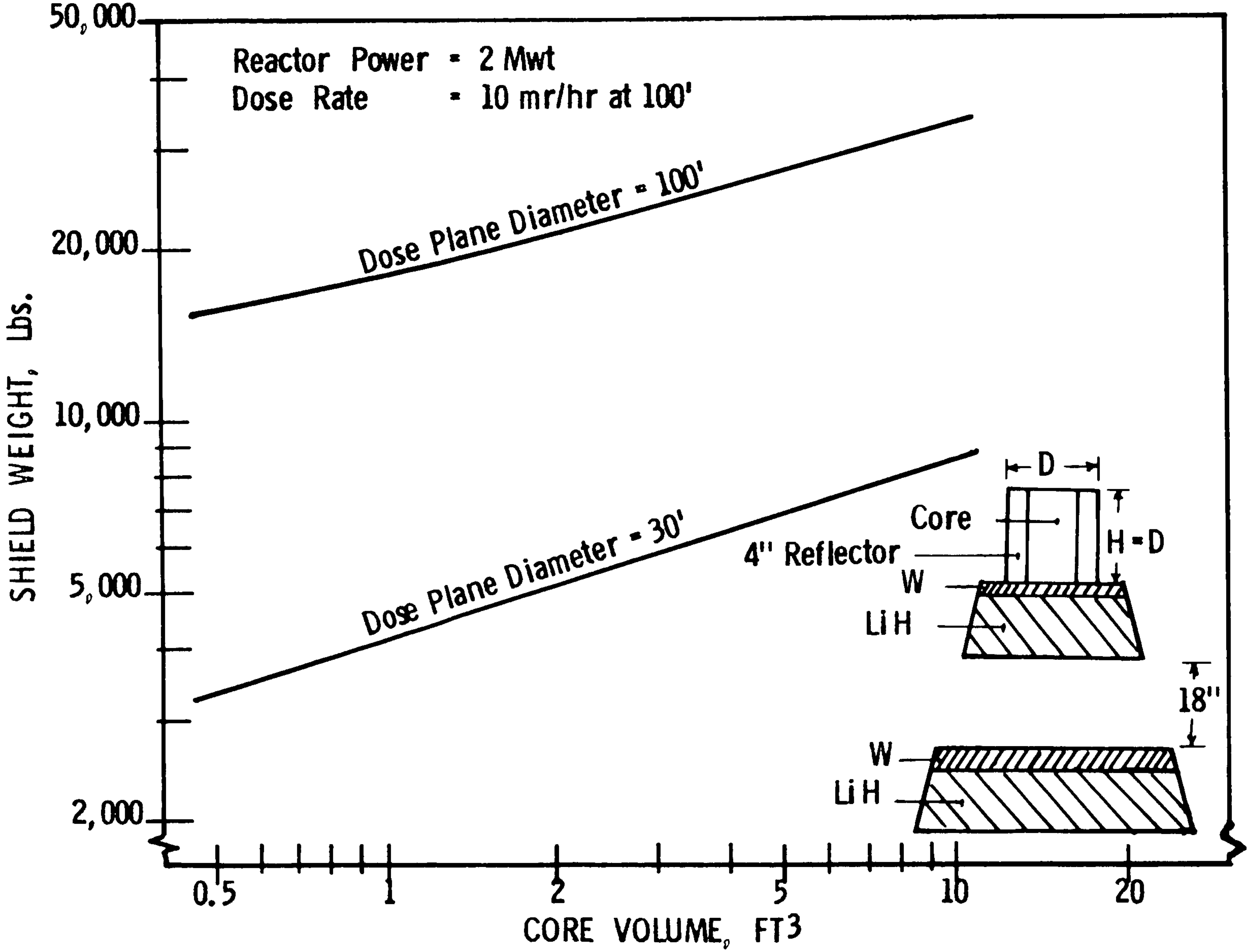


Figure 6

SHIELDED POWERPLANT WEIGHT

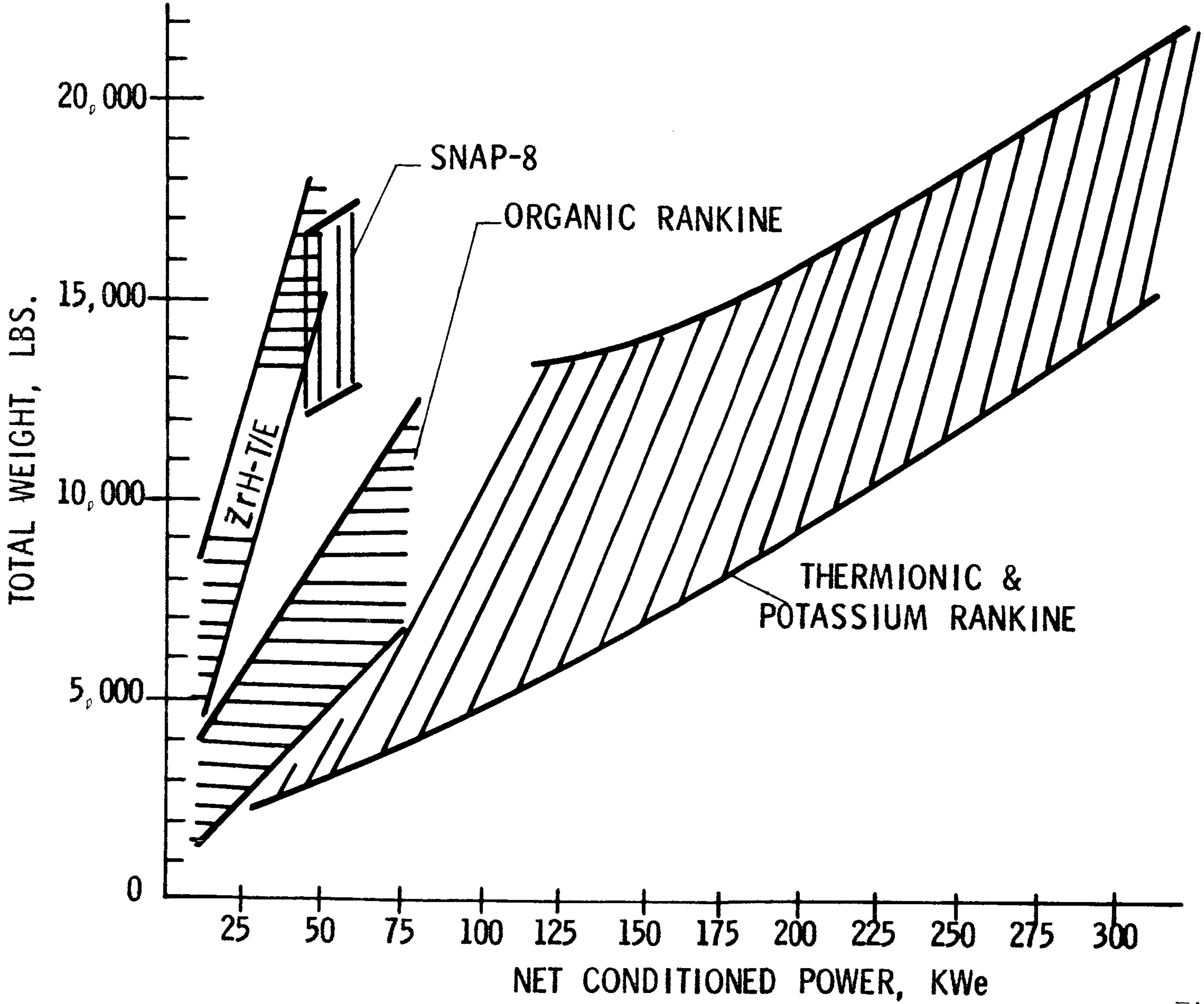
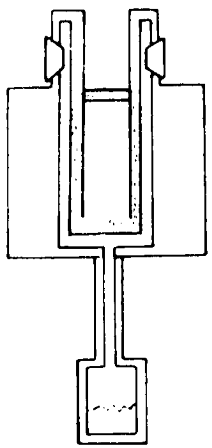


Figure 7

THERMIONIC REACTOR DEVELOPMENT STEPS

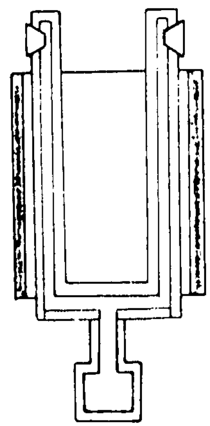
EXPERIMENTAL DIODE

- FUEL EMITTER
- THERMIONIC PERFORMANCE



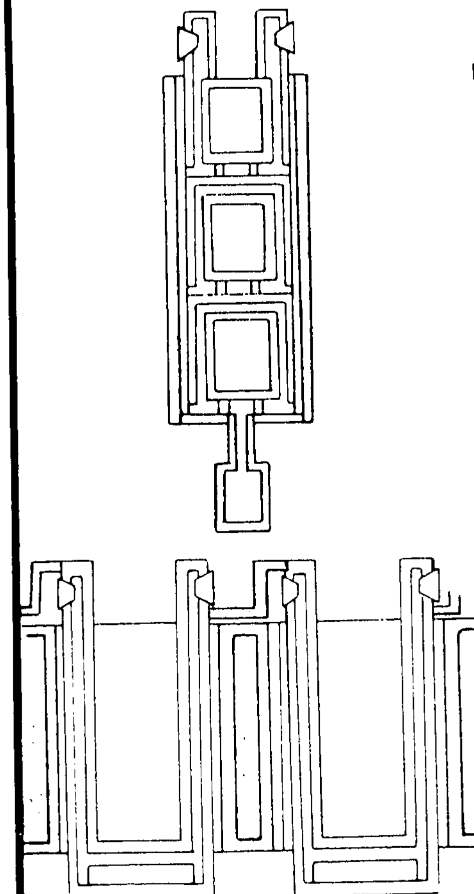
PROTOTYPE DIODE

- COLLECTOR SHEATH INSULATOR SHEATH
- SEAL
- LIQUID RESERVOIR



MULTIPLE DIODES

- INTERCELL LEADS
- FUEL ELEMENT SHEATH & STRUCTURE
- INTEGRAL CESIUM RESERVOIR



REACTOR EXPERIMENT

- REACTOR SYSTEM EFFECTS
- CONTROL

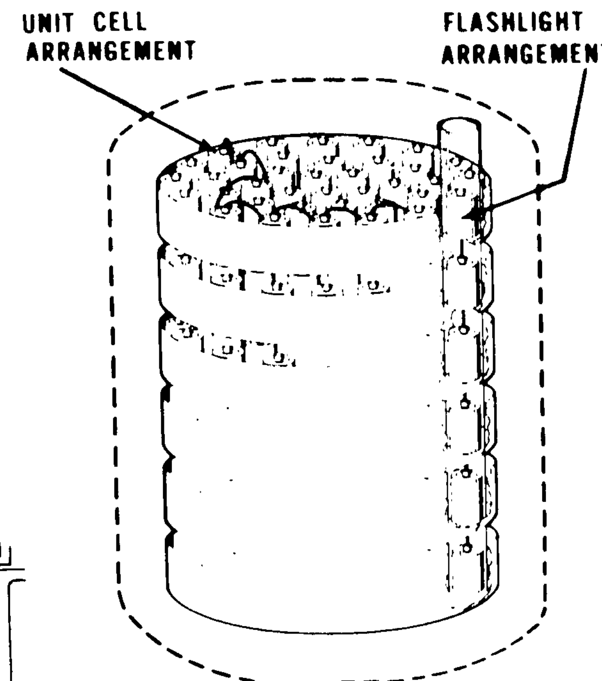


Figure 8

simple diodes during irradiation and to investigate irradiation and temperature effects on the high temperature fuel and emitter. This work is underway towards its objective of providing in-pile data on satisfactory fuel and emitters to 10,000 hours of operation.

The next steps in developing the fuel element involve adding to the basic diode all of the features which would be required in a reactor fuel element, although the dimensions of these prototype or multiple diodes may differ from the final fuel element design. The prototype diodes include the so-called tri-layer (i.e., a thin collector, the sheath insulator, and the external sheath) and a prototype seal insulator operating at the temperatures expected in the real fuel element. For the flashlight concept, prototype diodes are tested in a three-cell flashlight arrangement which includes the intercell electrical leads.

Most of our diode development effort is now going towards experimental diodes, single prototype cell diodes, and three-cell prototype flashlights. Full length 12-cell flashlight and multi-unit cell irradiations are planned in about two years. The experience of General Electric, Gulf General Atomic and Thermo Electron Corporation with in-pile operation of thermionic diodes is summarized in Figure 9. The longest operation of an experimental diode has been about 8000 hours, and prototype diodes have operated about 2000 hours.

Fast neutron irradiation effects on electrical insulators for use in thermionic fuel elements are also being evaluated. An irradiation capsule which the Los Alamos Scientific Laboratory has devised for this purpose may be of interest for other irradiation experiments. This irradiation capsule uses a heat pipe with a gas bulb to maintain a constant temperature on insulator materials irradiated in the EBR-II where instrument leads into the irradiation experiment are impractical. A heat pipe shown schematically in Figure 10, is basically a reflux condenser with a wick on the wall so that capillary action will pump the condensed liquid back to the evaporator section. The heat pipe concept has been applied to the LASL's irradiation capsule design as shown in Figure 11.[6] A similar concept is being investigated to regulate temperatures in a uranium nitride fuel irradiation capsule being designed by the Lawrence Radiation Laboratory.[4]

ADVANCED LIQUID METAL COOLED REACTOR PROGRAM

Some key features of the advanced liquid metal cooled reactor concept are summarized in Figure 12. The reference design is a minimum critical size, fast reactor with a thermal power level of about two MWt. The coolant outlet temperature will be about 2200°F. The reactor will be fueled with uranium nitride with a ductile tungsten alloy clad. The coolant will be lithium in a refractory metal alloy primary loop.

This reactor can be used with any of several types of power conversion. Potassium Rankine cycle power conversion would be used for a high performance power plant for use with electric propulsion. The reactor can also be used with a Brayton cycle or possibly with high temperature silicon-germanium thermo-electrics. A 1500°C heat pipe cooled version of this reactor is also being considered for use with out-of-core thermionics.

The primary emphasis in this program has been in materials development. Lawrence Radiation Laboratory has devised an interesting technique for liquid metal testing which may be applicable to other projects.

THERMIONIC DIODE IN-PILE EXPERIENCE

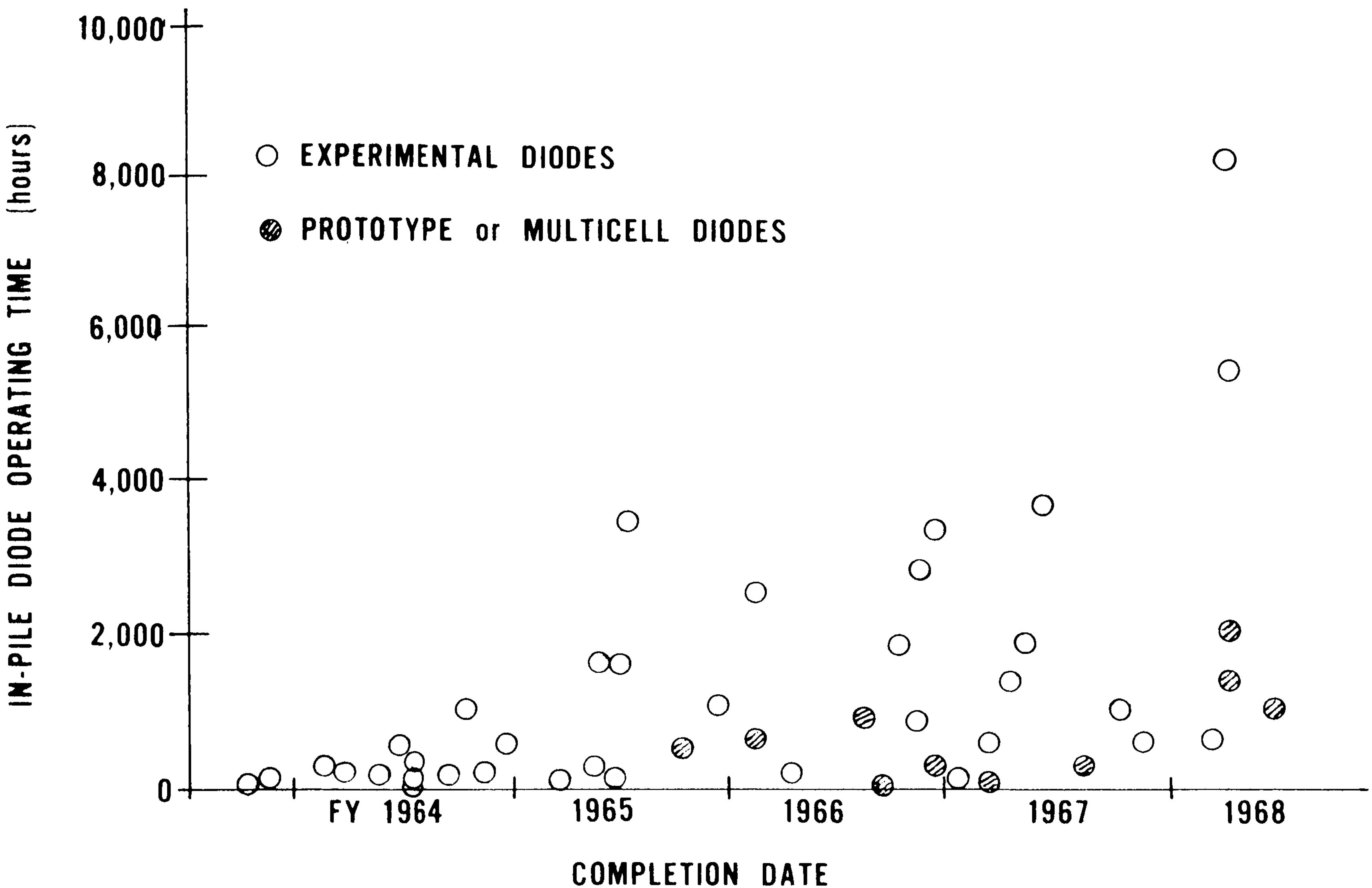


Figure 9

HEAT PIPE

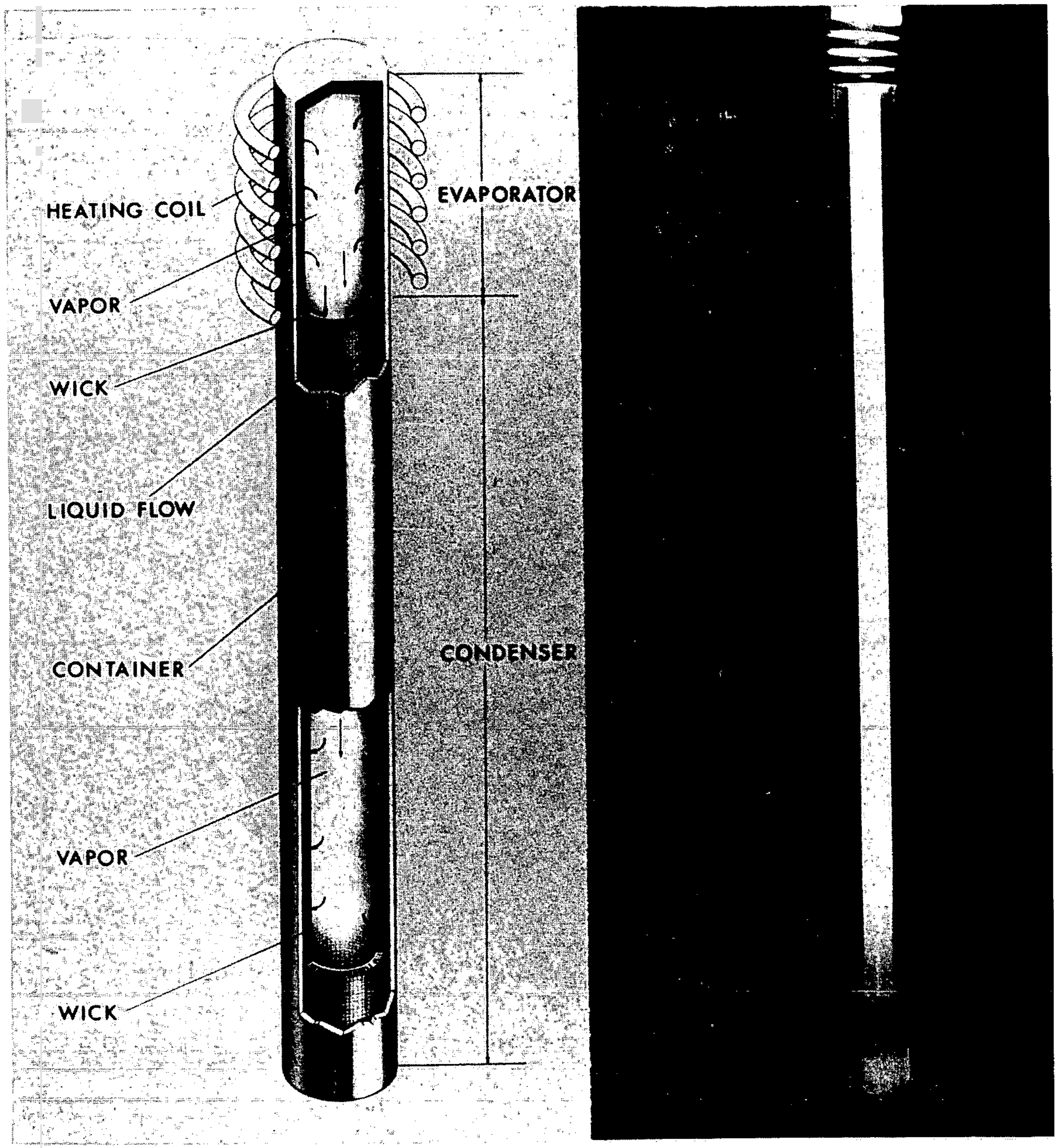
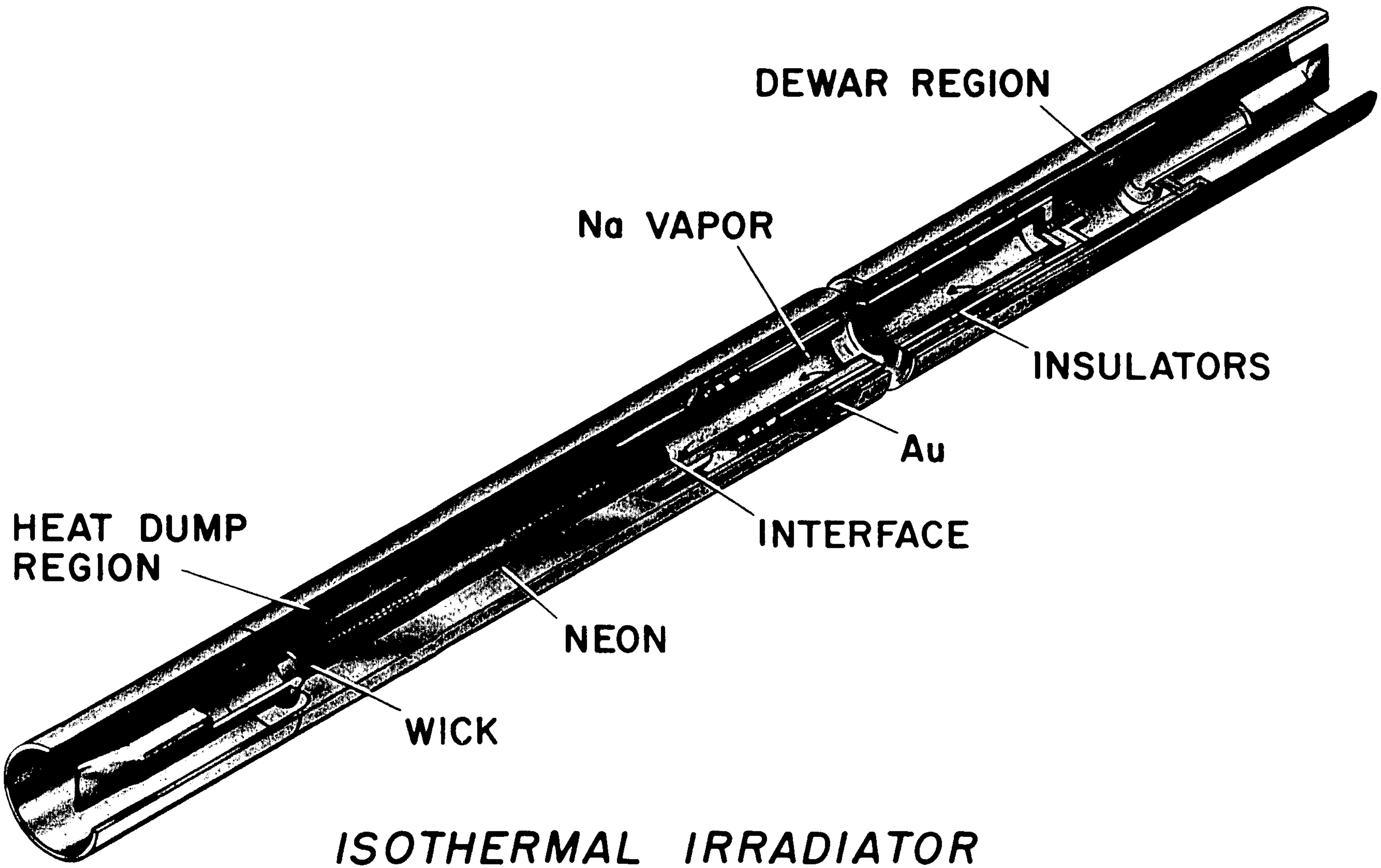


Figure 10



ISOTHERMAL IRRADIATOR

Figure 11

LIQUID-METAL-COOLED REACTOR CONCEPT SELECTION STATUS

		SELECTED	PROBABLE	TO BE SELECTED
REACTOR TYPE –	FAST SPECTRUM	✓		
REACTOR SIZE –	MINIMUM CRITICAL SIZE (~2 MWT)	✓		
REACTOR OUTLET TEMP –	APPROX. 2200°F		✓	
REACTOR MATERIALS –	URANIUM NITRIDE FUEL	✓		
	TUNGSTEN ALLOY CLAD	✓		
	LITHIUM COOLANT	✓		
	STRUCTURAL & PRIMARY LOOP MATERIALS (TUNGSTEN OR TANTALUM BASE ALLOYS)			✓

POSSIBLE TYPES OF POWER CONVERSION

RANKINE CYCLE

BRAYTON OR HIGH-TEMP THERMOELECTRICS

OUT-OF-CORE THERMIONICS
(REQUIRES HIGHER FUEL TEMP.)

Figure 12

This technique is a pumped capsule for screening tests on mass transfer in flowing liquid metal systems. The concept, shown in Figures 13 and 14, uses a relatively simple capsule instead of a loop. The basic idea is to place a membrane axially down the middle of the capsule so that flow can go up one side of the capsule and down the other side. One side of the capsule is made into an electromagnetic pump by flowing electricity across one side of the capsule and imposing a magnetic field perpendicular to the electrical current. One end of the capsule is then heated and radiator fins for cooling are attached to the other end. The capsule shown in Figure 14 was tested in lithium in a tungsten-molybdenum-rhenium alloy at 1400°C with a 200°C ΔT in a turbulent flow for 1000 hours.

SUMMARY

In summary, the space reactor power program is concentrating on three reactors as shown in Figure 15.

The first is a zirconium hydride reactor for use with either lead-telluride thermoelectric conversion or with dynamic conversion for use in the tens-of-kilowatts range in the 1970's.

Two reactor technologies are being developed for high performance, high power systems for the 1980's. The two competing advanced concepts are the thermionic reactor and the advanced liquid metal cooled reactor which is intended primarily for use with the potassium Rankine cycle but which has flexibility for use with a variety of power conversion systems.

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4. J.W. Hadley, Lawrence Radiation Laboratory, personal communication, 1968.
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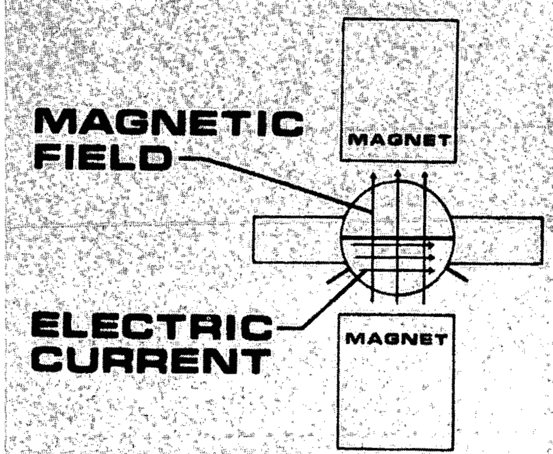
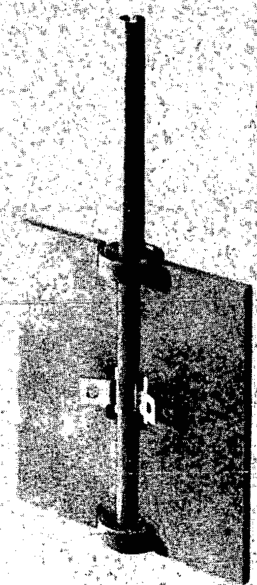
PUMPED CAPSULES

A SIMPLE METHOD OF DETERMINING CORROSION EFFECTS OF FLOWING LIQUID METALS WITHOUT USING EXPENSIVE, HARD-TO-FABRICATE CIRCULATING LOOPS.



PUMPING CONCEPT



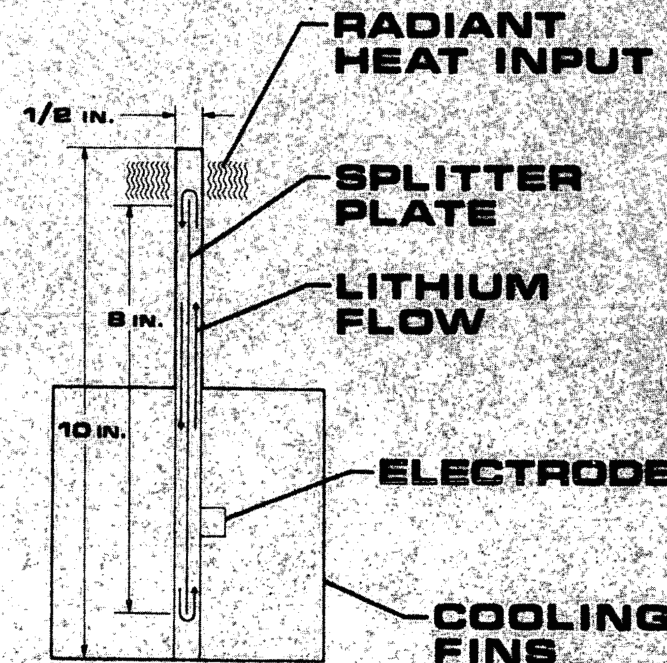
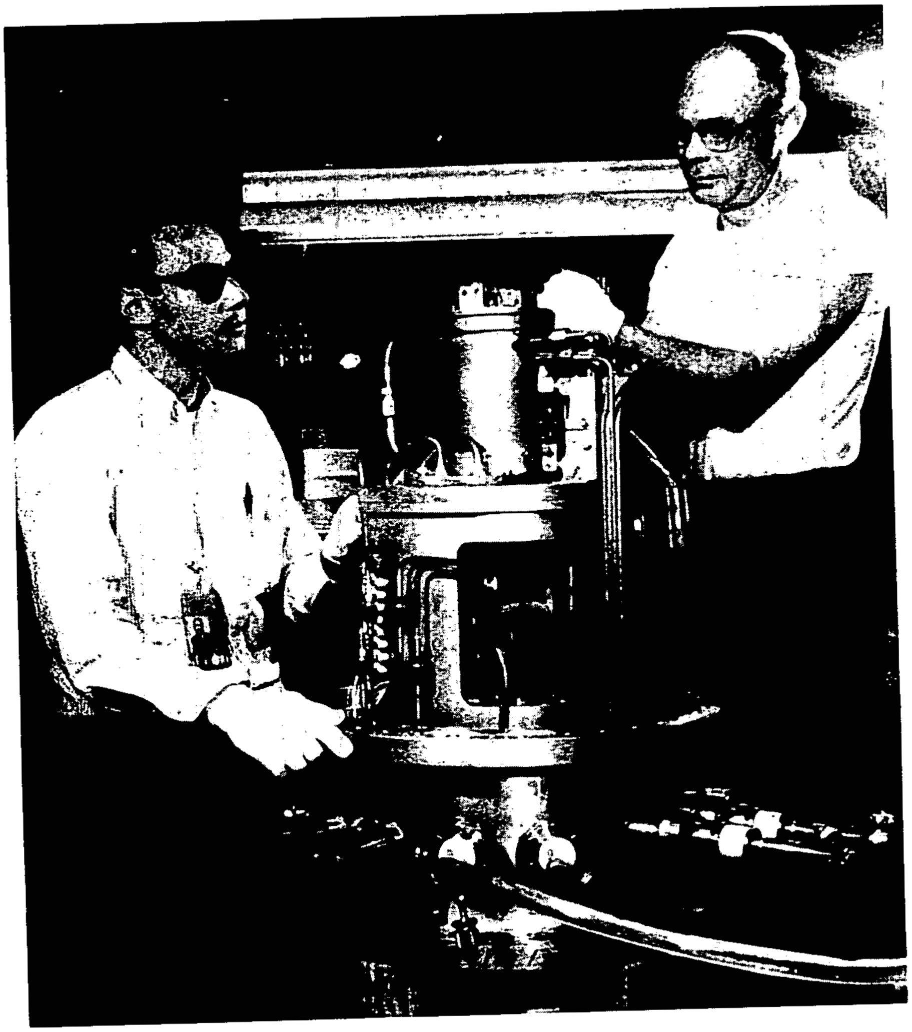


Figure 13



PUMPED CAPSULE TEST ASSEMBLY

Figure 14

REACTOR SPACE POWER SYSTEMS

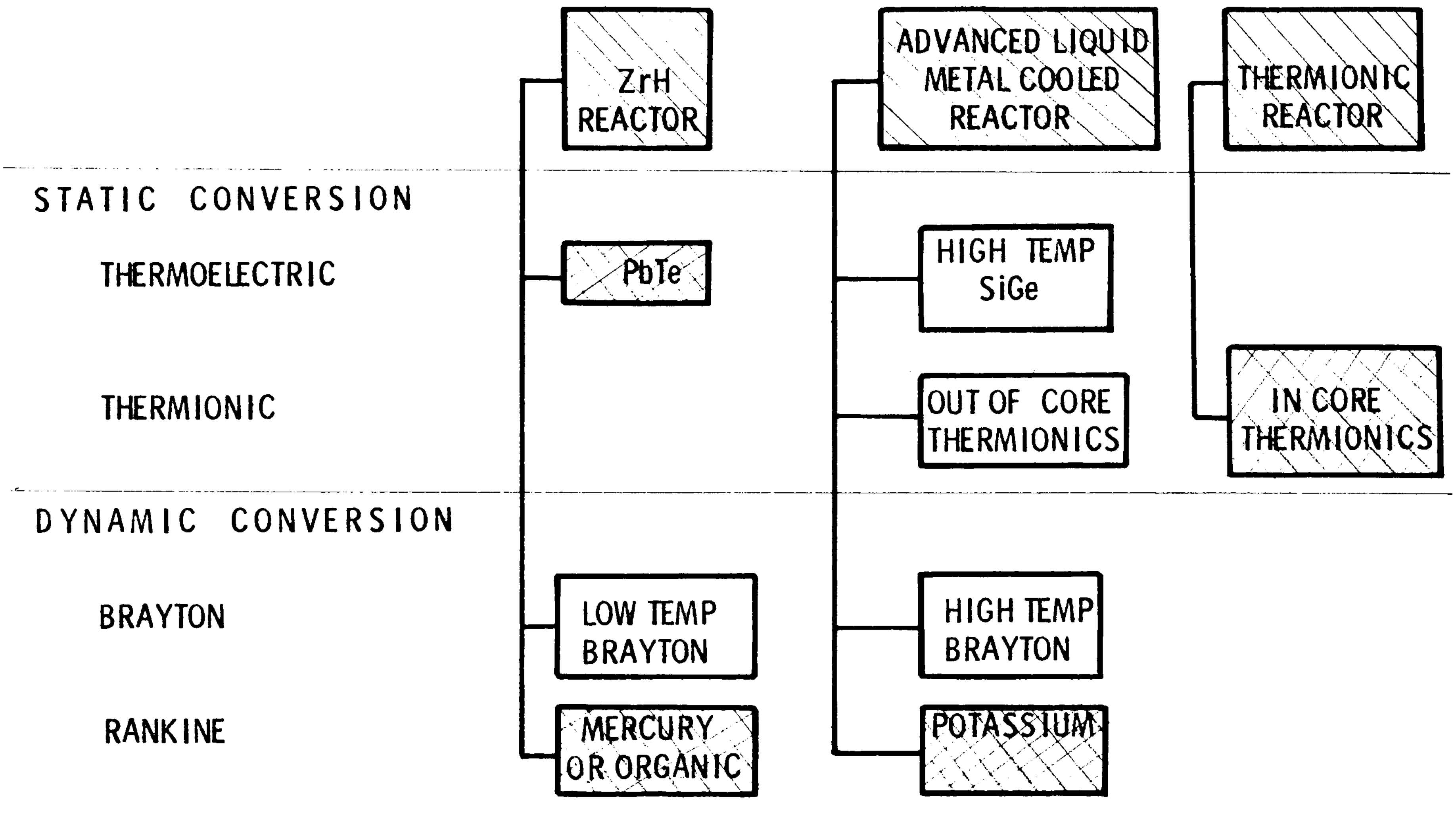


Figure 15

SESSION II

April 2, 1968

SYSTEMS REQUIREMENTS FOR FAST REACTORS

Chairman: Leonard J. Koch
Argonne National Laboratory

Local Co-ordinator: James H. Leonard
University of Cincinnati

FACTORS AFFECTING THE DESIGN
OF GAS-COOLED FAST BREEDER REACTORS

by G. Melese-d'Hospital
Gulf General Atomic Incorporated

ABSTRACT

Gas-Cooled Fast breeder Reactors are characterized by a great design flexibility because of the decoupling of gas temperature and pressure, the small coolant nuclear interaction, and the lack of chemical and neutron activation of helium. Performance, such as 10 years doubling time or 40% thermal efficiency, comparable to those of high-gain oxide-fueled LMFBR, are obtainable with GCFR at modest pressures (70-100 atm) and pressure drops ($\Delta p/p$ = 5%) for maximum clad temperatures of 650-700°C. Besides the advantage of easier gas technology compared to sodium, GCFR can benefit from better fuels such as carbides and from the simpler direct gas turbine cycle.

INTRODUCTION

When fast breeder reactors were first considered some twenty years ago, it was thought that only a liquid metal had the necessary heat transfer and transport properties to cool the high power density cores without too much neutronic penalty. Besides, sodium can operate at elevated temperature without high pressure because of its 881°C boiling point. The successful development of prestressed concrete pressure vessels containing the whole primary system of gas-cooled reactors in the early 1960's led to the real possibility of using a pressurized gas as a coolant for fast breeder power reactors.

Among the gases which may be considered, some are excluded for chemical or metallurgical reasons (air, hydrogen or CO), some for poor heat transfer properties (neon and argon), and some because of high neutron activation (nitrogen). The only three gases which have been utilized as coolants of thermal reactors, or proposed for fast reactors, are carbon dioxide, helium and steam. All of them are readily available, even helium, which can be extracted from the natural gas of the North Sea. As opposed to sodium, they are all compatible with water and air, do not boil and are transparent. Furthermore, they are not much activated by neutrons, may be used in a direct cycle or with an indirect steam cycle, and do not require the intermediate loop necessary with liquid metals. Therefore the capital cost with gas cooling is expected to be lower than with sodium cooling, perhaps as much as 10-15% lower for the same plant size. Besides, maintenance and reparability should be much easier with a gas than with a liquid metal.

There is little to choose between carbon dioxide, helium and steam from a heat transfer viewpoint (1-4) and all of them can lead to core thermal performance comparable to that of sodium. The data shown in Table 1 indicate that similar values of heat transport per unit flow area and surface heat flux can be obtained with sodium and with 100 atm helium in properly designed fast reactor cores. The cycle heat rating is 2-3% lower with sodium than with helium in view of the higher pumping power required by helium (for the same limiting clad temperature). Some of the thermodynamic advantage of sodium is lost in the intermediate sodium loop, and the net effect on the low fuel cycle cost is minimal.

Table 1

COMPARISON OF HELIUM AND SODIUM AS FAST REACTOR COOLANTS

Coolant	Helium	Sodium
Average pressure (atm)	100	1-7
Flow velocity (m/s)	100	5.5
Core Temperature rise (°C)	300	170
Heat transport per unit flow area $\rho v C_p \Delta T$ (Kw/cm^2)	100	100
Average film drop (°C)	200	20
Heat transfer coefficient (w/cm^2 °C)	1*	10
Surface heat flux (w/cm^2)	200	200

*Partial roughening of the fuel rod surface doubles the heat transfer coefficient and triples the friction factor.

Because of the higher density of steam, the pressure drop is much higher than with helium for the same coolant pressure and comparable performance, leading to more difficult mechanical and vibration problems. Furthermore, the breeding gain of steam-cooled fast reactors is very low because of the moderating and absorption properties of steam and the higher alpha values of plutonium in the softer neutron spectrum. In view of the large reactivity insertion caused by sudden loss of steam, drastic design compromises are needed to insure reactor safety. Finally, since reasonably low fuel cycle costs require a large fuel burnup, a satisfactory clad must be found with high strength, low neutron absorption, and good corrosion and irradiation behavior in high temperature, high pressure steam and fast neutron flux. Design improvements feasible with other gases, such as prestressed concrete pressure vessel or surface roughening, become less attractive with steam, which is both condensable and corrosive.

Much of the technology of gas-cooled thermal reactors (especially AGR and HTGR) is applicable to GCFR, as well as much of the physics and fuel element development program for LMFBR (5). Very good breeder performance is anticipated with helium because it does not bring any sizable neutron interaction and is inert. No undesirable design compromise is required with Gas-Cooled Fast Reactors which can be designed for minimum power costs.

GENERAL DESCRIPTION OF A 1000 MWe GCFR

Because of the small amount of reactivity invested in the helium coolant, much less than one dollar altogether, the GCFR core will not become prompt critical as a result of complete loss of coolant, as may be the case for sodium or steam cooled breeders. A core with high internal breeding ratio, (close to unity) allows batch refueling at long intervals, two-to-three years, without more than about one dollar of reactivity required for burnup control. Furthermore, in view of the harder neutron spectrum, the overall breeding ratio averaged over lifetime is approximately 1.5 with mixed PuO_2-UO_2 and 1.6 with carbide fuels. With helium cooling, the core design may be optimized for maximum performance and low fuel cycle cost without compromising reactor safety.

As mentioned previously, the whole reactor primary system is enclosed within a prestressed concrete pressure vessel (PCRV) including helium compressors, piping and steam generators. The main advantage of PCRV's for gas-cooled reactors has been discussed frequently (5,6): it is the small probability of catastrophic vessel failure, because of the multiple redundancy of the tension members (prestressing tendons), which are well protected from thermal and radiation effects and may be readily inspected and replaced. Furthermore, there is no main coolant piping outside of the vessel in the integrated design, and the coolant pressure is not limited by the vessel size. Also, site construction is much easier than with steel pressure vessels and no large parts have to be transported from the shop to the site. Large concrete vessels, 60 ft. internal diameter by 120 ft. long, have already been built at pressures up to 620 psi. Scale models designed for 1500 psi have been built and tested, and no special construction problems are expected up to about 2000 psi. Therefore, the coolant pressure in GCFR's could probably go up to about 150 atm.

Figure (1) shows a design of a PCRV for a 1000 MWe GCFR operating at 1000 psi, similar to that obtained during joint studies of Gulf General Atomic and the East Central Nuclear Group (7). The nuclear steam supply, with the reactor core, helium ducts, steam generators and helium circulators and drives, is integrated inside a prestressed concrete pressure vessel. Separate cavities have been provided for the steam generators and helium compressors in order to facilitate repair and maintenance. The concrete walls only see room temperature gas, thus eliminating the need for expensive thermal insulation.

Prestressed Concrete Pressure Vessel

The concrete vessel (PCRV) is horizontal with prismatic external shape approximately 128 ft. long and 55 ft. square. The internal cavity is nearly circular with an average diameter of about 32 ft. This vessel cavity is divided into three compartments by a cylindrical bulk shield structure: the central region, about 18 ft. in diameter, contains the reactor core; the other two compartments located on each side of the core are about 35 ft. long, and each contains two steam generators and three helium circulators. The prestressing tendon system comprises axial, circumferential and end tendons. The PCRV steel liner does not require thermal insulation since it only sees helium at a temperature of about 65°C (150°F). The concrete is maintained below 65°C (150°F) by the usual system of cooling coils embedded in the concrete and welded to the liner. The steam generator compartments are separated from the reactor compartment by 10 ft. thick bulk shield structures which may be water cooled. These partitions are designed to provide the necessary shielding to prevent activation of the heat exchangers. In order to prevent neutron streaming, the hot and cold helium ducts have two right angle bends within the bulk shield structure. In the steam generator compartments, flowing helium is contained within ducts and casings with external insulation. Thus the void space in these two compartments is maintained at about 65°C (150°F), permitting the installation of electrically operated controls and valves. Each of the two ends of the PCRV contains a centrally located 8 ft. diameter penetration for installation or removal of sections of the steam generators. One auxiliary helium circulator is mounted in each of these two large end penetrations; the main circulators are located in smaller penetrations in the ends of the PCRV. All penetrations are doubly contained, i.e., each PCRV penetration has two pressure seals, each designed for at least 2.5 times the design pressure. The prestressed concrete pressure vessel also serves as a biological shield for the reactor core since the concrete thickness is adequate if boron frits are incorporated in the inner portion of the concrete to reduce the production of capture gamma rays in the concrete.

Reactor Core

The reactor core is suspended from a deep grid plate which is kept at coolant inlet temperature with the gas flowing down through the core. The design of the grid plate is thus simplified and its reliability is increased. Furthermore, thermally induced distortions of the grid plate are reduced because the coolant inlet temperature is nearly constant across the core.

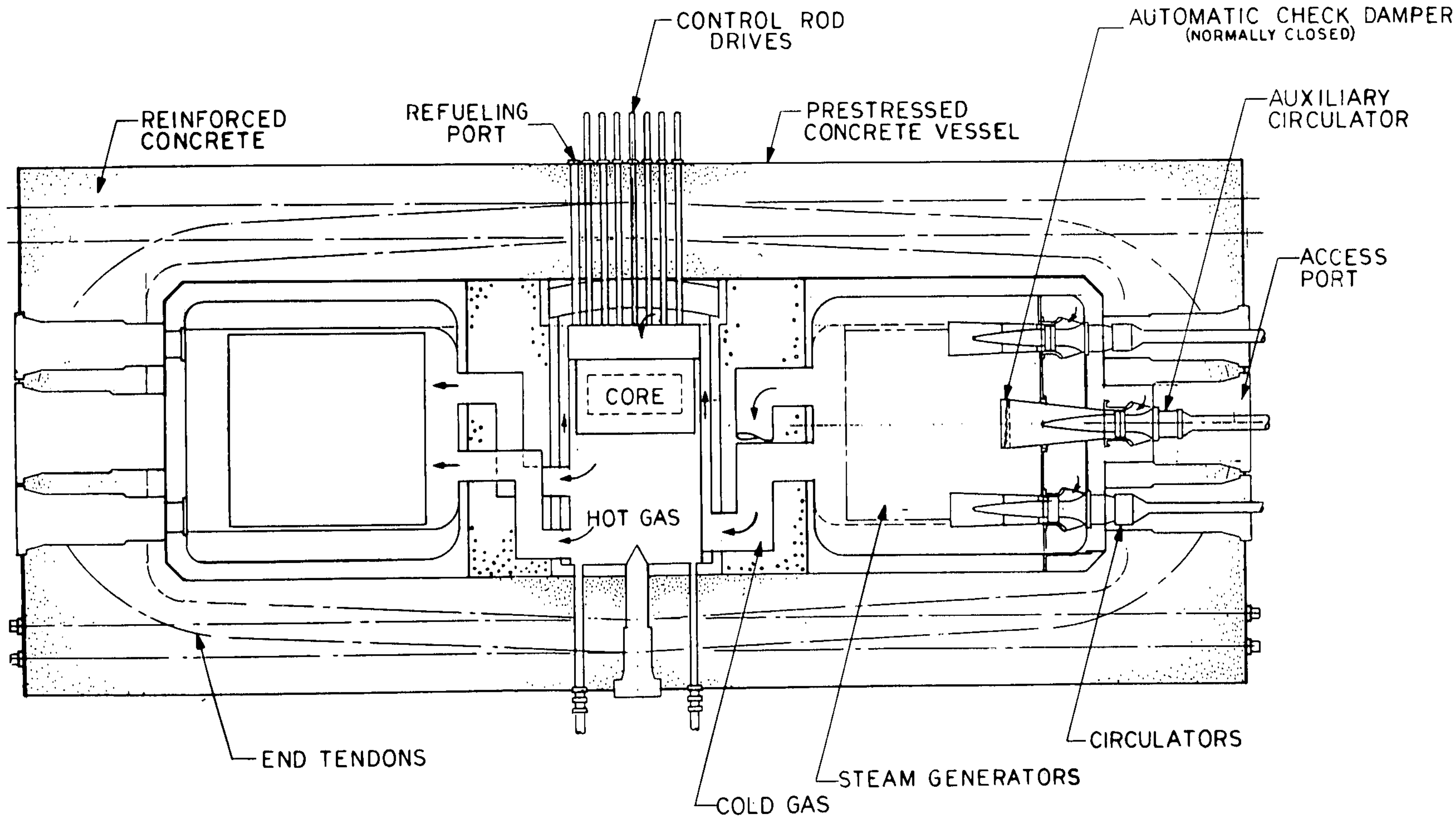


Figure 1 - Prestressed concrete reactor vessel for 1000 MWe GCFR

The depth of the grid plate is sufficient to eliminate any lateral motion of the cantilevered fuel elements. The control rods are located above the core in a region at coolant inlet temperature. The bottom of the core is free for fuel loading and unloading. Figure (2) shows a typical fuel element containing about 250 stainless steel clad fuel pins separated by spacers. There are approximately 280 fuel elements in the core and 30 control rod boxes, and about 200 radial blanket elements with the same overall dimensions but containing fewer and larger pins made of depleted UO_2. The axial blankets are integral parts of the fuel pin and also contain depleted UO_2. A collar is intalled in the center of each lattice cell at the top of the grid plate. The fuel element lifting device passes through this collar and is fastened to the grid plate with the fuel element locking tool. Each fuel element has a variable orifice to maintain constant gas outlet temperature across the core. The core and blanket are surrounded by thermal shields to reduce the neutron and gamma fluxes at the steel liner and concrete to acceptable levels.

Refueling

The reactor is refueled off load at atmospheric pressure with continuous gas cooling in the fuel transfer machine. There is a standpipe vessel penetration above each fuel element through which it is possible to retract fuel into the core and to manipulate the fuel from outside the vessel. The fuel handling and storage facility is located below the reactor compartment of the PCRV (Figure 3). Thus, the spent fuel elements can be transferred directly to the fuel storage pool without a fuel transfer cask. New fuel is loaded from the fuel handling room. The fuel elements are raised into the PCRV by the loading tool and installed in the fuel transfer machine, which then positions the element below the desired location in the grid plate. The fuel element is raised into position in the grid plate with the fuel element locking device which penetrates the top of the PCRV. The fuel element locking nut is located outside of the PCRV. The fuel transfer machine is simplified because the locking (and orificing) device performs the more complex operations during loading or unloading. Spent fuel, unloaded by reversing the loading procedure, is transferred to the storage pool where it will cool until it is ready for processing.

Blowers

Four two-stage axial helium compressors provide about 5% pressure rise and are driven by integrally-mounted series steam turbines. The series steam turbine is a single-stage axial machine located in the cold reheat line. A steam turbine drive permits short term overspeeding to insure adequate cooling after loss of coolant pressure with a reduced number of circulators in operation. In view of the redundancy of independent and widely separated main circulators, the most probable emergency to be dealt with is the loss of motive power rather than simultaneous mechanical failure of all four circulators. In this last case, the two auxiliary blowers, with a total power of about 1000 hp, located at each end of the PCRV, would provide the necessary coolant circulation. These circulators could be driven either by the main steam supply, or by an auxiliary source of steam.

FUEL HANDLING FACILITY
(END VIEW)

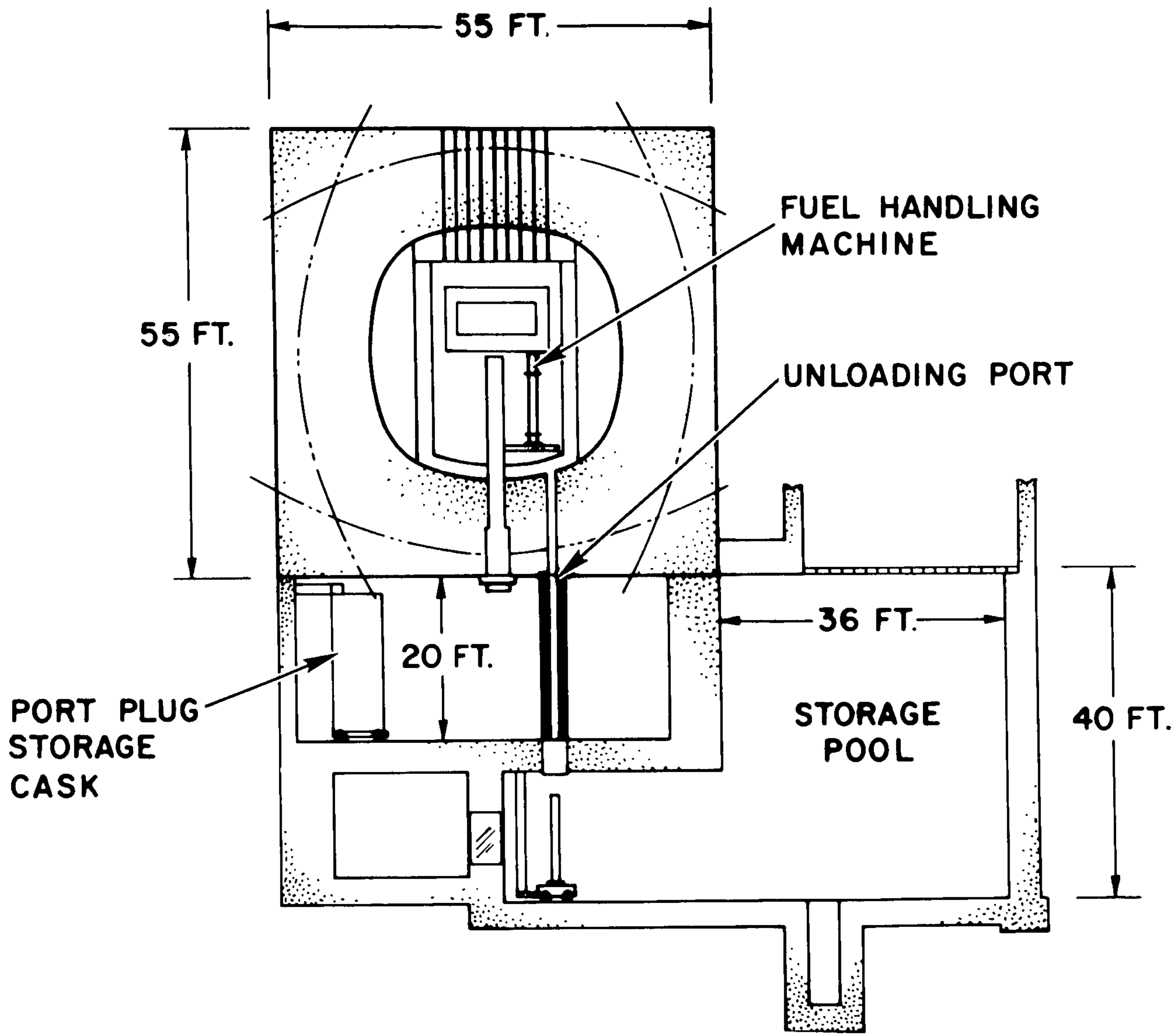


Figure 2 - Section through core and grid plate of 1000 MWe GCFR

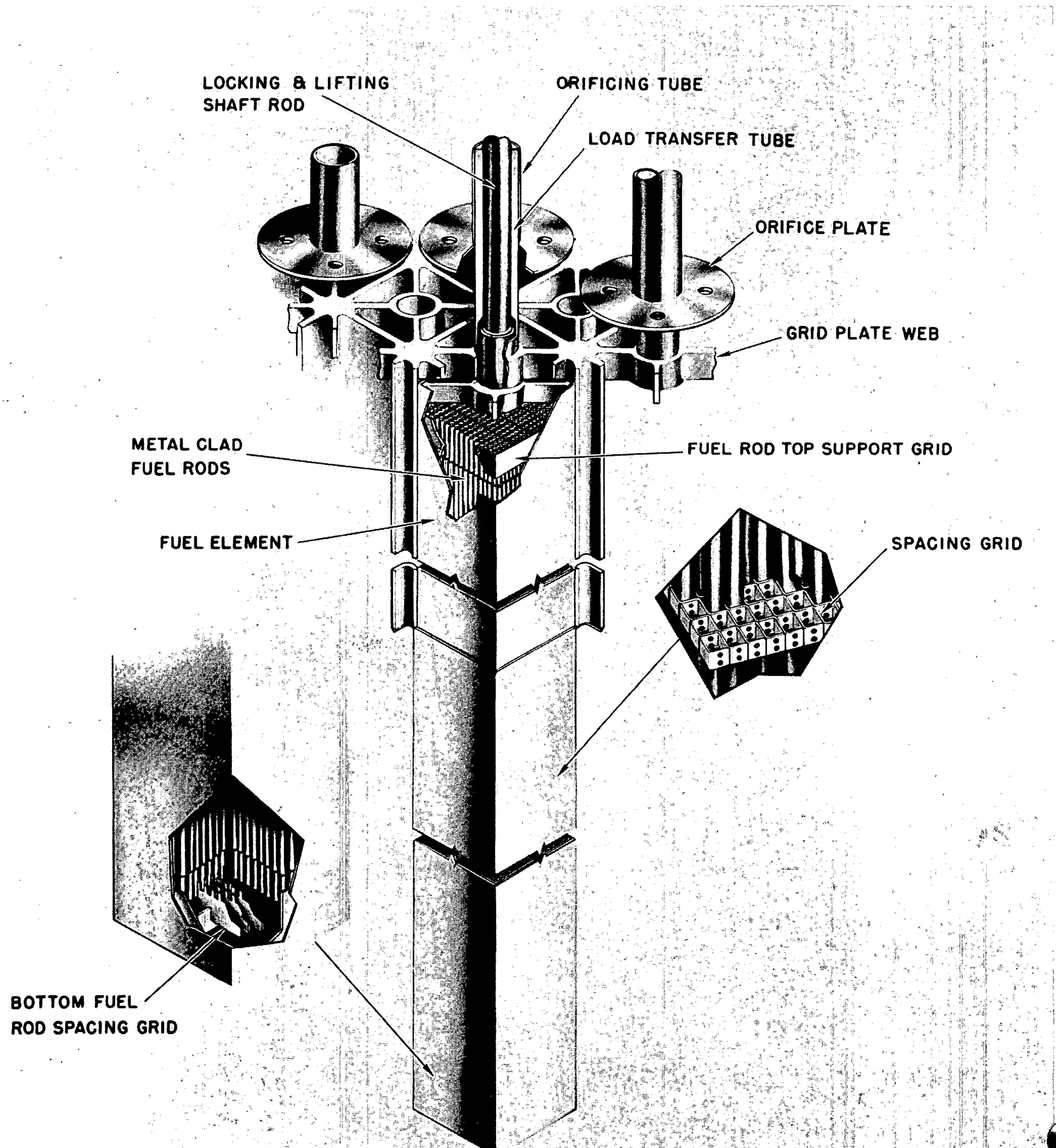


Figure 3 - Fuel handling facility for 1000 MWe GCFR

Loss of Coolant

Since the whole primary reactor system is completely enclosed within a prestressed concrete pressure vessel with doubly-contained penetrations, a sudden depressurization is considered very unlikely. Furthermore, with helium cooling, the reactivity introduced by complete loss of coolant is less than one dollar, thus allowing rapid insertion of safety rods without fear of fast power rise. In case of a sizable leak in the primary circuit, the reactor is assumed to be scrammed after a 5 second delay and feed heating to the boilers is reduced linearly with time over a period equal to one pressure decay time constant, leading to 100°C reactor inlet gas temperature. The reactor can survive without fuel clad meltdown severe coolant leakage accidents with exponential pressure decay time constants as short as 30-60 seconds, afterheat removal being provided by various combinations of main and auxiliary circulators.

INFLUENCE OF DESIGN PARAMETERS ON GCFR PERFORMANCE

Typical performance of a 1000 MWe helium-cooled fast breeder is shown on Table 2 with either mixed PuO_2-UO_2 or carbide fuels. The fuel enrichment is 12.7%, the radial maximum to average flux is kept at 1.17 by proper radial zoning, and a 27 cm axial reflector saving is assumed. The central fuel hole diameter is 20% of the rod diameter and the clad thickness is 4% of the diameter. This thin clad is permissible because of equalization of inside and outside can pressures. The maximum design clad temperature is 700°C, while the hot spot temperature of the stainless steel clad with 10% overpower is about 825°C. The frontal area blockage due to structure and control rods is 10% and the ratio of pumping to thermal power is 4%. Artificial roughening of the downstream part of the clad surface doubles the heat transfer coefficient and triples the corresponding friction factor. By increasing the coolant pressure from 85 to 119 atm and allowing a larger film drop, the linear rating and power density can approximately be doubled with mixed carbide fuels, which have both higher density and higher thermal conductivity than oxide fuels. The doubling time is halved with carbides because of the higher conversion ratio and specific power. Since the coolant temperatures must be lowered with carbide fuel to keep the same maximum clad temperature, the steam conditions are lower and the net thermodynamic cycle efficiency is slightly decreased. But the improved core performance with carbide leads to a decrease in fuel cycle cost from (1/2) mill/Kw-hr to less than (1/10) mill/Kw-hr for a 75,000 MWe assumed industry size. With a 15,000 MWe industry size, the fuel cycle costs would be respectively approximately 1 mill/Kw-hr and 0.6 mill Kw-hr.

Effect of Coolant Pressure

An average coolant pressure of 85 atmospheres was chosen for oxide fuel on Table 2 as a reasonable extrapolation of existing PCRV technology: the vessel to be built for the Fort St. Vrain HTGR will have a design pressure of 50 atm. In order to see the influence of the coolant pressure on GCFR performance, let us maintain a fixed maximum design clad temperature (700°C) and fixed helium inlet and outlet temperatures (340-640°C) and steam conditions. Figure (4) shows how core geometry and performance vary with core shape when both average coolant pressure and fractional pumping power are fixed. Core length, core volume, rod diameter and linear rating all increase with increasing core length to diameter ratio (L/D). The core power density and number of rods

Table 2

TYPICAL PERFORMANCE OF 1000 MW(e) HELIUM-COOLED FAST BREEDER

Fuel	Oxide	Carbide
Gas Pressure (atm)	85	119
Coolant Temperatures (°C)	340-635	315-587
Core L/D Ratio	0.60	0.50
Maximum Linear Rating (Kw/ft, w/cm)	17.1 (565)	33.5 (1100)
Core Volume (liter)	8590	4225
Active Core Length (cm)	157.5	110
Fuel Volume Fraction	0.30	0.29
Coolant Volume Fraction	0.54	0.55
Rod Diameter (cm)	0.795	0.765
Number of fuel Rods	39180	29010
Average Specific Power Total MWt/Kg Core Fissile (initial)	0.90	1.51
Average Core Power Density (MWt/liter)	0.273	0.586
Assumed Maximum Burnup (MWd/Kg)	100	140
Average Conversion Ratio (total)	1.51	1.60
Core Life (year)	2.36	2.44
Out of Pile Time (year)	1.0	0.5
Fractional Increase in Fissile Pu per Cycle	0.27	0.62
Geometric Doubling Time (year)	9.0	4.3
Steam Pressure (atm, psi)	163 (2400)	123 (1800)
Steam & Reheat Temperatures (°C, °F)	538 (1000)	482 (900)
Feedwater Temperature (°C, °F)	190 (375)	135 (275)
Net Plant Efficiency (%)	39.7	37.3
Fuel Cycle Cost (mill/Kw-hr)	0.50	0.08

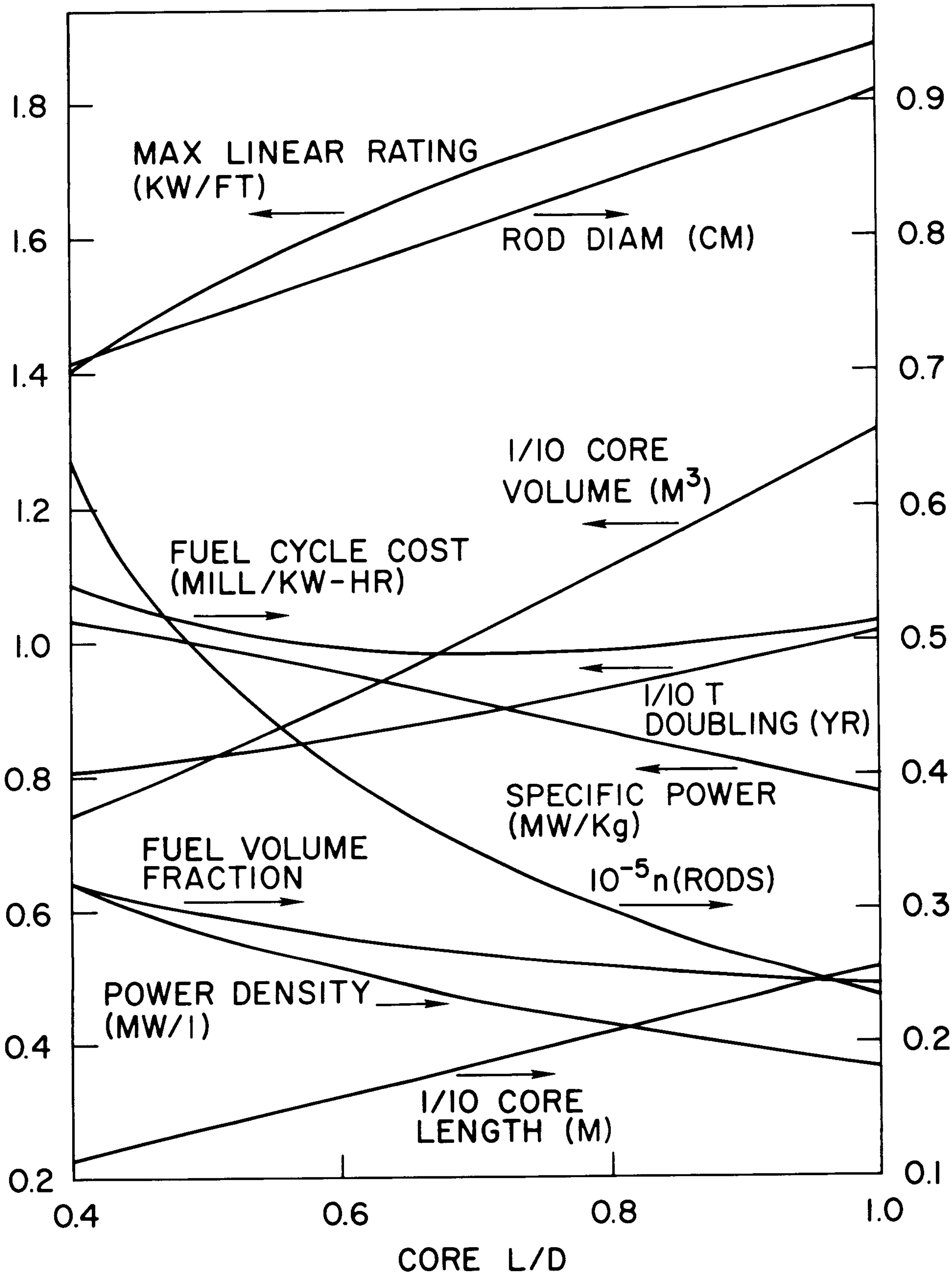


Figure 4 - Variations of 1000 MWe GCFR Performance with Core Shape for Fixed Maximum Clad Temperature (700°C), Coolant Temperatures (340-640°C) and Fractional Pumping Power (4%). Oxide fuel with 85 atm helium

decrease with increasing L/D, while the specific power and doubling time are close to optimum for the value L/D = 0.4. Since fuel cycle costs increase with increasing number of rods and increasing fuel inventory, a minimum fuel cycle cost is obtained at about 17 Kw/ft for a value L/D $\approxeq$ 0.7. At L/D = 0.5, the fuel cycle cost is only 4% (or 0.02 mill/Kw-hr) above its minimum, while the doubling time is only 2.5% above its minimum value of about 8 years. This core shape therefore gives a satisfactory compromise between low fuel cycle cost and low doubling time (or high specific power). Similar results are obtained at different pressure levels for fixed pumping fraction. Figure (5) shows the variations of the optimum core shape (for minimum fuel cycle cost), and the corresponding core parameters, versus the average coolant pressure. For pressures from 50 to 120 atm, the core length stays approximately constant while the optimum (L/D) and the rod diameter both increase. The power density, specific power and maximum linear rating increase with increasing coolant pressure while the doubling time, the number of rods and the fuel cycle cost decrease. But the comparison of reactor performance at different pressures is not very satisfactory because the maximum fuel temperature varies widely between the cases at 50 atm and at 120 atm, since the maximum linear rating increases from 9.6 to 24.5 Kw/ft. Comparison of core performance at various pressures for the same maximum linear rating is therefore preferable. The results of such a comparison at 16 Kw/ft are shown on Figure (6) for pressure variations from 70 to 120 atm. Core (L/D), core length and rod diameter all decrease with increasing pressure, while the specific power and power density both increase. The number of rods will increase approximately as the reciprocal of the core length, i.e., by a factor close to 2.5 between 70 and 120 atm. The fuel cycle cost reaches a minimum value of about 0.5 mill/Kw-hr for pressures between 85 and 95 atm. This minimum is very flat; the fuel cycle cost is within 5% of the minimum from 75 to 105 atm. A pressure of 85 atm therefore seems to be a reasonable compromise between cheaper PCRV construction (at 75 atm) and lower doubling time (at 105 atm). If the core shape and linear rating were fixed, specific power, power density and number of rods would increase with increasing pressure while core volume, rod diameter and doubling time would decrease. Either the inlet temperature, or the coolant temperature rise are assumed to stay constant. Fuel cycle costs decrease slightly with increasing pressure, by less than 5% from 80 to 120 atm.

Influence of Pumping Power

The amount of power spent for pumping affects both the cycle efficiency and the capital cost of the compressors and their drives. Furthermore, mechanical problems may arise in the case of large core pressure drop, and flow induced vibrations may be created by high dynamic head (much more severely for gases denser than helium). A single stage 40,000 hp centrifugal compressor appears to be feasible with a helium pressure ratio of 1.05. (An axial compressor would require two stages.) This corresponds to a ratio of total pumping power to thermal power of approximately 4%. The same pumping fraction would require a pressure ratio of 1.10 with CO_2. For fixed core shape (L/D between 0.4 and 1), maximum clad temperature (700°C) and helium temperatures (300 to 640°C), the total pumping fraction leading to minimum fuel cycle costs is approximately 6.5% for average coolant pressures from 50 to 120 atm. But the fuel cycle cost is only 5% above its minimum at a pumping fraction of 5%. When both maximum clad temperature and maximum linear rating are given, the

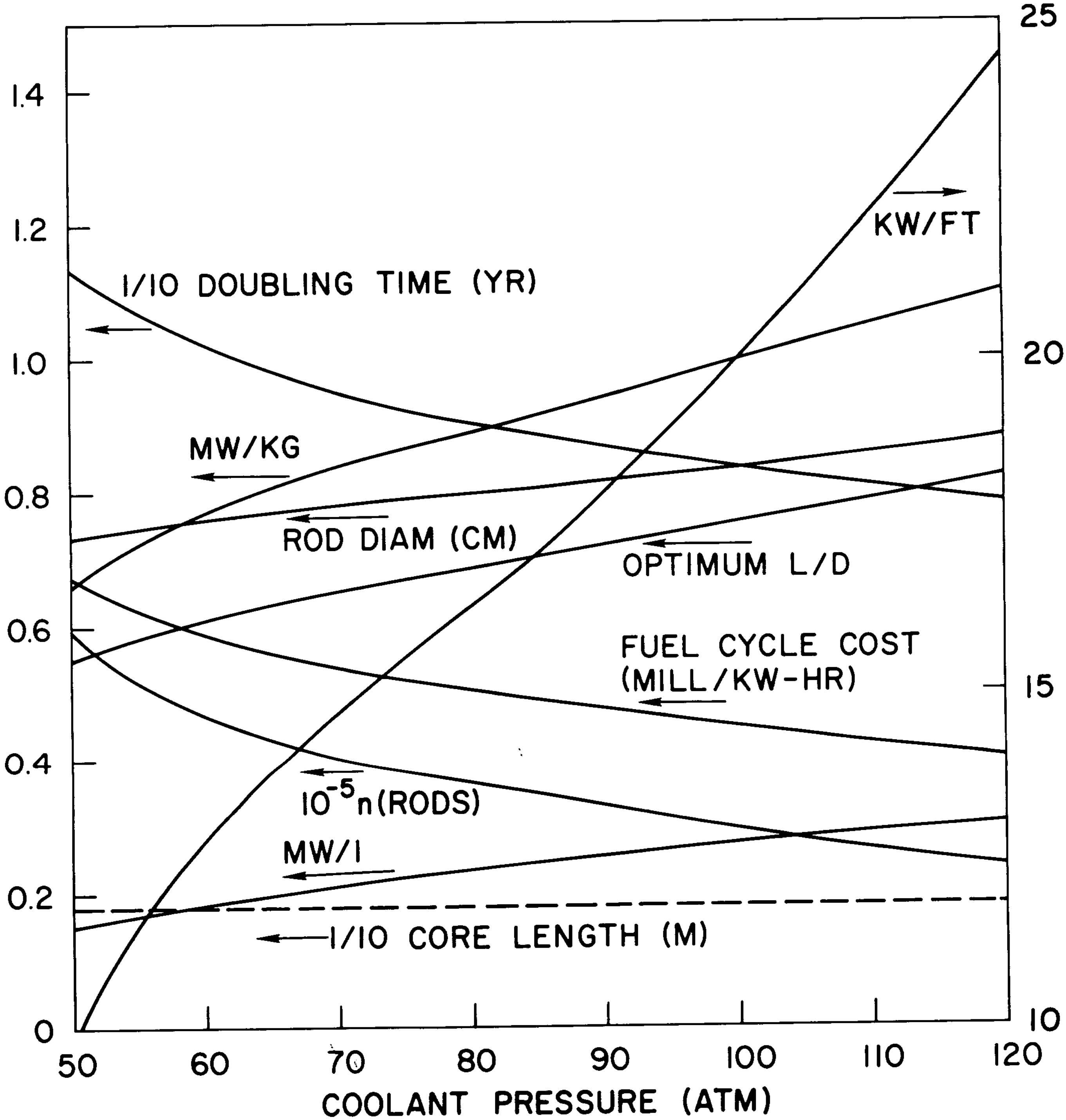


Figure 5 - Variations of Performance of 1000 MWe GCFR with Coolant Pressure for Optimum (L/D) Leading to Minimum Fuel Cycle Cost, for Fixed Maximum Clad Temperature (700°C), Ratio of Pumping Power to Thermal Power (4%) and Helium Temperatures (340-640°C)

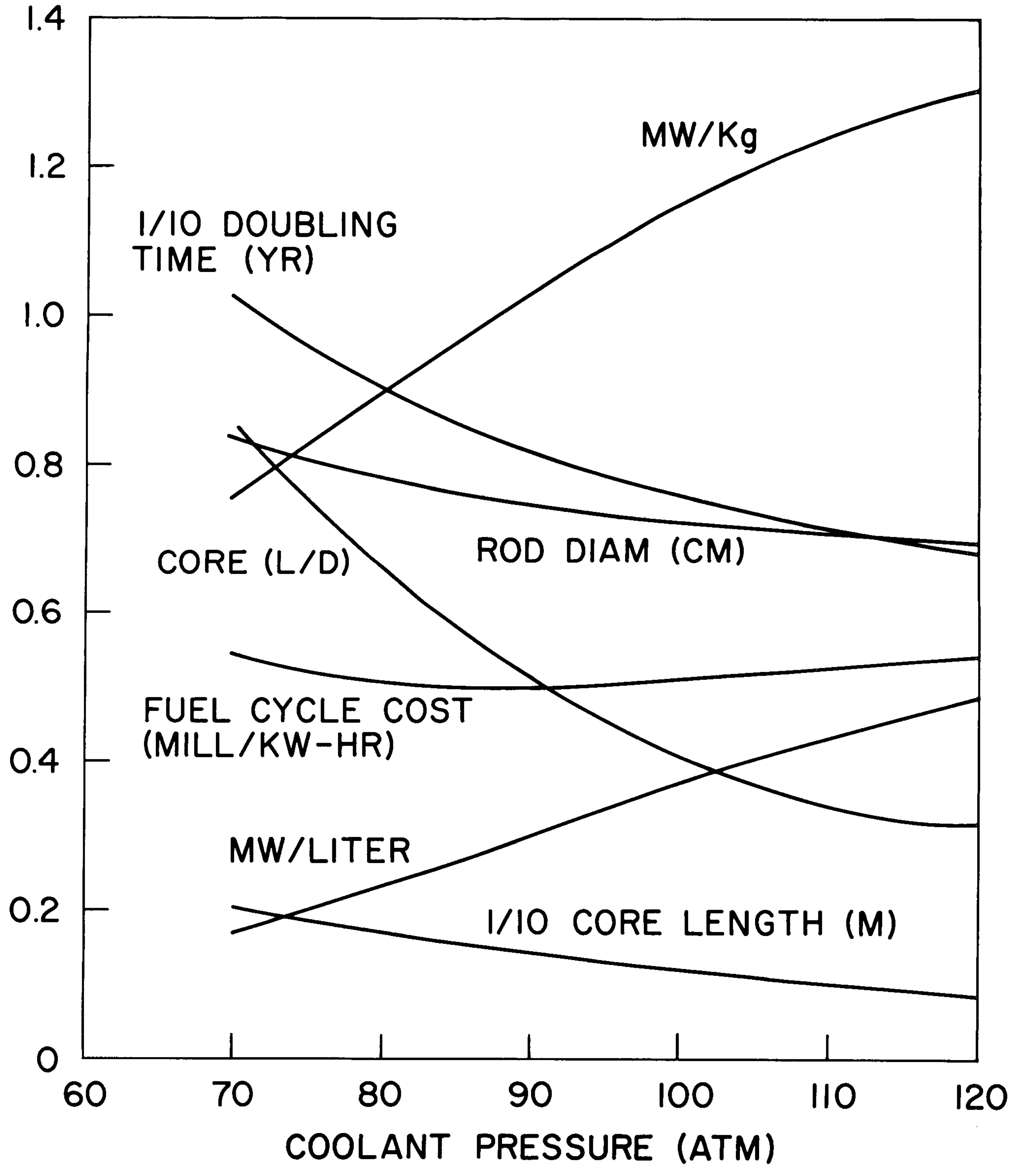


Figure 6 - Variations of Performance of 1000 MWe GCFR with Coolant Pressure for Fixed Maximum Linear Rating (16 KW/ft), Maximum Clad Temperature (700°C), Ratio of Pumping Power to Thermal Power (4%) and Helium Temperature (340-640°C)

pumping fraction leading to minimum fuel cycle cost increases with increasing coolant pressure at a given maximum linear rating. For instance, at 85 atm and for a maximum linear rating of 16 Kw/ft, the total pumping fraction leading to minimum fuel cycle cost is approximately 4.3% (Figure 7). But the fuel cycle cost is increased by only about 5% (or 0.02 mill/Kw-hr) for a pumping fraction of 3.4%. Similarly, the economic optimum (for low total power cost) at 85 atm and 18 Kw/ft corresponds approximately to the 4% pumping fraction mentioned previously (see Tables 2-4).

Clad and Fuel Temperatures

Other important design parameters are the maximum permissible clad temperature and the linear rating, which obviously also affect the maximum internal fuel temperature. For a maximum linear rating fixed at 18 Kw/ft, Table 3 shows the variations of performance of a 1000 MWe helium-cooled breeder (with oxide fuel) versus maximum clad temperature. The core shape is fixed and near optimum (L/D = 0.6) and the pumping fraction remains 4%. By proper choice of coolant temperatures, the specific power and the power density may be kept approximately constant for maximum clad temperatures between 600 and 700°C. An increase in thermodynamic cycle efficiency and a decrease in core volume and number of rods lead to a decrease in fuel cycle cost, but only by 10% (or 0.05 mill/Kw-hr). On Table 4, we have varied the maximum linear rating and maximum clad temperature together and showed three designs with decreasing degrees of conservatism. By properly adjusting coolant temperatures, the specific power and power density are about the same for all three designs. The higher core volume and larger number of rods of the most conservative design increases the fuel cycle cost, but only by about 1/10 mill/Kw-hr when going from a very conservative design (12 Kw/ft, 600°C) to a more reasonable design (18 Kw/ft, 700°C) which should avoid fuel melting at the hot spot.

Surface Roughening

We have also shown on Table 4, the performance obtainable with smooth as well as rough clad surfaces. Comparing smooth and rough cores for the same maximum clad temperature and pumping fraction, we see that the coolant temperature must be lower with smooth rods in order to obtain reasonable power density, specific power and power rating. In order to reduce the number of smooth rods, the core must be much longer. The fuel cycle cost is about 25% higher with smooth rods, the cycle efficiency is somewhat lower and the specific power is only 80% of the value for roughened rods. A detailed study of the optimum degree of surface roughening and the corresponding optimum core shape has already been reported (8). More recent data on heat transfer and friction factor of bundles of artificially roughened rods (9) essentially confirm the experimental data used in Ref. (8). These studies have also been extended to CO_2 cooling and carbide fuels. When inlet and outlet coolant pressure and coolant inlet temperature are fixed, and maximum surface and internal fuel temperatures are kept constant, the core shape leading to minimum fuel cycle cost is such that the ratio of active core length to diameter is approximately 0.6. This result is valid for a range of maximum clad temperatures (at least 600-700°C), maximum linear ratings (10-20 Kw/ft for oxide and 20-40 Kw/ft for

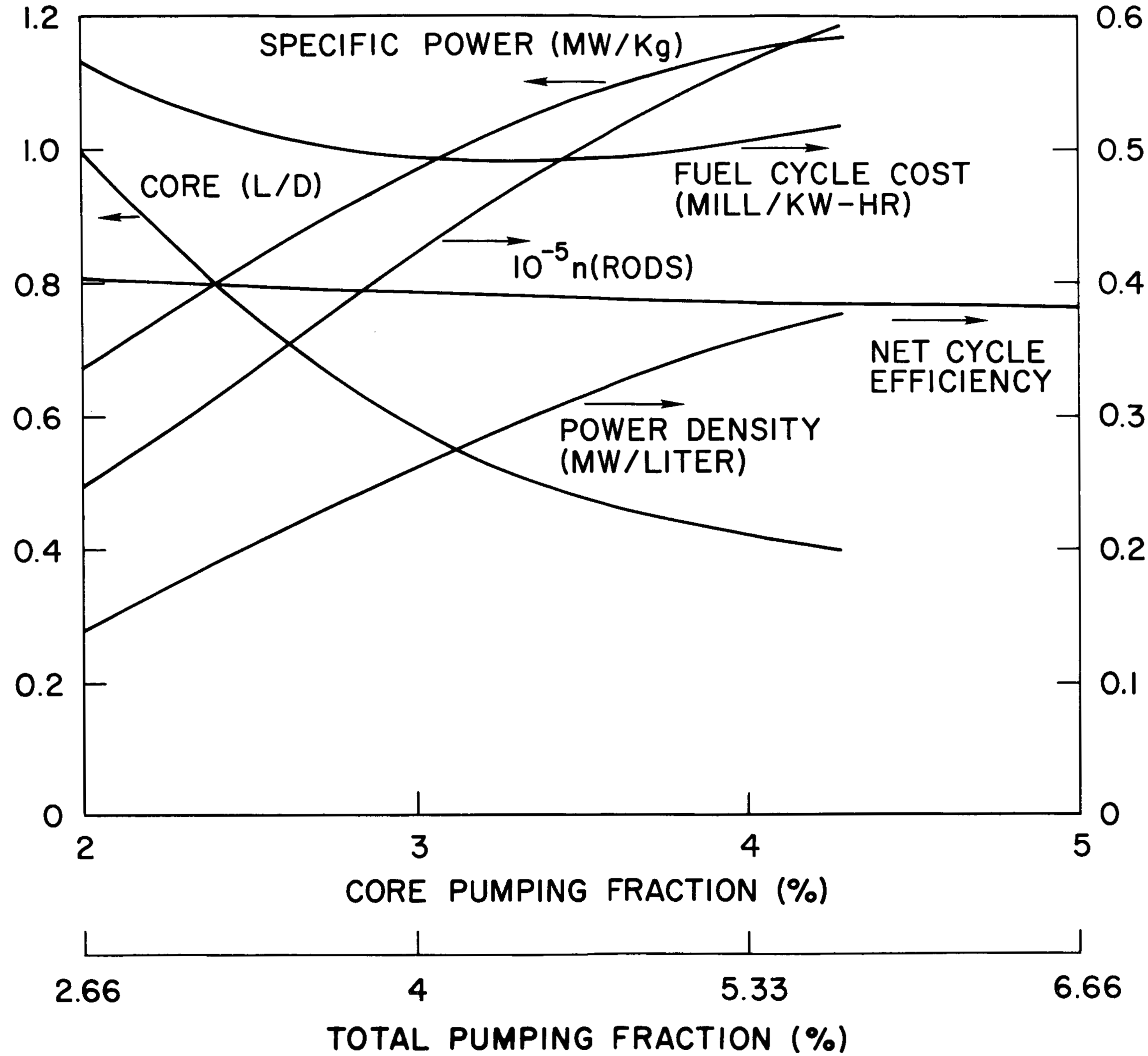


Figure 7 - Variation of 1000 MWe GCFR Core Performance with Pumping Fraction for Fixed Maximum Linear Rating (16 KW/ft), Maximum Clad Temperature (700°C) and Coolant Temperatures (340-640°C). 85 atm and Oxide Fuel

Table 3

INFLUENCE OF MAXIMUM CLAD TEMPERATURE ON PERFORMANCE OF 1000 MWe GCFR FOR FIXED MAXIMUM LINEAR RATING

Maximum Clad Temperature (°C)	600	650	700
Coolant Inlet Temperature (°C)	275	315	340
Coolant Outlet Temperature (°C)	530	580	630
Net Cycle Efficiency (%)	34	37.2	39.5
Specific Power (MWt/Kg)	0.85	0.82	0.85
Power Density (MWt/liter)	0.234	0.233	0.25
Core Volume (liter)	11660	10790	9365
Core Length (cm)	174.4	170	162
Coolant Void Fraction	0.575	0.565	0.55
Rod Diameter (cm)	0.835	0.845	0.835
Number of Rods	39600	37000	36500
Fuel Cycle Cost (mill/Kw-hr)*	0.549	0.533	0.50

Maximum linear rating: 18 Kw/ft (590 w/cm) with oxide fuel.
85 atm helium with 4% ratio of pumping power to thermal power.
Fixed core L/D = 0.6. Partial surface roughening doubles the heat transfer coefficient and triples the friction factor.
*75,000 MWe industry size and 100 MWd/Kg maximum burnup.

Table 4

1000 MWe GCFR PERFORMANCE WITH ROUGHENED OR SMOOTH SURFACES AND VARIOUS LIMITING TEMPERATURE CONDITIONS

Clad Surface	Roughened*			Smooth
Maximum Clad Temperature (°C)	600	650	700	700
Maximum Linear Rating (Kw/ft, w/cm)	12 (395)	15 (492)	18 (590)	10.3 (338)
Coolant Inlet Temperature (°C)	275	315	340	315
Coolant Outlet Temperature (°C)	550	590	630	590
Net Cycle Efficiency (%)	34.4	37.4	39.5	37.4
Specific Power (Mwt/Kg)	0.85	0.82	0.85	0.69
Power Density (MWt/liter)	0.234	0.233	0.25	0.17
Core Volume (liter)	11530	10690	9365	14400
Core Length (cm)	174	170	162	264
Core L/D	0.6	0.6	0.6	1.0
Coolant Void Fraction	0.575	0.565	0.55	0.605
Rod Diameter (cm)	0.68	0.77	0.835	0.685
Number of Rods	59500	45000	36500	43500
Fuel Cycle Cost (mill/Kw-hr)**	0.61	0.555	0.50	0.63

85 atm helium cooling with a 4% ratio of pumping power to thermal power. Oxide fuel.

*The heat transfer coefficient is doubled and the friction factor tripled.

**75,000 MWe industry size and 100 MWd/Kg maximum burnup.

carbide), and coolant pressures (70-120 atm). The total net electric power remains constant (250-1000 MWe) and partial surface roughening is assumed such that the ratio of Stanton number (or dimensionless heat transfer coefficient) to its smooth value may be anywhere from 1.5 to 3. If we either choose the optimum value of L/D for each degree of surface roughening or take the same near optimum value L/D = 0.6, doubling the Stanton number with respect to smooth rods leads approximately to maximum specific power and power density and to minimum fuel cycle cost. This result is valid for the general conditions stated in the preceding paragraph. It remains valid whether the rough friction factor is tripled or quadrupled with respect to its smooth value, depending on the type of surface roughening and the flow Reynolds number. In all these cases, the fuel cycle cost minimum is very flat, which allows a great flexibility in core design.

Helium versus CO_2

The performance figures for GCFR cores cooled by either CO_2 or helium are very close for the same limiting conditions, such as maximum clad temperature, linear rating, and pumping fraction. But the pressure drop with CO_2 is much higher than with helium, by a factor of 2 to 2.5. If CO_2 and helium were now compared for the same core pressure drop, performance with helium would become much better than with CO_2. Typically, with helium, the specific power would be 30% higher and the power density 45% higher. At the same pumping fraction, the higher pressure drop and dynamic head with CO_2 could lead to serious core mechanical and vibration problems. Furthermore, in view of the larger Reynolds numbers, by about a factor of 5 as compared to helium, surface roughening is less efficient with CO_2. For instance, for the same doubling of the Stanton number, the friction factor may be tripled with helium and quadrupled with CO_2. The rotating machinery will obviously be quite different for CO_2 and for helium with the same pumping power.

Other Design Parameters

Variations of several other design parameters have also been considered (8). For instance, for fixed overall core geometry, coolant pressure and temperature conditions, and fixed maximum clad temperature, the increase in linear rating (or surface heat flux) over its value for smooth rods simply depends upon the relative increase in surface heat transfer coefficient due to surface roughening (Fig. 8). Also, core performance and fuel cycle cost are not very sensitive to variations in friction factor for fixed heat transfer characteristics, or to variations in core (L/D) for given roughening properties although there is an optimum value of (L/D)leading to minimum fuel cycle cost. For fixed core performance, the maximum clad temperature varies in a narrow range for rather large variations of surface heat transfer coefficient. The linear rating is found to increase approximately linearly with the difference between maximum clad temperature and coolant exit temperature, whether the coolant inlet temperature or the coolant temperature rise remains constant. Figure (9) shows that the rod diameter also increases quasilinearly with increasing temperature difference, while the number of rods and the fuel cycle cost decrease; the other core parameters are not very sensitive to this temperature difference, for fixed core (L/D). Doubling the clad thickness from 4% to 8% of the rod diameter for fixed maximum clad and fuel temperatures, coolant inlet

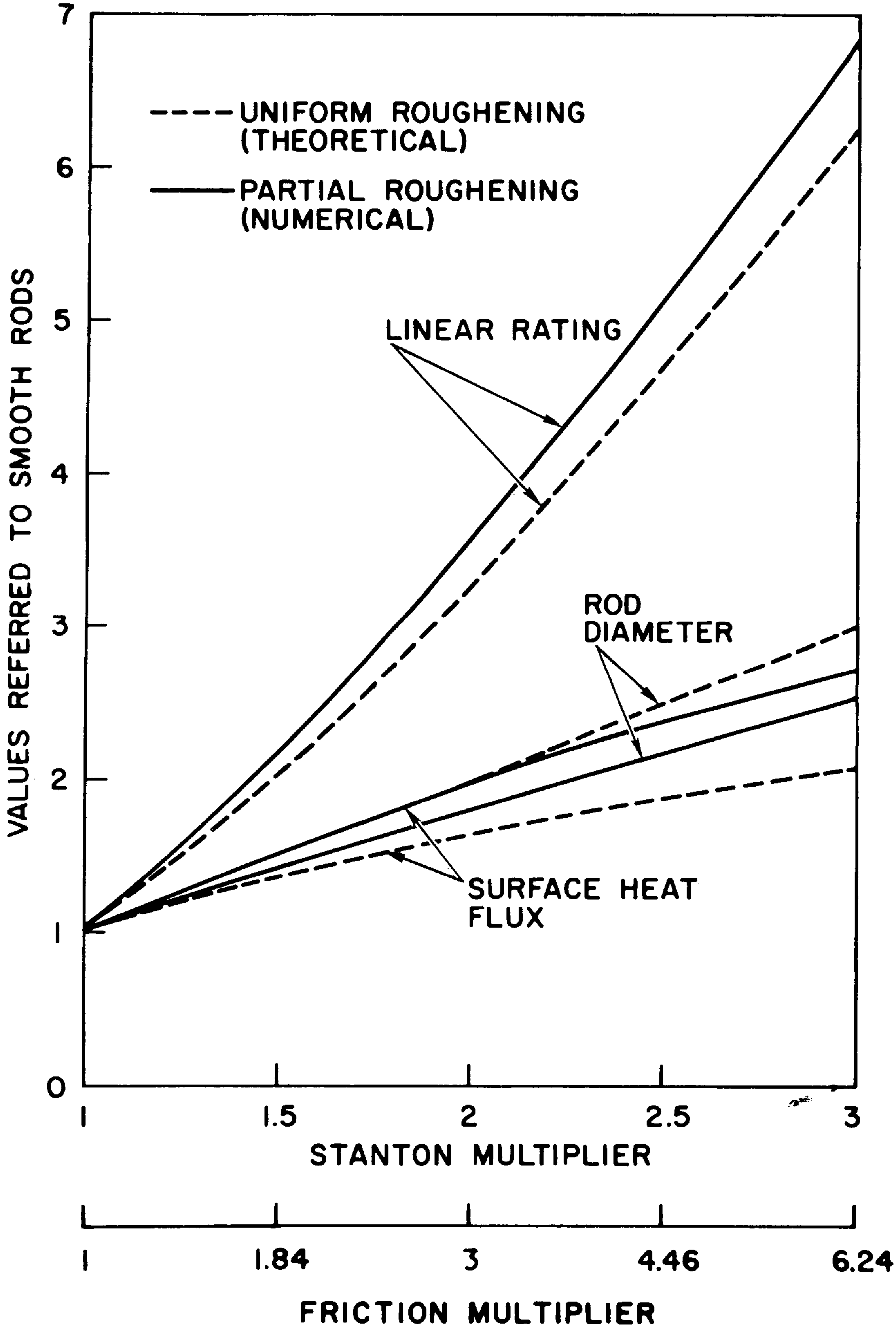


Figure 8 - Improvement in Core Performance with Increase in Surface Heat Transfer Coefficient by Optimum Surface Roughening, (Fixed Coolant Temperature and Pressure, Fixed Maximum Clad Temperature and Core Length)

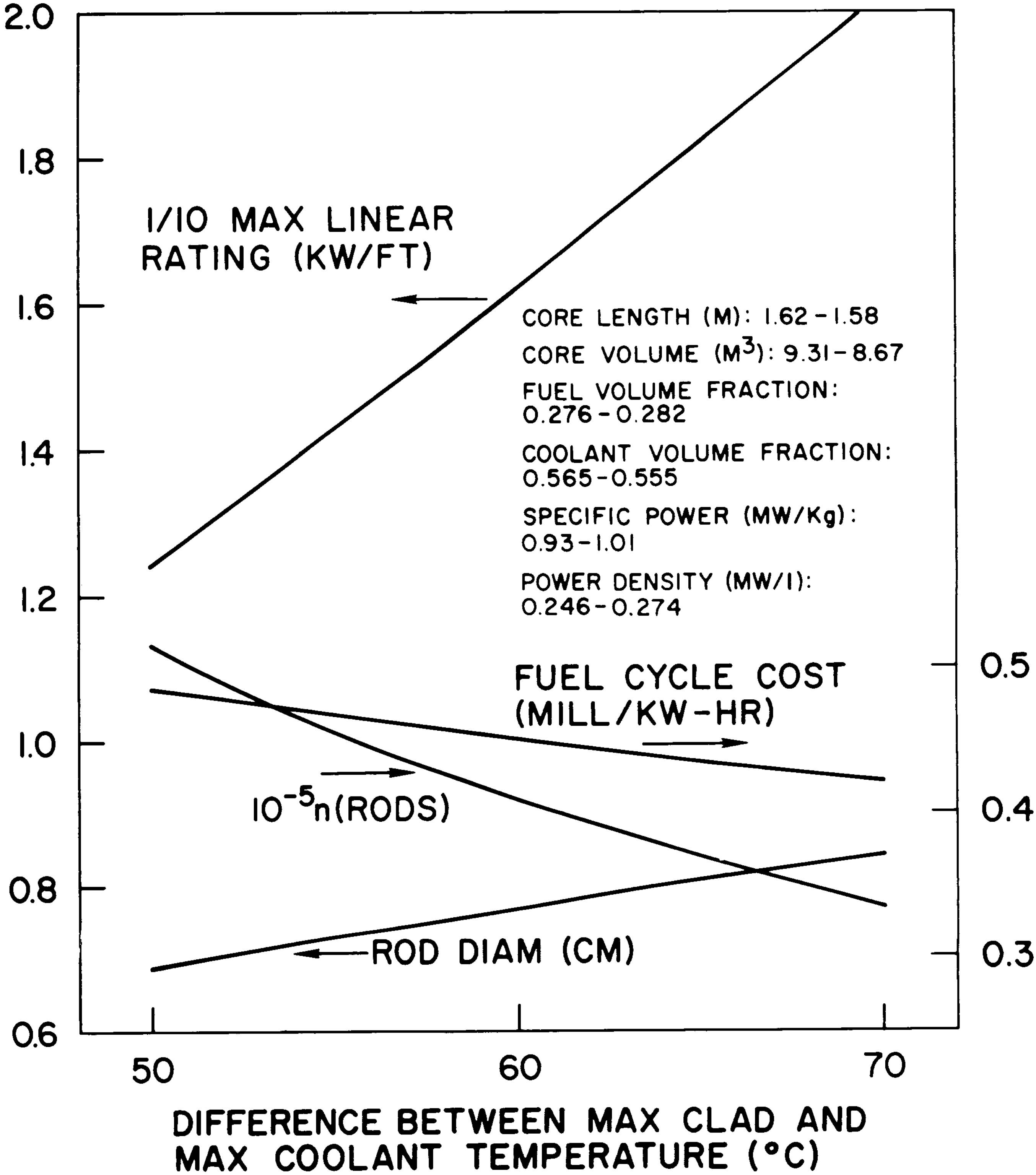


Figure 9 - Variations of Performance of 1000 MWe GCFR with the Difference between Maximum Clad and Maximum Coolant Temperature for Fixed Core L/D (= 0.6), Pumping Fraction (4%), Maximum Clad Temperature (700°C) and Coolant Temperature Rise (300°C) with Optimum Surface Roughening

temperature and core L/D, leads to a small decrease in specific power (8%) and to an increase in rod diameter (10%) and fuel cycle cost (about 10%). For the same fixed conditions, there is an optimum fissile enrichment leading to minimum fuel cycle cost. Finally, all other conditions remaining the same, halving the fuel burnup, leads to an increase in fuel cycle cost from 0.50 to about 0.90 mill/Kw-hr (10).

FUTURE PROSPECTS OF GCFR

We have seen the design flexibility of Gas-Cooled Fast breeder Reactors. For instance, very satisfactory performance may be obtained by proper design for a wide range of coolant pressures, clad temperatures and maximum linear ratings. Similarly, GCFR may be designed with smooth or roughened fuel rods, or may be cooled either by helium or by carbon dioxide. Helium-cooled fast breeders may fully utilize the potential savings brought about by better fuels, such as carbides, which, of course, are hardly compatible with CO_2 or steam cooling. The use of a prestressed concrete pressure vessel around the whole primary circuit makes possible a compact and a safe power plant. With low fuel cycle cost because of good core performance and cheaper components due to the simple gas technology, the total power costs of GCFR are expected to be substantially lower than those of other fast breeder systems, even at rather modest helium pressure and temperature levels. Furthermore, refueling, maintenance and repairs are believed to be substantially easier with a gas rather than with a liquid metal system.

A further reduction in capital costs could possibly arise from the use of a direct gas turbine cycle. For instance, large size gas turbines, about 200-300 MWe, could be incorporated in the walls of a multicavity prestressed concrete pressure vessel. Such designs have already been proposed for High Temperature Gas-Cooled Reactors operating with helium at higher outlet gas temperatures (750-850°C), but lower pressures (40-60 atm). Even with the present maximum design clad temperature limitations of stainless steel at about 700°C, a net plant efficiency of 34%-36% could be obtained with 85 atm helium in a direct cycle. Core performance and fuel cycle cost are very close to the values with a steam cycle at the same maximum clad temperature and linear rating, while capital costs are expected to be lower, even with 80% regeneration in the gas turbine cycle. The number of stages of the rotating machinery is higher with helium than with CO_2, but the optimum pressure ratio is much smaller with helium. Therefore the helium pressure is much higher in the low pressure side of the regenerator, leading to better heat exchange and a smaller system for the same top pressure. The cycle efficiency is comparable with helium and CO_2 for the same top conditions. With higher outlet gas temperatures allowed with refractory metals (vanadium alloy), chromium cermet fuel, or even graphite and SiC-coated particles (11), the net cycle efficiency with a gas turbine cycle would run from 40% with 720°C gas at 100 atm to about 50% with 930° gas.

While gas cooling for fast reactors has such long range possibilities, a satisfactory helium-cooled fast breeder reactor could be developed in the same timetable as sodium-cooled breeders. Much of the required technology for GCFR's is either available or being developed for other gas-cooled thermal reactors or liquid metal-cooled fast breeders (5). For instance, large prestressed concrete pressure vessels have been built in Europe for CO_2-cooled reactors (Magnox or AGR) and one is being built in the U. S. for a helium-cooled reactor (HTGR). While these vessels operate at lower pressure (up to about 50 atm), some of them are larger than the vessel proposed for the 1000 MWe GCFR; furthermore, scale models of PCRV designed for 100 atm pressure have been successfully tested. Much of the helium technology such as handling, purification and storage already exists or is being developed in Europe and in the U. S. for High Temperature Gas-cooled Reactors (12). Similarly, steam-driven helium circulators and once-through steam generators developed for the HTGR program would contribute directly to the technology required for GCFR's. Most of the physics data and much of the metallurgical data will be obtained from other fast reactor programs. The GCFR will use mixed uranium and plutonium oxide fuels in stainless steel or nickel base alloy cans, very similar to those used in liquid metal fast breeders, except that the pin diameter will be larger and the clad will be artificially roughened as for AGR fuel rods. The assumed burnup for GCFR is the same as for LMFBR, but the maximum design clad temperature may be somewhat higher, although it is lower than in AGR. But even with a maximum clad temperature reduced to 650°C, the GCFR gives very satisfactory performance and low fuel cycle cost. The main difference between LMFBR and GCFR fuel pins is the presence of high external coolant pressure which may create stresses at the beginning of life if the internal and external pressure are not balanced, although it will counterbalance stresses caused by fission gas buildup during lifetime. With pressure equalization or venting of fission product gases to a manifolded plenum (and not to the main coolant), the fuel development problem is very similar for sodium or helium-cooled reactors.

CONCLUSION

Gulf General Atomic has been studying gas cooling of fast reactors since 1961, both at GGA Europe (Zurich) and in the USA (in San Diego). Joint studies with the USAEC are being pursued and preliminary design studies of a 1000 MW(e) power plant have been performed with the East Central Nuclear Group of U. S. utilities. More recently, a joint GCFR development program has been initiated by the Swiss Federal Institute for Reactor Research and Gulf General Atomic. Capsule irradiations are done at the Oak Ridge National Laboratory and physics experiments are performed in cooperation with the USAEC. It is proposed to perform further fuel irradiation in EBR II, in a gas loop in the Fast Flux Test Reactor Facility and also in a reactor experiment, GCFRE. The essential features of this small reactor experiment are a driver section using low performance fuel and a totally-contained central test loop (13). This experiment should provide nuclear data, indicate burnup effects on fuel and clad, and demonstrate the performance of full scale fuel boxes and subsystems. Since most of the fuel of GCFRE operates under thermal conditions within existing technology, early reactor operation should be feasible without further fuel development. With statistical information on fuel element performance in a fast flux obtained from GCFRE, a large GCFR power plant could be in operation in the early 1980's. Together with such advanced converters as HTGR's, Gas-Cooled Fast breeder Reactors should then provide maximum utilization of world uranium and thorium resources at highly competitive power costs.

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DISCUSSION

R.W. Dickinson (Liquid Metal Engineering Center) - In the refueling of this very high power density reactor, it has previously been suggested that it be refueled under water yet I didn't notice any provisions here. Has this been tried; do the helium circulators and electric motors work well under water after they have been flooded? This is sort of an engineering question. Second, it appears that your blower drive motors are running in helium. If so, how are these lubricated and maintained, and how does one solve the electrical insulation problem of running very large motors in high temperature helium? Third - it appears that there is quite a high temperature gradient across your reactor; something in the order of 4.5 or 5° per centimeter. Is there evidence that the thermal strains in the cladding will permit this high temperature gradient?

G.B. Melese d'Hospital - The refueling is done at atmospheric pressure with helium and with continuous helium cooling at atmospheric pressure; so it is a dry refueling. Now, on the question of compressor drives, etc., in helium, I think it is best answered by the fact that we are presently operating for the Fort St. Vrain reactor, a 6000 horsepower helium compressor at a pressure of about 50 atmospheres (700 psi) and a temperature quite comparable and probably higher than the one considered here for this reactor. So, the problem is solvable and this is the kind of development performed for a high temperature gas reactor which would apply to gas cooled fast breeders. The third question of the temperature gradient - the temperature gradient in the core is not bad, only about 300°C. We haven't tested any fuel element with this temperature gradient yet. I'll mention one thing about testing - from calculations, it seems that at the design clad temperature which is about 700°C with a hot spot below 800°C, that it shouldn't be any major problem but this is precisely why we want to build a gas cooled fast reactor experiment and test all these questions. Now we have performed capsule tests at the Oak Ridge National Laboratory at temperatures of about 700°C clad temperature and 15-16 kilowatts per foot. These rods are cooking presently with 1000 psi outside pressure in the ORR and have reached a burnup to 15-20,000 megawatt days per ton apparently without any deleterious effects. We cannot answer any further questions until we reach the burnup that we are shooting for which is just like everybody - 50,000 megawatt days per ton first, and then 100,000 megawatt days per ton. The main development problem we see for this reactor, just like the liquid metal fast breeder reactor, is the fuel development. But we believe that if we use a design for pressure equalization or provision for venting to a plenum, that the conditions would be very similar to those of the liquid metal fast breeder reactor fuel and, therefore, much of the experience coming from the liquid metal program would be applicable to the gas program.

K.A. Trickett (U.S. AEC) - The gas cooled reactor has a much higher film drop so if the commonality of fuel elements is assumed and you run the sodium and gas reactors at the same maximum clad temperature, what sort of a penalty does this give you in the gas cooled reactor?

G.B. Melese d'Hospital - Well, I mentioned this briefly. I said that there was some obvious thermodynamic advantage in sodium in view of the fact that the film drop is more like 25° in sodium and maybe 100°-150° in gas, I agree, but the problem is this. We have a single loop system - we don't have a double loop system - and with a 50° difference between the temperatures in the steam generator, we don't have to pay a very high penalty, we believe, because the steam generator hopefully will be simpler with helium on one side and steam on the other than with sodium on one side and steam on the other. We need more surface area but we may have a simpler design which means that the capital cost will be comparable.

THE WESTINGHOUSE 1000 MWe FOLLOW-ON STUDY, TASK I: TRADE-OFF STUDIES

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Abstract

Technical and economic comparisons, or trade-off studies, are reported for several design features of a 1000 MWe liquid metal fast breeder reactor. The design features selected are: a four-module core, fuel vented to coolant, combined hot-cell refueling and containment, two heat-removal loops, a piped primary system, a non-reheat steam cycle.

INTRODUCTION

The Westinghouse Electric Corporation is one of five industrial contractors performing 1000 MWe LMFBR Follow-On Studies for the Argonne National Laboratory. The primary objective of these Studies is "to provide information which will be of assistance to the AEC in the furtherance of the National LMFBR Program Plan." This broad objective is used to guide the specific objectives of the Study which are:

a. to conceptually design a nuclear steam supply system and energy conversion system which could be committed to sale in 1980;

b. to optimize this conceptual design for minimum power cost;

c. to perform a number of parametric analyses;

d. to identify data and information which are lacking, the lack of which affects the safe and economic construction of an LMFBR plant based on this design;

e. to recommend research and development programs needed to supply the lacking information;

f. to assess the relative value of such research and development programs.

With the previous LMFBR design study performed by Westinghouse for the AEC[1] as a starting point, this Study is now in the phase in which the conceptual design of the nuclear steam supply system is being performed. Rather than follow the costly and time-consuming procedure of carrying a large number of possible design concepts through the conceptual design stage, Westinghouse has chosen to perform a number of binary trade-off studies first, thereby eliminating a number of concepts at an early design stage. The trade-off studies which have been performed are:

a. Four-module vs. seven-module vs. pancake cores[2]

b. Vented vs. sealed fuel rods[3]

c. Hot-cell vs. under-the-plug refueling[2]

d. Hot-cell containment vs. pressure shell containment[2]

e. Two vs. three coolant loops[5]

f. Piped primary coolant system vs. integral (also called sea-of-sodium or combined unit reactor vessel) primary coolant system[6]

g. Sodium reheat steam cycles vs. steam reheat or moisture separation steam cycles[4]

Other studies needed to establish the design concept (such as a determination and comparison of fuel properties) were performed in parallel with the trade-off studies.

Those who planned this meeting wisely suggested that we avoid detailed papers on the subjects of economics and safety, because the former is presently conjectural and the latter is outside the scope of the meeting. However, the work reported in this paper relies rather heavily on the "conjectural" economics. In comparing alternate design concepts, we seek first to have each meet the technical requirements, second to have each meet safety requirements, and finally we select the alternate design concept which we think will minimize the cost of power.

The economic aspects of some comparisons are clear-cut. Thus, as we shall see later, the economic comparison of sodium-reheat steam cycles with moisture separation or live steam reheat steam cycles shows sodium reheat steam cycles 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 cost comparison uncertainty is small.

On the other extreme, the economic comparison of a piped primary coolant system with an integral primary coolant system has a large uncertainty associated with it. The design of each system lacks the test of time, the component costs for each system are uncertain, and the systems being compared are not very similar.

In spite of our inability to be sure of costs or of design details, a choice between concepts must be made. The larger the cost difference between two systems, the more likely it is that the correct choice can be made. The smaller the cost difference between two systems, the smaller are the economic penalties that will result from having made the wrong choice.

Because of limited time, the bulk of this paper will be devoted to a presentation of results, leaving discussions of the details of the work for the more complete reports in the reference list.

DISCUSSION

A. Four-module vs. Seven-module vs. Pancake Cores

As a result of concern over the severity of the power excursion which could result from sodium boiling, a criterion was established for this study that the reactivity insertion resulting from voiding of core and axial blanket be zero or negative. Twelve reactor configurations were studied (Table 1), with the aim of choosing, from those configurations which satisfied the sodium void constraint, that reactor configuration which minimized fuel cycle costs.

Depletion analyses were performed, for the average equilibrium burnup of 67,000 MWD/Tonne, prior to calculation of the voiding reactivity. Two fuel enrichment zones were considered for the modular cores and three for the pancake core. Chromium carbide-modified (U,Pu)C fuel in assemblies of the same dimensions (except length) was used for all twelve reactor configurations.

The sodium voiding reactivity is shown in Figure 1, from which it can be seen that a negative sodium voiding reactivity change is possible for the core sizes considered in both the four-module and pancake configurations. The seven module core height would have to be either much smaller or much larger than those considered here. The Doppler coefficients fell in the range -3.6×10^{-3} to -4.6×10^{-3} $(T/k)(\Delta k/\Delta T)$ for eleven of the configurations. Configuration J had a Doppler coefficient of -2.9×10^{-3} $(T/K)(\Delta k/\Delta T)$.

The evaluation of the fuel cycle economics was performed on the basis of a three year fuel cycle, with 1/3 of the core and blanket replaced and reprocessed each year. The average burnup of the discharged core was assumed to be 100,000 MWD/Tonne. The following factors were considered in the evaluation:

a. fuel fabrication cost

b. fabrication capitalization charge

c. plutonium credit

d. inventory charge

e. shipping cost

f. shipping credit

g. reprocessing cost

h. reprocessing credit.

Using present best estimates for these factors, a plutonium value of $10/gram, 6 months fuel fabrication time, 36 months core residence time, 4 months for cooling and shipping and 2 months storage, the fuel cycle costs of Figure 2 were obtained.

Of the designs having acceptable sodium void coefficients, the four-module configurations have the lowest fuel cycle costs and, consequently, a four-module design was chosen. A blanket-to-core volume ratio of 2.86 was selected as one giving a reasonable balance between fuel cycle cost and sodium void reactivity. A sketch of the resulting reactor configuration is shown in Figure 3.

TABLE 1

Summary of Core Designs Studied

Designation	Modules	Core Height,* L (in.)	Core Diam., D (in.)	L/D	Blanket Thickness Radial (in.)	Blanket Thickness Axial (in.)	Blanket Width (in assembly widths)	Initial Average Pu Core Loading† $\frac{Pu}{Pu + U}$, %
D	7	60.56	31.03	1.94	14.81	12.08	4	21.25
B		52.38	33.36	1.56	11.54	"	3	19.63
E		"	"	"	18.33	"	5	21.78
A		43.07	36.79	1.16	11.23	"	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	"	5	19.12
G		"	"	"	30.96	"	7	19.40
F		33.52	55.18	0.603	24.24	"	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	"	2	18.80
K		"	"	"	22.44	"	3	18.80
J		15.52	162.18	0.0954	16.75	"	2	25.67

*All dimensions for the hot condition of the core (890°F).

†Isotopic composition of the plutonium:

Pu-239	61 W/O
Pu-240	22 W/O
Pu-241	13 W/O
Pu-242	4 W/O

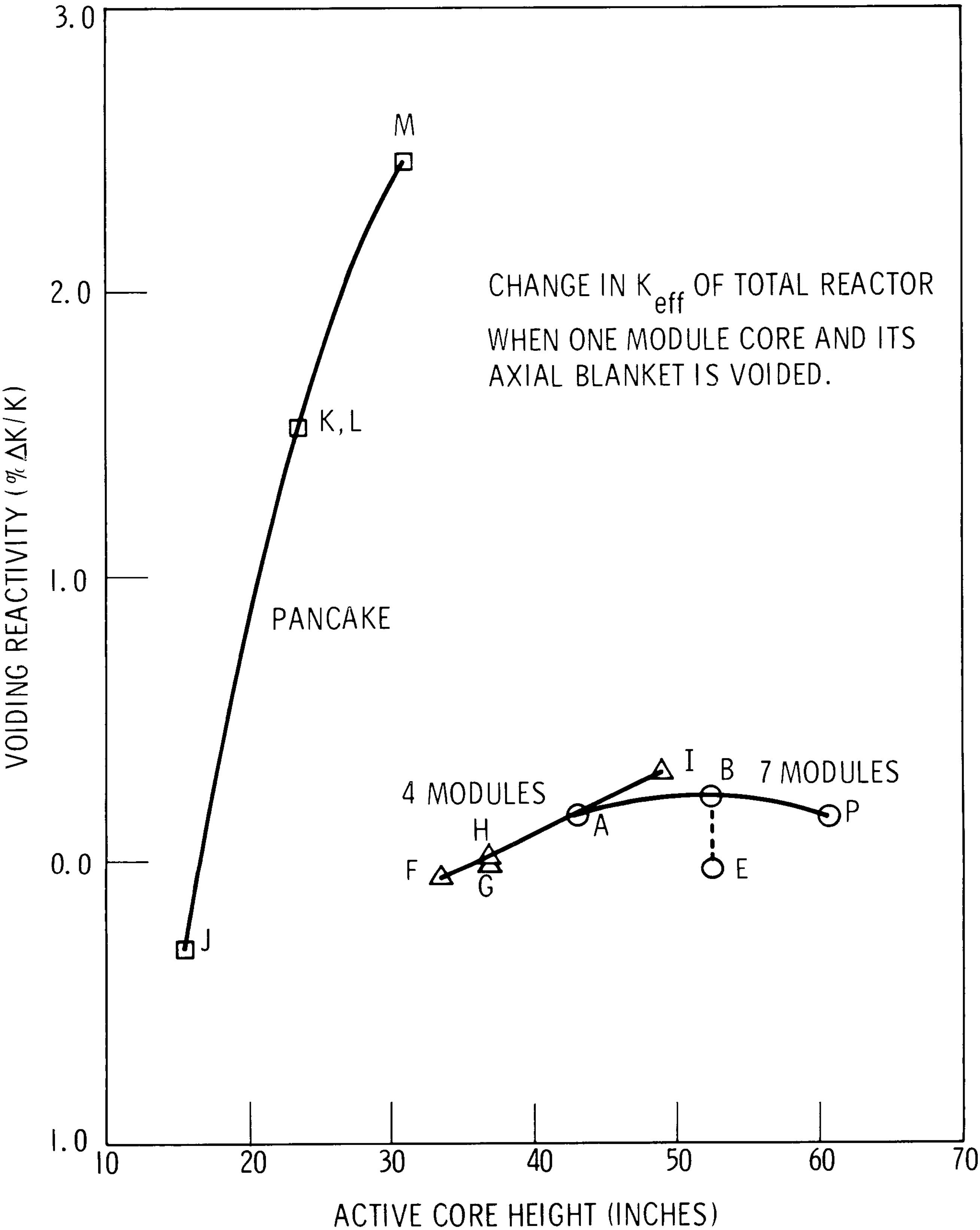


FIG. 1 VOID REACTIVITY VS CORE HEIGHT

TOTAL FUEL CYCLE COST VS BLANKET-TO-CORE VOLUME RATIO

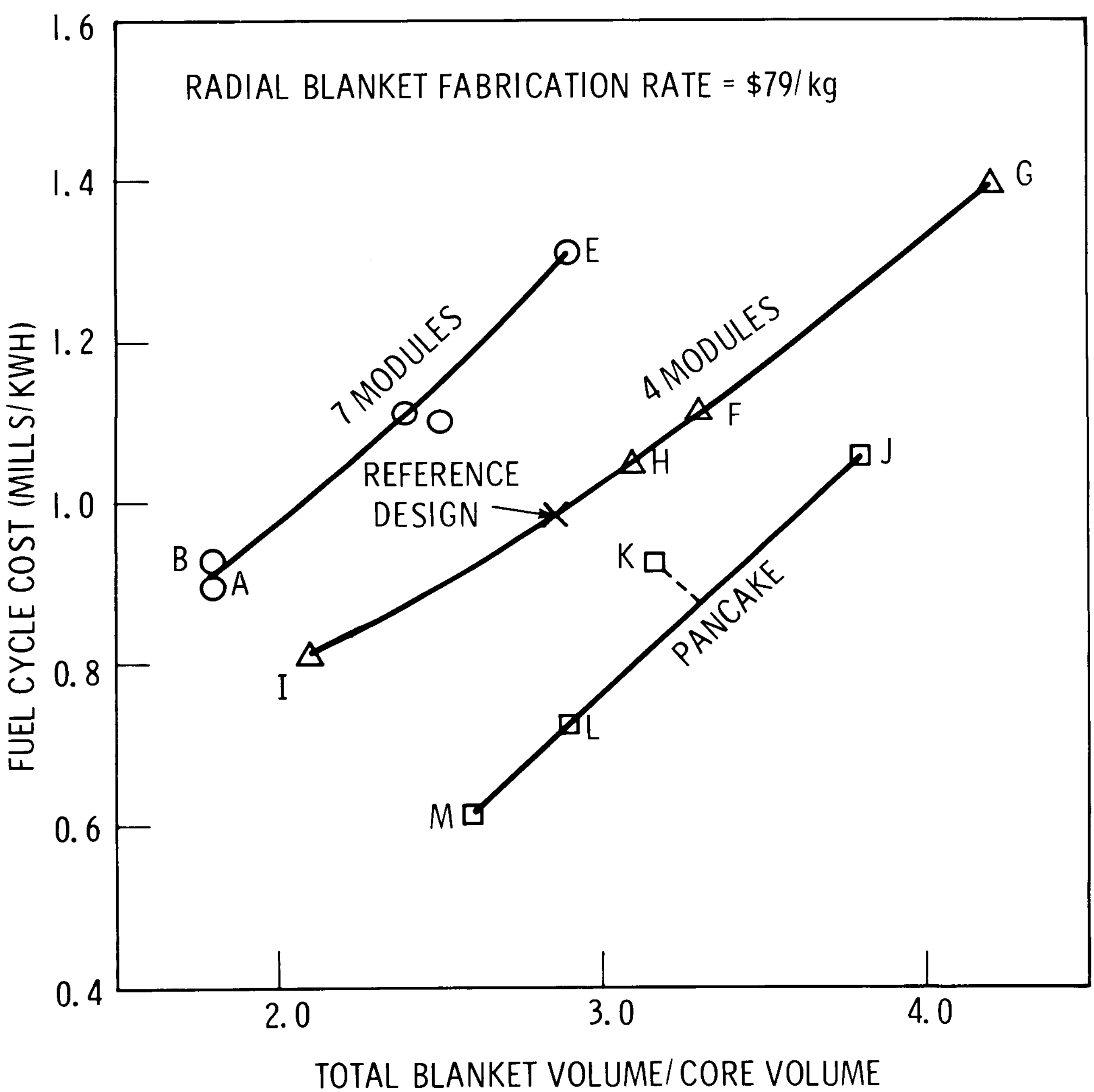
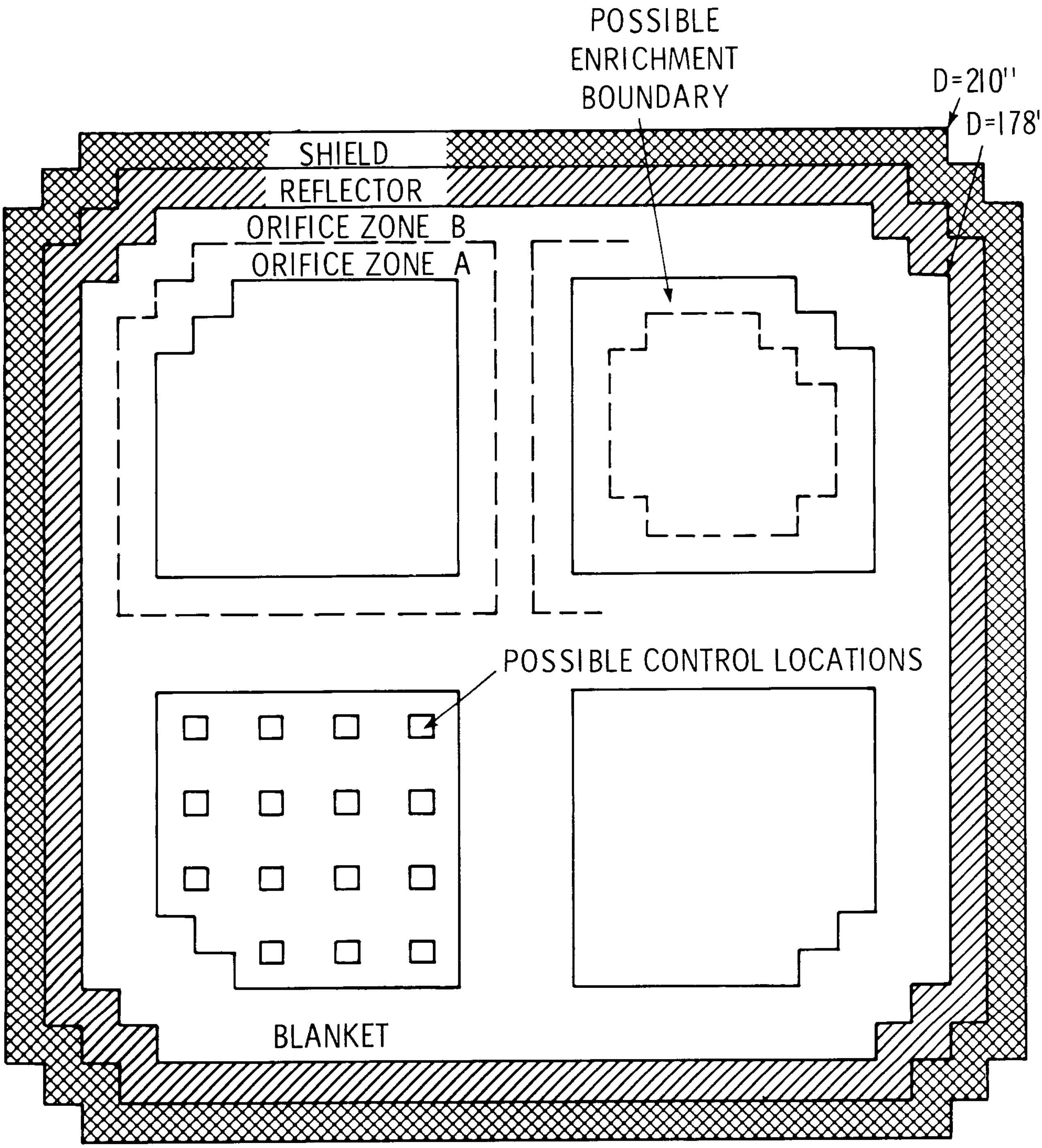


FIG. 2 TOTAL FUEL CYCLE COST VS BLANKET-TO-CORE VOLUME RATIO



$\frac{V_B}{V_C} = 2.86$

61 ASSEMBLIES/MODULE

16 x 16 = 256 RODS/ASSEMBLY

LESS 9 RODS FOR CONTROL
LESS 4 RODS FOR STRONGBACK

(92.71 cm.) 36.5" ACTIVE HEIGHT
(15.09 cm.) 5.94" ~ LENGTH OF ASSEMBLY SIDE
(38.10 cm.) 15" BLANKET THICKNESS TOP + BOTTOM EACH

FIG. 3 SCHEMATIC REACTOR CROSS SECTION

B. Vented vs. Sealed Fuel Rods

Because the fuel inventory and fabrication costs are high for fast breeder power reactors, it is necessary to operate the fuel at a high specific power and for high burnup. Such operation produces a large amount of fission product gas, a portion of which can escape the fuel. In spite of the probable economic penalties, all fast breeder reactors designed to date contain the fission product gas within the fuel rod. The increased fuel rod length needed to accommodate the gas increases the cost of the vessel, fuel handling machines, pumps and vessel internals. There are also potential safety hazards associated with such fuel rods: the possibility of gas blanketing a fuel assembly in the event of an in-core cladding rupture, and the possible hazard of handling fuel elements with large quantities of pressurized radioactive gas.

Sodium bonded mixed carbide fuels are considered in this study. Based on the preliminary fission gas release curve of Figure 4, and the calculated fuel temperature distribution, a release rate of about 20 percent in the hot rod and an average release rate of 5 percent were determined. The assumed fission product gas release strongly affects the comparison of vented and sealed fuel rods. Further fission product gas release studies have corroborated the venting analysis, but the data spread is still large. A very much lower fission product gas release could eliminate the economic advantage (but not the potential safety advantage) of the vented fuel rod.

The non-vented rod requires a plenum length above the top axial blanket of 48 inches to accommodate fission product gases and an additional 11 to 13 inches to accommodate sodium bond expansion. Several vented rod designs were considered, including diving bell, elastic tube valve, mechanical check valve and porous plug. All of the designs can meet the performance requirements but all also require development and testing. A total length of 26 inches was believed adequate for both the bond sodium expansion plenum and the vent mechanism.

The economic analysis considered the capital cost differences of the vessel, fuel handling equipment, building, pumps, gas purification system and sodium purification system. The operating cost differences due to the coolant pumping power and the fuel fabrication charge were also considered. Cost differences due to core internals, instrumentation, shipping, maintenance problems, failed fuel detection system and gas purification refrigeration systems were not quantitatively compared, but were not expected to alter the cost comparison appreciably.

The comparison for the gas purification system assumed that one percent of the 40,992 rods in the non-vented core would fail during the period 1-1/2 years to 3 years after loading. A cladding thickness of 14 mils was used for both the vented and the non-vented design, although a reduction of 2 to 4 mils appeared reasonable for the former.

The vented concept was found to have a $788,000 capital cost advantage and a $293,000 annual operating cost advantage over the non-vented rod. Remembering that the cost comparison is not comprehensive (e.g., maintenance costs were not considered), it appears that the cost advantage of the vented concept is not sufficiently great to make a clear-cut choice. However, the vented design has a potential safety advantage by enabling retention of the sodium bond in the event of a pinhole cladding failure, by decreasing the probability of gas blanketing in the event of cladding rupture, and by minimizing the amount of pressurized radioactive gases to be handled outside the core. Thus, in spite of the many development problems remaining, the vented-to-coolant fuel rod design was chosen.

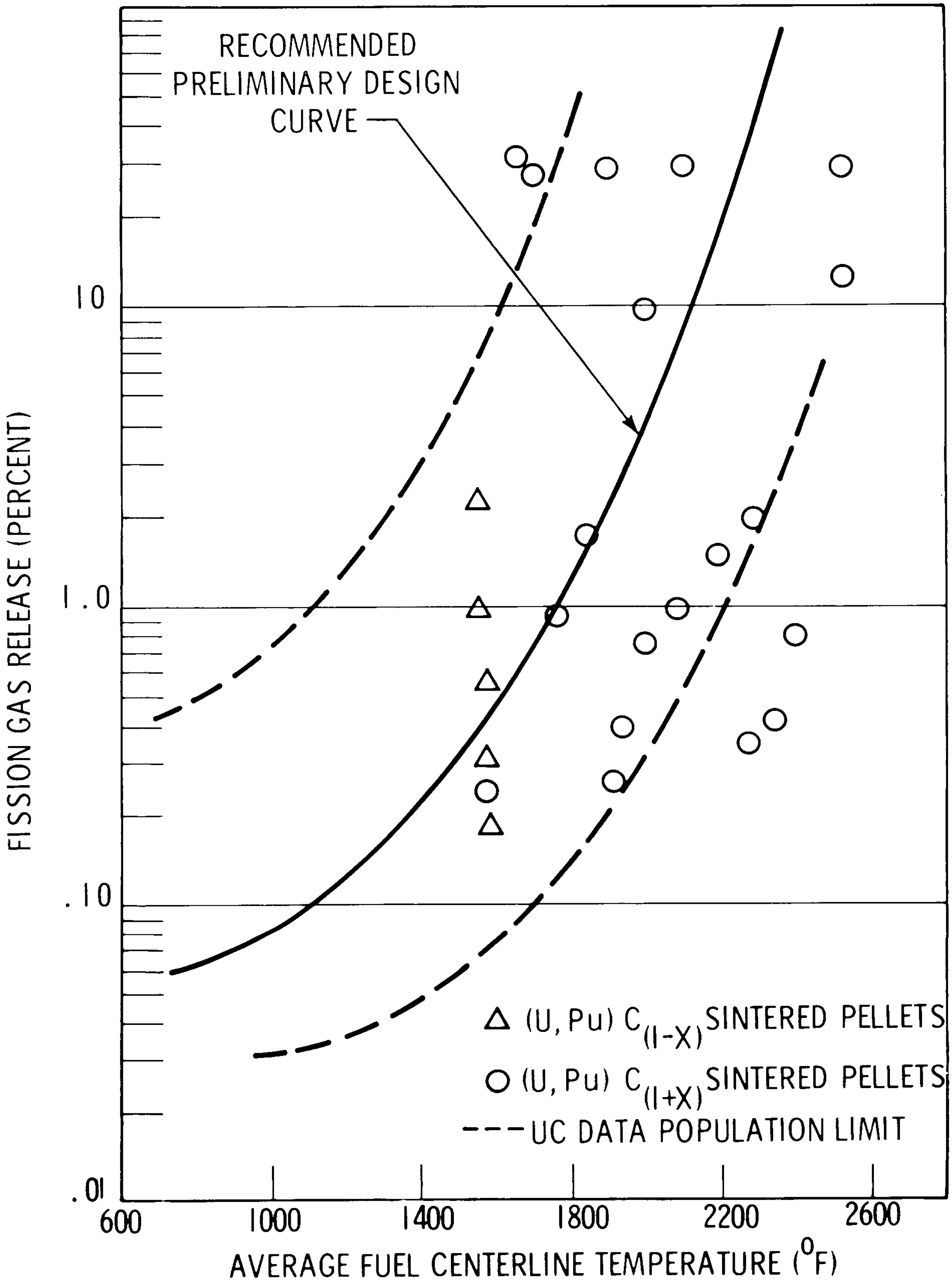


FIG. 4 FISSION GAS RELEASE AS FUNCTION OF AVERAGE FUEL CENTERLINE TEMPERATURE

C. Hot-Cell vs. Under-the-Plug Refueling

The hot-cell refueling system is one in which the reactor shield plug is removed for refueling and the refueling is performed by equipment in a refueling cell, or hot cell, located directly above the reactor. The hot cell concept has the advantage of inherent flexibility to handle or support an unexpected effort in fuel handling equipment maintenance or in reactor maintenance. It also provides the means for removal and inspection of reactor vessel internals as required.

The under-the-plug system operates with the reactor shield plug in place during refueling. Fuel handling is performed by equipment working through the plug or by equipment located under the plug. Fuel assembly storage during the decay heat period is in the reactor vessel or in an adjoining tank. This system has a good potential for efficient fuel handling which could reduce plant downtime.

In a cask system, the fuel is removed from the reactor without an extensive waiting period for decrease of fission product decay heating. Shielding of the fuel handling machine is adequate to permit personnel access to the reactor area during refueling. The principal advantage of this system is that operating experience to date has identified and resolved many problems.

Both the hot cell and under-the-plug concepts envisage lateral transfer of the fuel element from the reactor vessel to the fuel storage tank, under sodium. Table 2 outlines the concept comparisons which led to elimination of the shielded transfer cask and narrowed the comparison to a trade-off of the hot-cell concept against the under-the-plug concept.

These two concepts were carried through the conceptual design stage, and cost estimates were prepared which considered all portions of the plant which differed for the two concepts. Figure 5 shows the design of the hot-cell refueling concept, while Figure 6 shows the under-the-plug refueling concept.

The capital cost comparison of the two concepts included building, reactor vessel and plug, fuel handling equipment and miscellaneous equipment. A range of $4/lb to $8/lb was considered for the vessel and plug costs. The corresponding capital cost difference between the two concepts was 1.26 to 2.46 million dollars in favor of the hot-cell concept.

Operating cost differences, primarily due to plant downtime, were not considered in the cost comparison. Planned downtime for refueling will be greater for the hot-cell concept, but planned downtime for inspection and maintenance of equipment below the plug will be greater for the under-the-plug concept. Unexpected downtime for repair of equipment within the vessel will also be greater for the under-the-plug concept. Thus, one cannot now say which concept will yield a better overall plant availability.

Although the capital cost difference is small and the operating cost difference is uncertain, the greater flexibility of the hot-cell concept tips the balance and it has been chosen for the reference design.

Table 2

Fuel Handling Concept Comparison

Rating Factors	Hot-Cell		Under-the-Plug		Cask	
	Rating		Rating		Rating	
1. Reliability	Good	Limited exposure to sodium, sodium vapor, and radiation. Available for preventive maintenance.	Poor	Complex mechanisms in sodium, sodium vapor, and radiation. Design will have to be refined to a high degree to give long term reliability.	Good	Limited exposure to sodium, sodium vapor and radiation. Available for preventive maintenance.
2. Accessibility for Maintenance	Good	Good accessibility. Will not interrupt reactor operation, but may require remote maintenance.	Poor	Difficult and time consuming to repair equipment under the plug. Can only be done with reactor down.	Ave.	Good accessibility to fuel handling cask. Rotating plugs require reactor downtime to repair.
3. Ease of Operation	Poor	Requires high degree of operator skill to perform remote manual operations. Reactor plug handling presents alignment problem.	Good	Locations are pre-indexed. Operations are interlocked. Not dependent upon operator.	Ave.	Somewhat higher level of risk for personnel exposure to radiation.
4. Safety	Good	Hot cell design provides good radiation and missile protection. Reduces possibility of sodium and air reaction.	Ave.	Low level risk for personnel exposure to radiation. Malfunction may damage equipment or core.	Ave.	Somewhat higher level of risk for personnel exposure to radiation.
5. Development	Ave.	Development needed in sodium vapor deposition. Fission product control, decontamination, and equipment operation in inert atmosphere.	Poor	Development of high reliability mechanisms a must to offset poor accessibility.	Good	Experience to date shows equipment O.K. High decay heat dissipation will require more work.
6. System Compatibility with Reactor Main. & Inspection	Good	Hot cell with remote equipment provides excellent facility for reactor inspection or maintenance.	Poor	Requires erection of hot cell to gain access to reactor internals.	Poor	Requires erection of hot cell to gain access to reactor internals.
7. Potential for High Plant Availability	Ave.	System refinement estimated to have average potential.	Good	System refinements appear to offer possibility of efficient fuel handling.	Poor	Little potential for significant improvement.

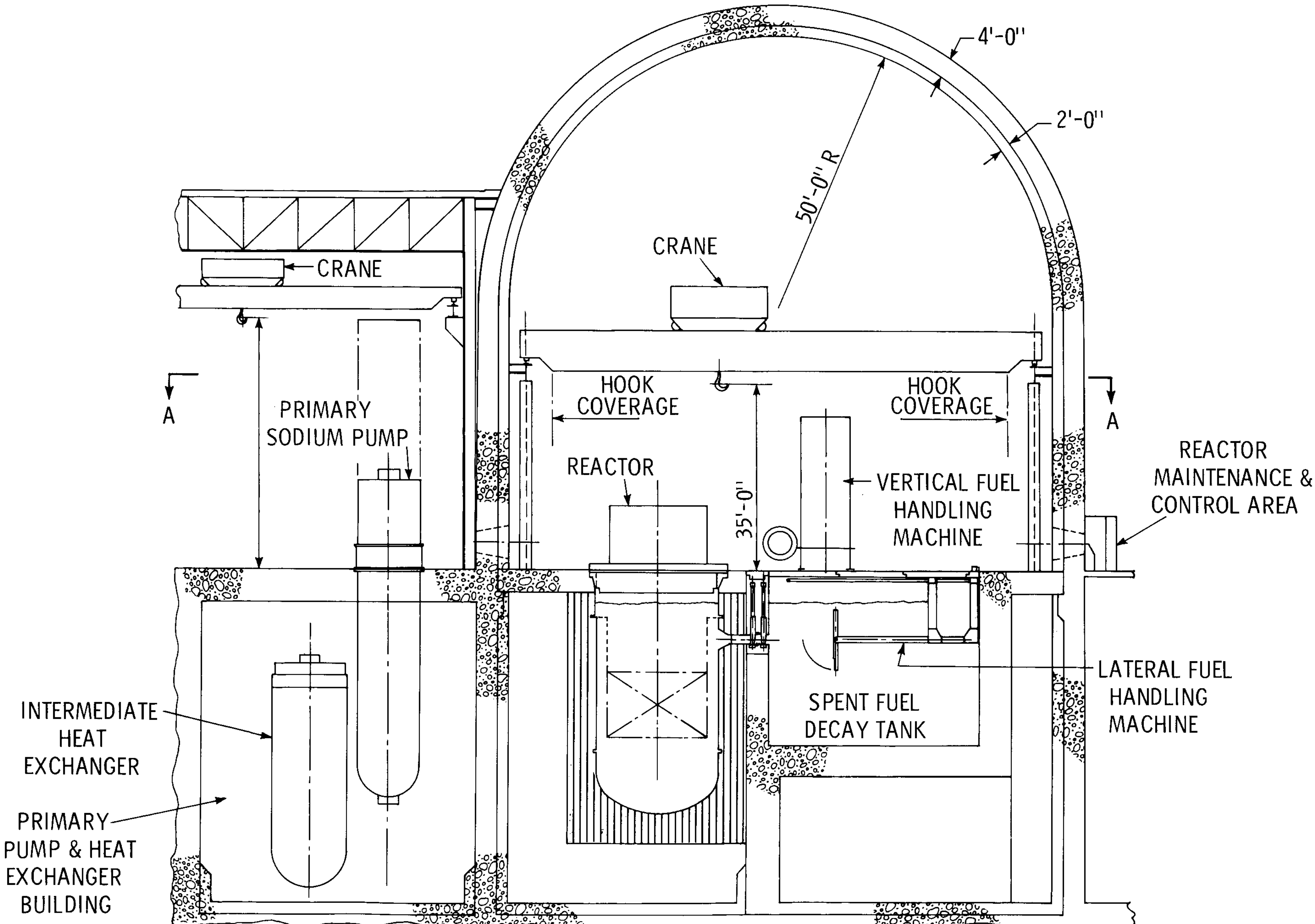


FIG. 5 HOT-CELL REFUELING & CONTAINMENT CONCEPT

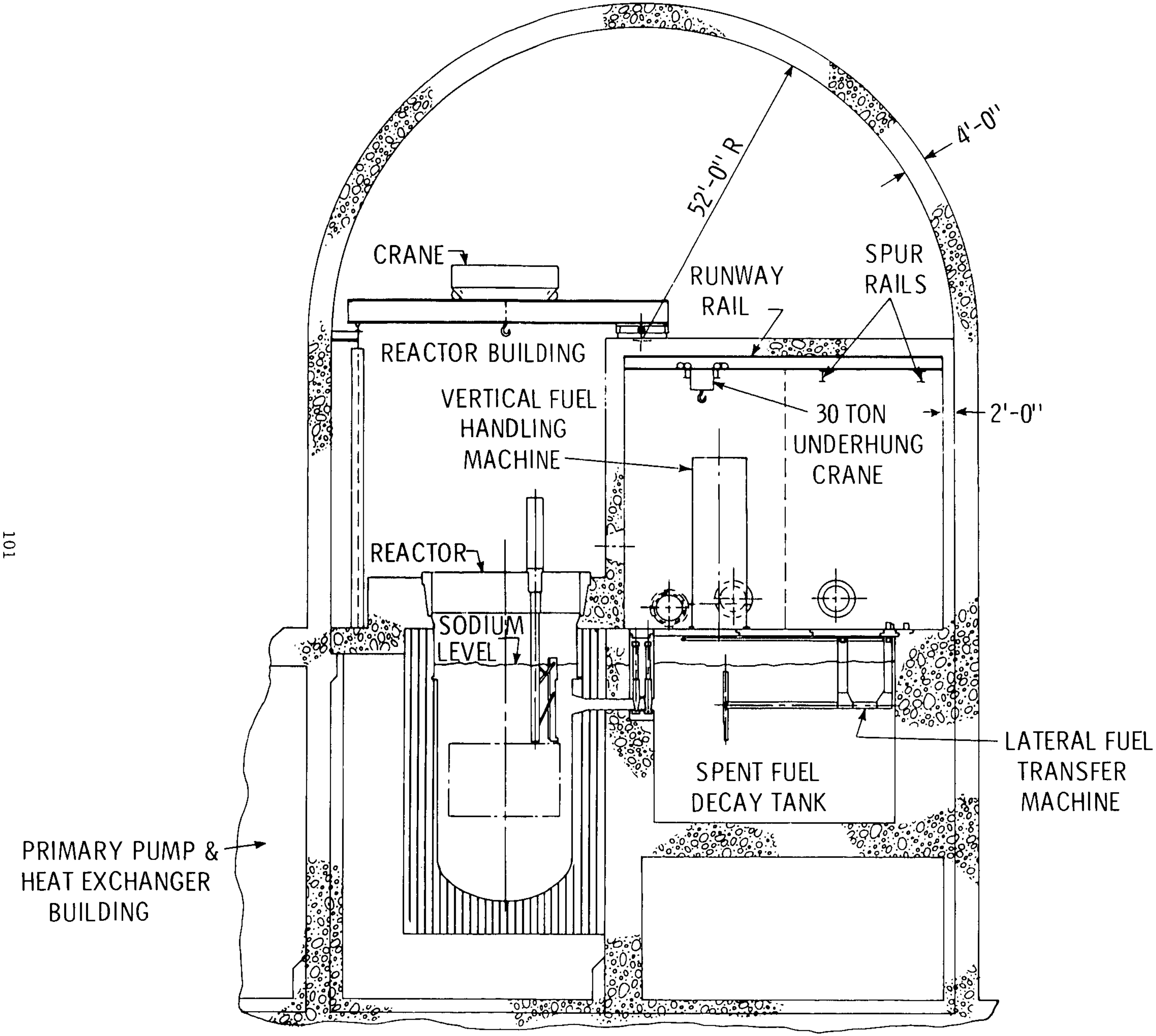


FIG. 6 UNDER-THE-PLUG REFUELING CONCEPT

D. Hot-Cell Containment vs. Pressure-Shell Containment

After a qualitative review of several containment design concepts, and after preliminary safety analyses had provided information for specification of a design pressure of 30 psig, two concepts were judged to merit a more complete comparison. These two concepts, the hot-cell containment concept (Figure 5) and the pressure shell containment concept (Figure 7), were carried through the stages of conceptual design and comparative cost estimation.

The two designs arose from the technical specifications on area, headroom and design pressure. They were judged to be equally safe, although further analytical and experimental verification is needed for both to ensure that no credible missile can penetrate the outermost gas barrier.

The cost advantage of the hot-cell containment design over the pressure-shell containment design is $5,450,000. Changed in equipment layout could decrease this cost advantage by reducing the diameter and thickness of the steel shell in the pressure-shell containment concept, but could not eliminate the cost advantage. Use of the pressure-shell containment with an under-the-plug refueling scheme would result in a more reasonable design, but it is not believed that it would be economically or technically superior to the combination hot-cell refueling and containment of Figure 5. Hot-cell containment was selected.

E. Two vs. Three Coolant Loops

Qualitative studies of component arrangements, probable 1980 manufacturing capability, component capacity requirements and system complexity preceded this comparison of two-loop and three-loop plants. Discussions with the designers and manufacturers of intermediate heat exchangers, pumps and steam generators led to the conclusion that the capability for manufacture of these by 1980 will not limit the choice of a two loop or of a three loop plant of the sizes considered in this study.

The comparison of two loop and three loop plants considered safety, reliability, availability and cost, in addition to the factors mentioned in the preceding paragraph. It was concluded that the larger number of components in the three loop plant decreased overall plant reliability, increased maintenance costs, had virtually no effect on plant thermal efficiency, and increased allowable part load availability (a maximum of 759 MWe for 2-out-of-3 loops, 616 MWe for 1-out-of-2 loops). Safety was not affected by the number of loops; both concepts employ a siphon breaker in the vessel and a combination auxiliary-emergency cooling loop.

The economic comparison considered all parts of the plant that differ for the two concepts, including buildings, heat transfer components, piping, pumps and handling equipment. The result was that the two loop concept cost $4,220,000 less than the three loop concept, and was consequently chosen. Had the result been that three loops were technically or economically superior to two loops, then it would have been necessary to study plants with a larger number of loops. A one loop plant does not appear to be feasible by 1980.

F. Piped Primary Coolant System vs. Integral Primary Coolant System

In the piped primary coolant system, all of the primary system components are interconnected with piping, and are contained in inert gas-filled equipment vaults.

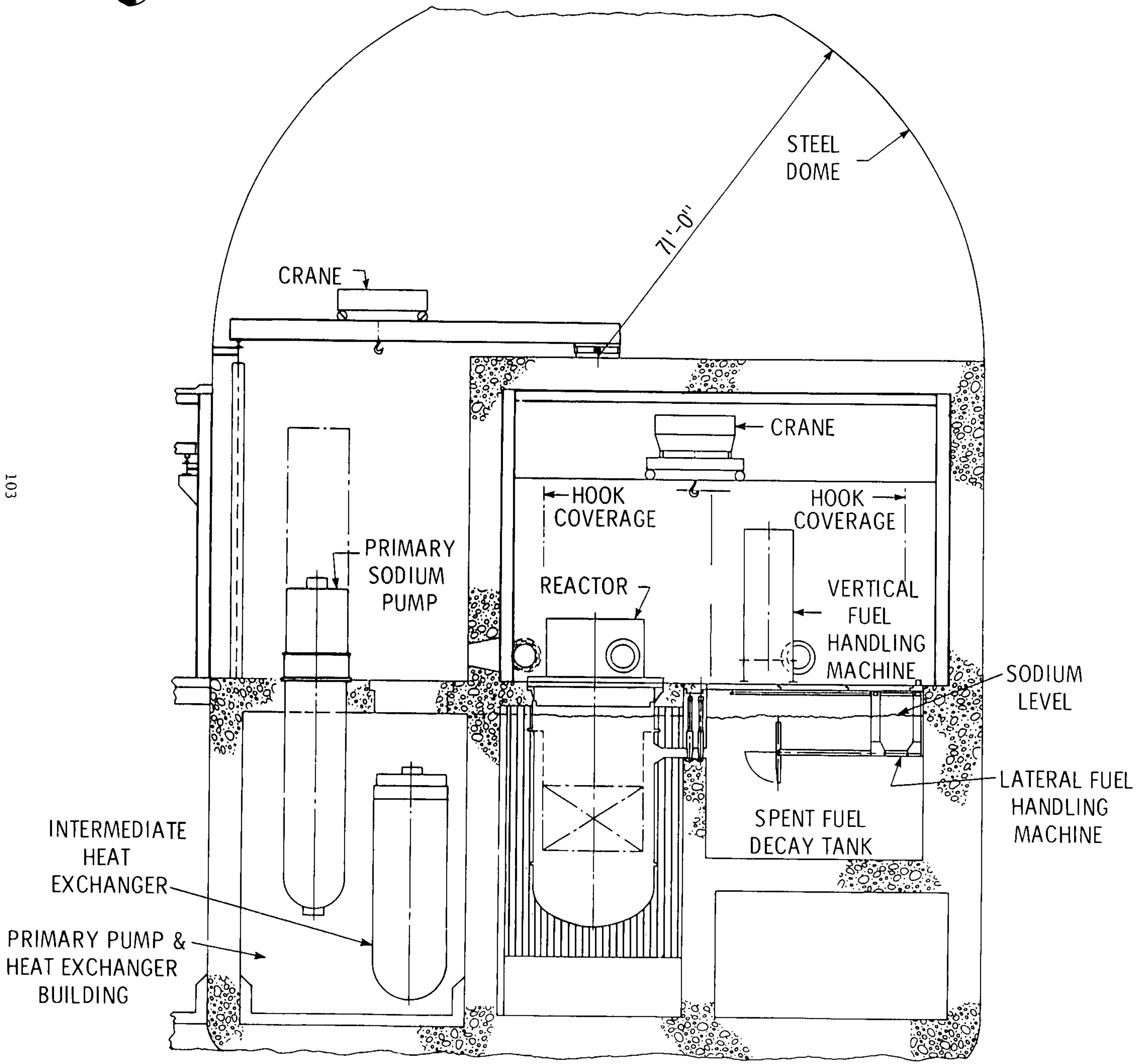


FIG. 7 PRESSURE-SHELL CONTAINMENT CONCEPT

The primary system components of an integral primary coolant system are contained in a common tank which is filled with sodium. The integral primary system coolant communicates with the sodium filling the tank at one or more points.

The advantage normally cited for the integral primary system is that the core can never be drained of sodium -- e.g., as a result of a piping failure. This feature can be achieved in the piped primary system only by a more complex design than would otherwise be needed -- e.g., by addition of a double vessel wall, a siphon breaker or double-walled piping, or auxiliary sodium storage.

The trade-off study of the two systems required that both meet the same operational and safety requirements. After some initial scoping studies, the two designs which were compared are shown in Figures 5 and 8. Both the technical comparisons and the economic comparisons are very complex,[6] and an adequate discussion is beyond the scope of this paper. The two systems are so different that the economic comparison, summarized in Table 3, has a large degree of uncertainty. However, it should be noted that the piped primary system is favored by from \$5,800,000 to \$15,488,000 in capital cost, depending primarily on the assumptions made for vessel costs (\$4/lb to \$8/lb for the piped primary system vessel, \$2/lb to \$4/lb for the integral primary system tank) and emergency cooling requirements.

The piped primary system was selected on the basis of this capital cost advantage, reinforced by the belief that design analysis, design flexibility, and maintenance also favor the piped primary system.

G. Sodium Reheat Steam Cycles vs. Steam Reheat or Moisture Separation Steam Cycles

Straight expansion of steam through the turbines is unacceptable, since excessive erosion of the low-pressure turbine blades would result. As a consequence, moisture separation or reheat are required. The moisture separation can be done internally, between turbine stages, or externally, in a separate moisture separator. Reheat can use either steam or sodium as the heat source.

Preliminary studies indicated that some moisture separation steam cycles may not require an external moisture separator, but one is needed for the 2400 psig/ 900°F condition which is the reference point for this trade-off study. Further preliminary studies showed only a slight heat rate advantage for a live steam reheat cycle over an external moisture separation cycle, and since the steam reheat cycle requires a greater capital investment, live steam reheat was not considered further. This left a comparison of moisture separation steam cycles with sodium reheat system cycles.

The moisture separation cycle is preferred on technical grounds because of the additional complication and reduced reliability associated with the sodium reheaters. The moisture separation cycle is also favored economically. The cost comparison included all plant factors which differed between the two designs (e.g., structures, pumps, other components). Table 4 summarizes the features of the two cycles, and indicates a capital cost advantage of \$6,106,000 for the moisture separation cycle.

In general, the cost difference between moisture separation cycles and sodium reheat cycles depends on the assumed fuel cycle cost, the total plant capital cost, and the capital cost scaling factor, n. Figure 9 shows that the choice of a moisture separation steam cycle is still valid for a very wide range of these variables.

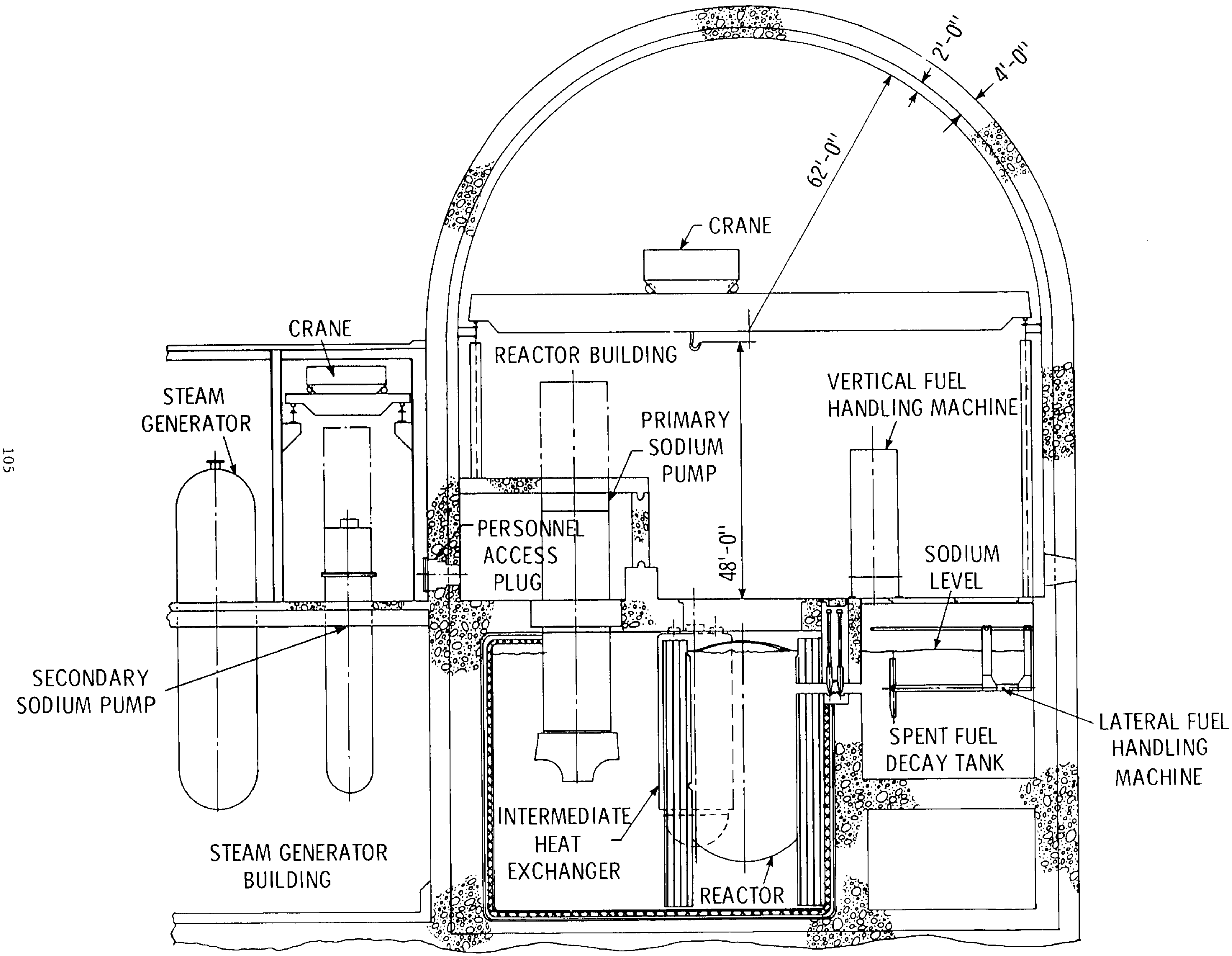


FIG. 8 INTEGRAL PRIMARY SYSTEM CONCEPT

TABLE 3

Comparison of Piped- and Pool-Type Primary Systems

A. Identical Emergency Cooling (Separate Circuits)

	Thousands of Dollars			
	Piped		Pool	
	Low	High	Low	High
Total Direct Costs	20183	24929	26553	35610
Total Indirect Costs	9082	11218	11949	16025
Total Costs	29265	36147	38502	51635

Minimum Differential $ 9,237
Maximum Differential $15,488

B. Different Emergency Cooling (Separate Circuit for Piped Primary System. Pony Motors on Main Pumps for Integral Primary System)

	Thousands of Dollars			
	Piped		Pool	
	Low	High	Low	High
Total Direct Costs	22603	28029	26603	35710
Total Indirect Costs	10171	12613	11971	16070
Total Costs	32774	40462	38574	51780

Minimum Differential $ 5,800
Maximum Differential $11,138

TABLE 4

Moisture Separation vs. Sodium Reheat Steam Cycle

	Moisture Separation	Sodium Reheat
Throttle pressure, psig	2400	2400
Throttle temperature, °F	900	900
Reheat temperature, °F	--	900
Feedwater temprature, °F	500	500
Heat input, MWt	2600	2600
Turbine Type	TC4F/44	CC4F/44
Net Heat Rate, Btu/kW-hr	8490	8281
Net plant efficiency, %	40.20	41.22
Net electrical output, MWe	1045	1072
S.R.-M.S. Capital cost difference, $	--	6,106,000

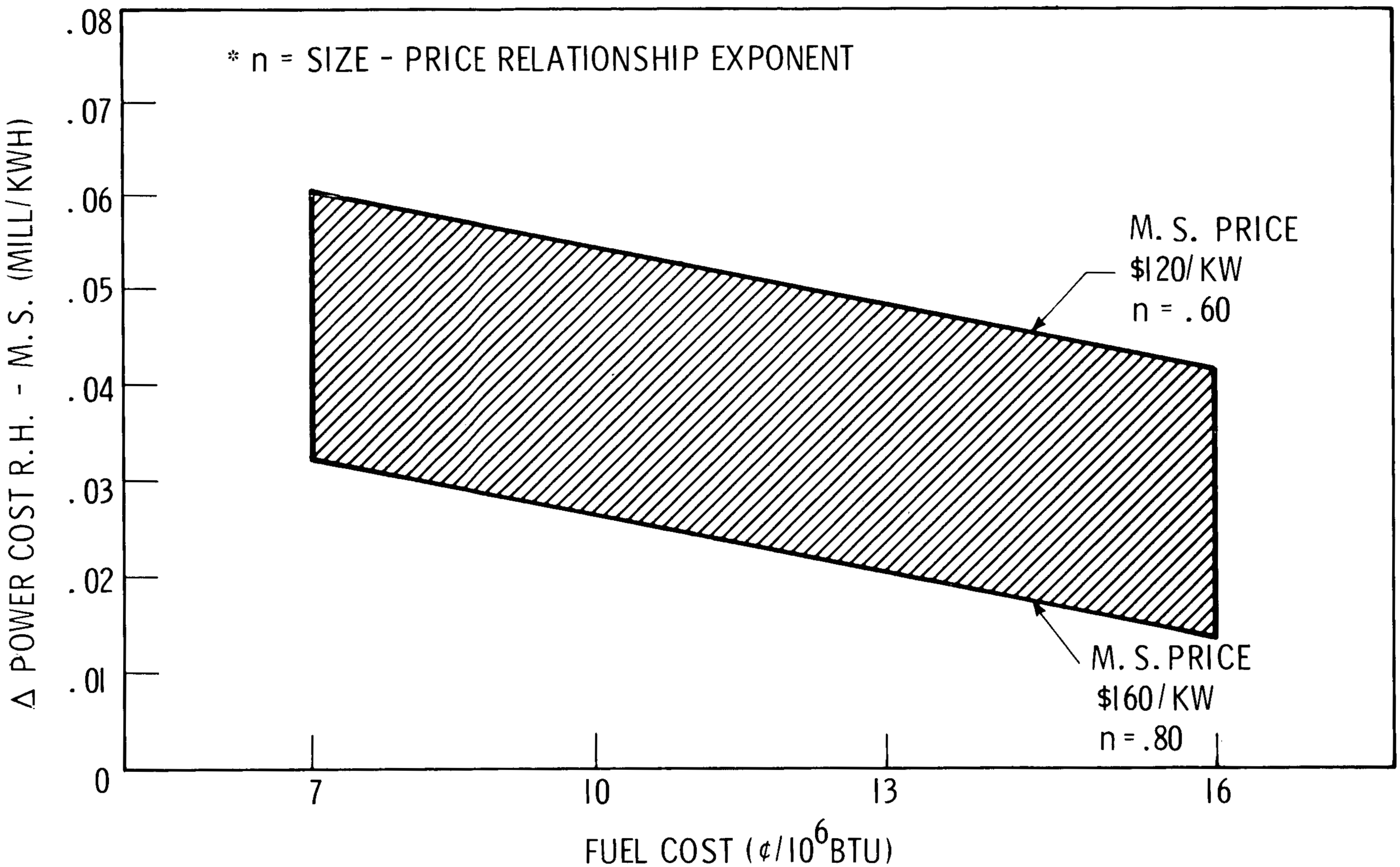


FIG. 9 DIFFERENTIAL POWER COST ENVELOPE FOR SODIUM REHEAT - MOISTURE SEPARATION

It has also been shown[4] that the choice is likely to be the same for a wide range of throttle steam temperatures. The only reason this choice would not be correct would be if the heat rate difference were appreciably more than the 209 Btu/kW hr shown in Table 4.

CONCLUSIONS

The results of these trade-off studies, plus parallel studies on fuel properties, sodium purification, design basis accidents, various IHX and steam generator concepts, component and plant reliability, and the suitability of oxide fuel in a core designed for carbide fuel, define the Task I reference design concepts (Table 5). The next phase of the study will proceed through the conceptual design of the entire plant.

Acknowledgement

The work reported represents the contribution of a number of people in Westinghouse Electric Corporation's Advanced Reactor, Atomic Equipment, Heat Transfer and Large Turbine Divisions, as well as personnel of the architect-engineer, United-Engineers and Constructors. While many other persons contributed to this work, the principal contributors are A.A. Bishop, R.G. Cockrell, W.E. Gunson, J. Jedruch, I.M. Keyfitz, A.J. Lipps, L.E. Strawbridge, J. Sutherland, and J. Wasko of Westinghouse and R. Newbold and J. Crowley of United Engineers and Constructors.

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TABLE 5

Some Characteristics of the Task I Reference Design

Thermal power	2600 MWt
Net electrical power	1074 MWe
Throttle steam pressure	2415 psia
Throttle steam temperature	900°F (no reheat)
Vessel nozzle-to-nozzle pressure drop	90.0 psi
Vessel inlet temperature	770°F
Vessel outlet temperature	1000°F
Maximum fuel centerline temperature	2624°F
Maximum cladding surface temperature	1313°F
Core height	38.4 in.
Core module diameter (effective, one module)	46.6 in.

Tandem compound, quadruple flow, 44 inch last stage blade turbine with external moisture separator.

Two 1300 MWt steam generators.

Two 1300 MWt intermediate heat exchangers.

Two 129,000 gpm primary sodium pumps.

Two 106,000 gpm secondary sodium pumps.

Combined hot-cell refueling and containment structure.

Piped primary coolant system.

Four core modules.

$Cr_{26}C_6$ added to hypostoichiometric (U,Pu)C fuel.

Fuel vented to coolant.

Type 316 SS fuel cladding.

Incoloy 800 steam generator tubing.

All other structural material Type 304 SS.

DISCUSSION

G. Wensch (U.S. AEC) - I am somewhat surprised to see this large cost differential between the one pot design and the vessel and external piping design. The question I have is this - in the one pot design did you include all dump capacity?

C.A. Anderson, Jr. - We had a long hassle on that trying to decide whether we should be consistent and we finally decided that the reasons for which you have full dump capacity in the pipe system is the same as the reasons for which you could want full dump capacity in the pool system. So we did put that in. I don't remember the number associated with that, it was something like 1.2 million dollars for that storage.

G. Wensch - The other part of my question is in having the external piping, did you include all the tracings, x-ray, calrods, programming and heating up the piping?

C.A. Anderson, Jr. - Yes. For everything that was different between the two. Things that they had in common, we did not consider as greatly, I'll tell you, but everyting that was different between the two, such as those you mentioned, we did consider. We had architect engineers who are credited in the paper - they did much of this work for us; we did some of it ourselves.

FAST STEAM COOLED REACTOR: SYSTEM PRESSURE CONSIDERATION, 300 MWE PROTOTYPE AND AN APPROPRIATE TESTBED FOR FUEL ELEMENTS

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Summary

A system pressure of 120 atmosphere has been found suitable for the prototype of the Steam Cooled Fast Reactor by considering economical influences and development risks. A preliminary design for the 300 MWe prototype is proposed. Essential features are a direct cycle, a pressure-suppression containment and a steel pressure vessel with the Löffler cycle components arranged in compact loops. For fuel testing, an all fast reactor is proposed. Its core and main components can be housed in the existing pressure vessel of the Superheat Reactor at Großwelzheim.

INTRODUCTION

The first design ideas of the German 300 MWe fast steam cooled reactor (DSR) have been shown at the ANS Meeting in San Francisco last year[1]. In order to briefly re-introduce the concept on which our considerations are based, a schematic flow diagram is shown in Figure 1. The reactor working as a mere superheater is used in connection with a normal Löffler cycle and the blower is driven by a steam turbine the outlet of which is fed into the medium pressure stage of the main turbine.

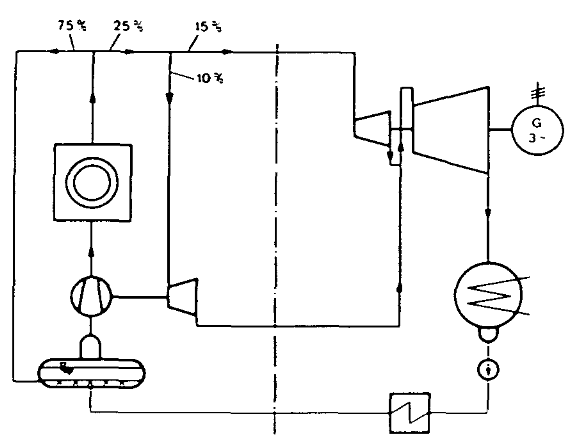

Fig. 1 Steam Cooled Fast Reactor (DSR): Schematic Flow Diagram

SYSTEM PRESSURE CONSIDERATION

After it was decided to use a direct cycle the most important question seemed to be which system pressure should be selected. Two influence areas have been identified:

a. Capital and fuel cycle costs,
b. Safety considerations and development problems.

The first item can be measured under usual assumptions for fuel cost, fabrication and reprocessing costs, and estimates for the cost of plant construction, components, and equipment. However, this is not as simple for the second point mentioned. Only semiquantitative arguments can be found and have to be evaluated.

Cost Influences

In order to limit the parameters to be varied during the analysis, a core lattice was used as proposed in the Karlsruhe D 1 study.[2] No attempt was made to further optimize or re-evaluate these findings which have led to a lattice with fabricable pitch to diameter ratios and fuel element diameters. Furthermore, it is not the target of our prototype to show a maximum breeding gain, but rather an optimum in power generating cost, with emphasis on a reliable and safe system.

So long as one keeps the core lattice and the maximum admissible cladding temperature constant, one can fix the core height according to a maximum breeding ratio, to a minimum fissile inventory, to a certain steam exit temperature or to a maximum cycle efficiency. We did the last, because the efficiency has a strong influence on capital and on fuel cycle costs. The result of this procedure can be seen in Figure 2. With decreasing pressure, the core height must decrease in order to avoid excessive pumping power. The H/D ratio varies from 0.5 to 0.13. Having in mind this way of optimizing the system, one comes to the following set of variables which are shown in Figure 3 as function of pressure. The assumed core has turbulence promoters and the system uses a turbine cycle with reheat. While the efficiency of the system is increasing with pressure, the breeding ratio and the fissile inventory is decreasing. The effect of these variables on the fuel cycle cost is shown in the last diagram of Figure 3. There is a definite but low drop in cost with higher pressure as one would expect.

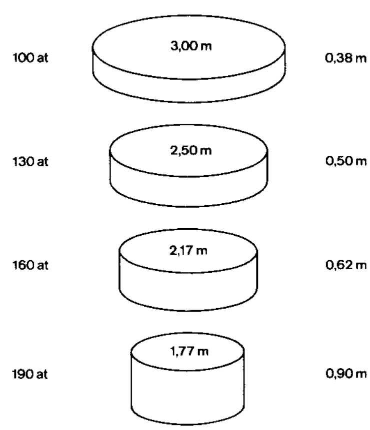

Fig. 2 DSR 300 MWe: Variation of Core Shape with Pressure

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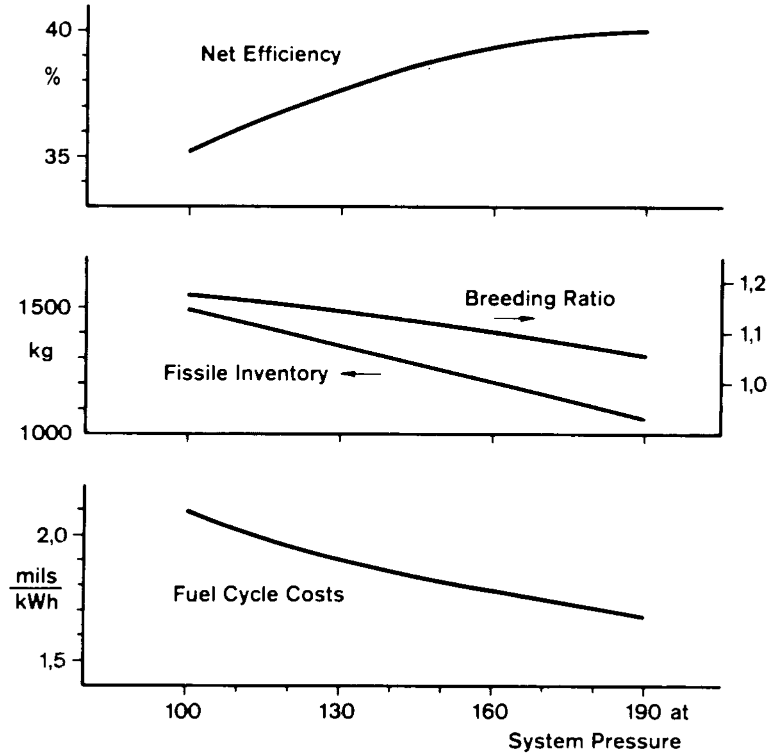

Fig. 3 DSR 300 MWe: Efficiency, Breeding Ratio, Fissile Inventory, and Fuel Cycle Costs as Function of System Pressure

For the fuel cycle cost calculation, the following assumptions have been made:

Plutonium	10 \$/g
Core Fabrication	250 \$/kg
Fuel Reprocessing	50 \$/kg
Blanket Fabrication	50-60 \$/kg

Another factor in this category is the plant cost. We tried to estimate those parts of the system which are essentially pressure dependent. Table 1 indicates that the strongest influence comes from the vessel internals and from the turbine if the absolute prices are considered.

Thus both, the fuel cycle and the capital cost have a weak but clear trend to lower values at higher pressures as has been found also by others.[3] The variation amounts to 5 to 10% between 120 and 160 atmospheres.

Safety Features

To the other category of factors which influence the choice of system pressure belong all safety considerations. Similar to the sodium void coefficient the loss of coolant in the steam cooled reactor introduces a positive reactivity which is more or less balanced by an adequate Doppler coefficient. After preliminary accident analysis the opinion developed that the ratio of loss of coolant reactivity and isothermal Doppler constant should be preferably below a value

TABLE 1

DSR 300 MWe: Estimate of Pressure Depending Capital Costs

System Component	Cost Variation between 100 and 190 at. %	Tendency with increasing pressure
Pressure Vessel	10	Decreasing
Vessel Internals	50	Decreasing
Steam Generator	-	Min. at 160 at.
Steam Blower	-	None
Reheater	30	Decreasing
Piping	10	Increasing
Containment	5	Decreasing
Turbine-Generator	10 - 15	Decreasing

which is about 2. As with decreasing steam density and with the flattening of the core the loss of coolant reactivity decreases considerably, there is a pronounced tendency to lower pressures although the Doppler shows a slight opposite trend. It is difficult to define a distinct value for the system pressure above which operation would be unsafe. But the range below 140 or 130 atmospheres seems to be acceptable.

Fuel Development Problems

There is still the question left which difficulties may arise with higher pressure in order to develop the new components of the system. Steam generator, turbine driven blower and other parts of the cooling loop are considered to be more or less conventional equipment which needs development but the problems should not be much greater than with other machinery and it should not be pressure dependent. The reactor pressure vessel might cause welding problems because its wall thickness exceeds 200 mm (8 inches) at high pressures and the related high temperature; but this point was not thought to be limiting. The most difficult problem, however, are the fuel elements. There is experience with fuel pins under steam cooling only for pressures of 70 at and for lower linear power. Also geometry, neutron spectrum, burnup, and fuel chemical composition are different. While one can compensate the effect of system pressure on the cladding by pre-pressurization of the fuel pin under normal operating conditions, this is not the case after a system pressure loss; because now the pin stands under a much higher internal pressure. Especially at the end of its life when all the fission gas has been produced and mostly been released to the plenum, the probability of a rupture of the pin is greater for higher pressure. This problem is independent of the swelling forces of the fuel. It will be solved mainly by a suitable canning material which reveals the necessary strength and ductility after long time irradiation. Today commercially available material with sufficient corrosion resistance may cause a penalty because one has to have a rather great wall thickness. Apart from these considerations the pre-pressurization of the fuel pins is a new technique and would be accepted with reserve until large scale fabrication experience is available.

Summarizing the mentioned findings it can be stated that the cost calculations emphasize the higher pressure range for the system - and this is congruent with the calculations of other sources - but safety considerations as well as the feasibility of the fuel concept makes it wise to stay with a lower pressure - at least for the prototype, i.e., the first reactor of its kind. Figure 4 represents this situation qualitatively and shows the final decision to set the system pressure at 120 atmospheres at core inlet.

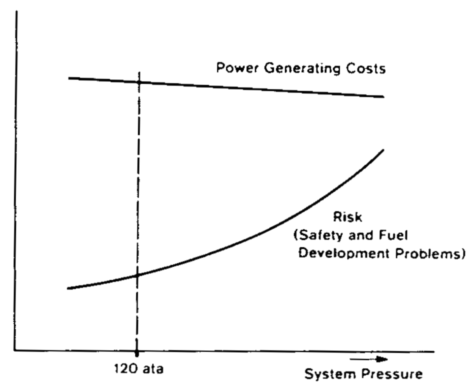

Fig. 4 DSR 300 MWe: Expected Power Generating Costs and Development Risk as Function of Pressure

THE 300 MWE PROTOTYPE DESIGN

Flow Scheme

Figure 5 shows the arrangement of the reactor, the components and the turbine in an direct cycle. Saturated steam is superheated in the reactor to 460°C. The steam flow at the reactor exit is divided into 4 loops for steam generation and to the turbine. The steam circulators are driven by high pressure turbines. The 20 atmospheres exhaust steam is fed into the low pressure section of the main turbine. Steam circulating power is high, 50 MW, which is a consequence of the relatively low system pressure. The essential data of the steam cycle are given in Table 2.

TABLE 2

DSR 300 MWe: Plant Data

Net Plant Output	300	MW
Net Efficiency	35.5	%
Turbine Inlet Pressure	103	ata
Turbine Inlet Temperature	455	°C
Steam Flow	1200	t/h
Turbine Exhaust Pressure	0.04	ata
Feedwater Temperature	218	°C

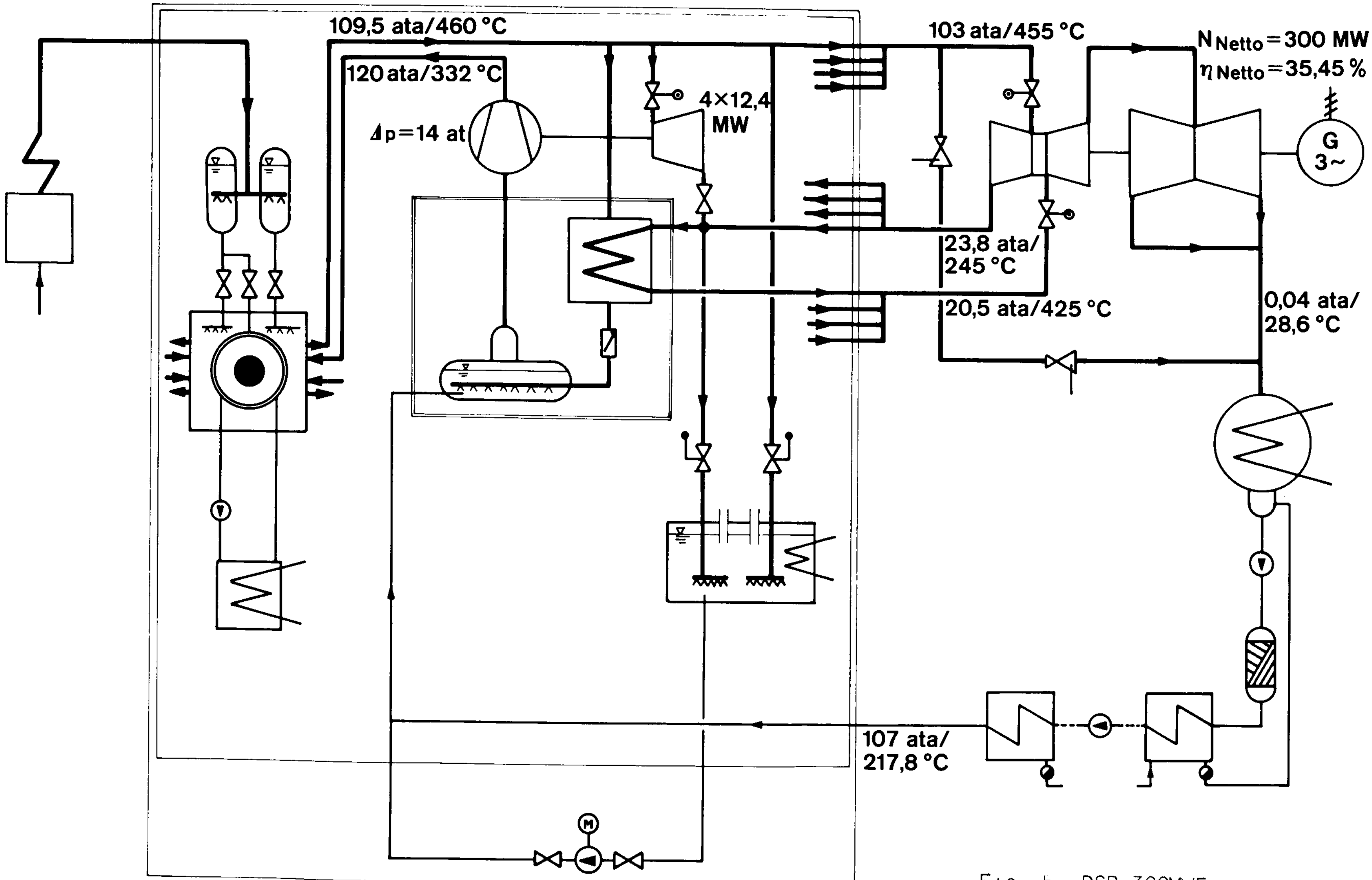

FIG. 5 DSR 300MWE
FLOW DIAGRAM

In the present concept, reheat is provided. This is done by heat exchangers, at 24 atmospheres and 420°C. It looks attractive to omit the reheat, because the plant cost would be lower and at present the rise in efficiency does not balance the difference in plant cost. On the other hand a turbine without reheat at DSR steam conditions would be an extra design with probably higher price, and some of the savings in plant cost will be lost. Furthermore, it is uncertain to what extent the contamination of the turbine is affected by reheat and a clear distinction does not seem possible. At present reheat has been included in the cycle as part of usual steam technology.

Containment

It was straightforward to start a design with a dry containment because the amount of steam inside the pressure vessel and the recirculation loops seemed to be much less than the corresponding amount of water in a boiling water reactor.

Later on, the blow down process was evaluated in more detail and a pressure suppression system was designed in order to explore the incentives of this system for the steam cooled reactor. It turned out that a compact arrangement is possible and for the present design, a pressure suppression system is proposed.

A steel sphere of approximately 30 m diameter houses the reactor, the components of the Löffler-cycle, and the emergency cooling system. The pressure vessel is located in the center of the sphere. The components of the steam circulating loops form a ring of pressure vessels around the reactor outside the concrete shielding. Four loops with blower and contact boiler are provided. Outside of the ring of component vessels, the absorption chamber fills the remaining space up to the wall of the sphere. The drywell is formed by the air volume around the pressure vessels and the components. Steam pipes are arranged in the upper and lower part of the spherical building. The main steam lines are equipped with flow limiters so that steam flow cannot exceed 200% of rated flow in case of a pipe break outside the containment. Besides that, two isolation valves in each line will stop the loss of steam out of the containment. More details will be given in (4).

Pressure Vessel

According to the present thinking about the prototype, the pressure vessel has most of the features of light water reactors. Other techniques like prestressed concrete vessels have not been taken into consideration because the prototype plant should retain as much proven technology as possible.

The pressure vessel, as it is shown in Figure 6 turns out to be smaller in height and bigger in diameter than a boiling water reactor of the same power. Radial shielding is effected by a water blanket. In an earlier design a steel shielding was evaluated as well. It was found that the attenuation of neutrons below 1 MeV was rather weak and that a high amount of steel was necessary for proper shielding. Besides that, the present water blanket will be used as a steam reservoir which might slow down the loss of steam pressure in case of a pipe break.

The pressure vessel is connected with the components of the Löffler cycle by coaxial steam pipes. The location is above the level of the core within the vessel and the core can be flooded, even when a large nozzle is broken. The

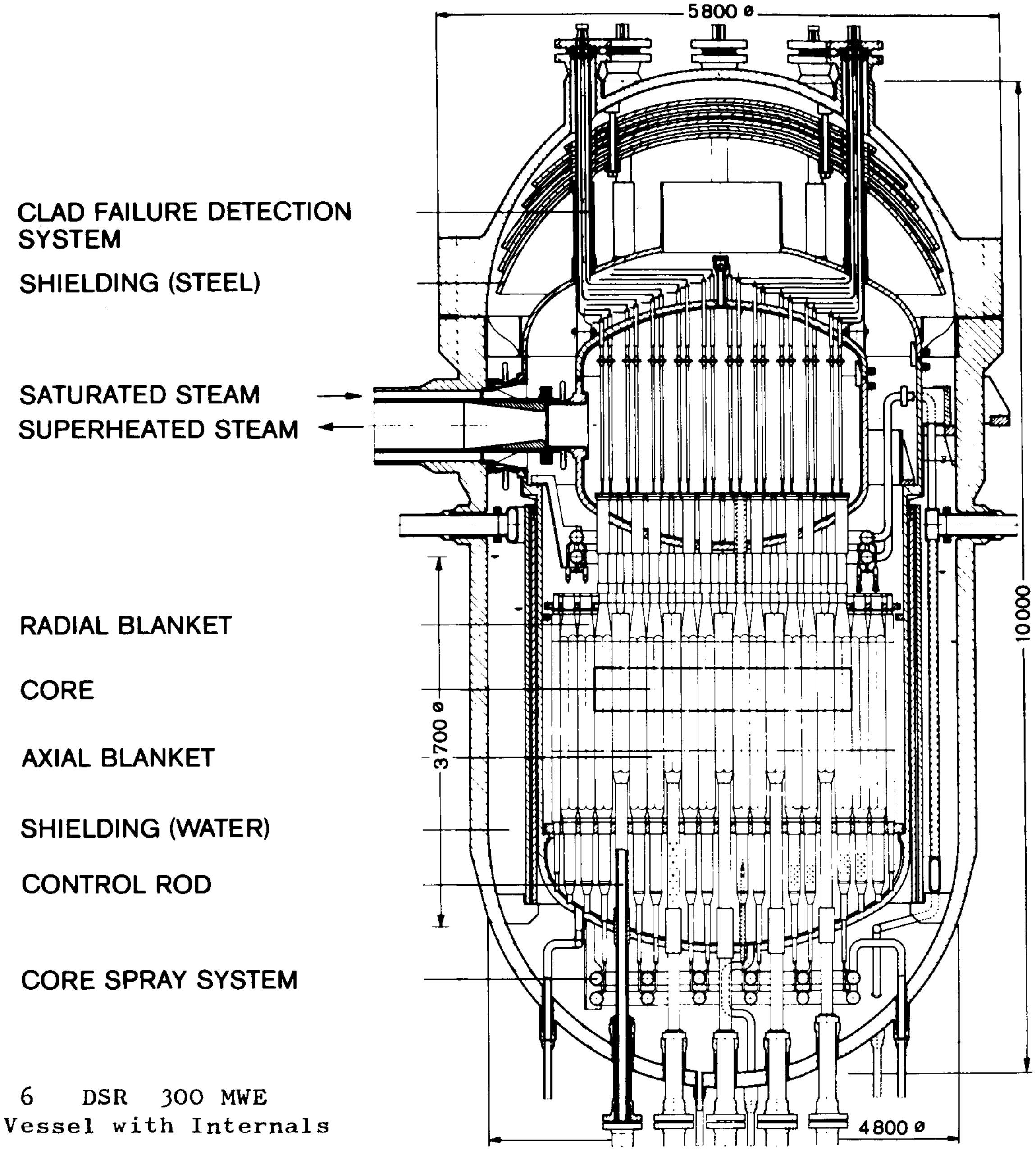

Fig. 6 DSR 300 MWE
Pressure Vessel with Internals

saturated steam enters the vessel in the outer pipe. It is distributed to the fuel bundles of the radial blanket, the shielding assemblies outside the radial blanket and the control rod drives inside the core. After this, the different steam flows are combined into a plenum of slightly superheated steam beneath the reactor core. Finally the steam is heated on its way up through the core where it passes the gas plenum, the lower axial blanket, the core region and the upper axial blanket. The fuel elements are connected to a plenum of superheated steam, where the steam is collected and led to the inner pipes of the coaxial steam nozzles. Each exit pipe is equipped with a venturi type flow limiter.

The control rod drives are at the bottom of the pressure vessel, mainly because this arrangement preserves many features of an existing drive for boiling water reactors so that only minor modifications are necessary. The drive is "reversed" so that shutdown of the reactor is effected by downward movement of the absorber, which is in contrast to the arrangement in an BWR. A preliminary analysis[5] of the shutdown behavior of the system showed that a moderate acceleration would be sufficient for safety purposes, whereas the delay time must be shorter and should be approximately 50 msec instead of 200 msec. Present experiments showed that this can be obtained after some development effort.

Reactor Core

As can be seen from the preceding discussion and the table of core data (Table 3) a flat pancaked core shape results as a consequence of the desire to have an optimal thermal efficiency. This determines the height of the core, because a large height with a densely packed lattice would require much pumping power and a corresponding loss in efficiency. A small height with the same lattice leads to low steam temperatures and again low efficiency. Between both extremes, the efficiency has a flat optimum. In a straightforward application of this analysis, a core was defined with 180 subassemblies, arranged into two zones of different enrichment and 19 control rods. The composition of the core is: 41% fuel, 28% coolant, 25% clad and structure, and 6% control rod.

TABLE 3

DSR 300 MWe: Core Data

Core Diameter	2.59	m
Core Height	0.44	m
Number of Fuel Bundles	180	
Fuel Pins per Bundle	397	
Number of Control Rods	19	
Reactor Inlet Pressure	120	ata
Core Pressure Drop	7	ata
Reactor Exit Temperature	460	°C
Steam Flow Rate	6000	t/h
Average Enrichment	17	%
Breeding Ratio	1.05	
Doppler Constant $T \frac{dk}{kT}$	- 0.008	
Void Reactivity	+ 0.02	
Flooding Reactivity	- 0.03	

The fuel pins nave 7 mm outer diameter with 0.4 mm cladding thickness. 397 of them form a subassembly in an hexagonal array. The fuel pins will be operated with a linear rod power of 490 W/cm in the hottest pin with tolerances included. This corresponds to a central fuel temperature close to melting. The temperature of the clad surface is 650°C, and again this is valid for the hot channel with all factors included. Since the resulting steam temperature would be rather low under these conservative assumptions, a gain in heat transfer was assumed in the design as it can be obtained by turbulence promoters. According to experiments, a gain in heat transfer by a factor of 2 and increase of 5 in the friction coefficient have been applied. The longtime behaviour of such turbulence promoters is still in question, and appropriate experiments are highly desirable.

The shape of the core results in a high leakage and gives a fairly high enrichment of 17% fissile plutonium. Because of the high nickel content of the proposed clad material - inconel 625 - the breeding ratio is only 1.05 which has been calculated for the equilibrium core with fission products according to 75,000 MWd/t discharge burnup of the elements and plutonium in the blankets. The flooding curve, reactivity versus water density in the coolant channels, is relatively flat. The void reactivity is +2%, the flooding reactivity is -3%. These values refer to a core at the end of an equilibrium cycle which is loaded with plutonium with high Pu 240 content.

FUEL ELEMENT TESTBED

As mentioned earlier the development of the fuel elements is a key problem. Unfortunately there is no reactor available which would allow the irradiation of fuel pins under fast neutron spectrum and at the same time have steam cooling at the proper pressure level. One can try to substitute the various conditions by irradiating either under the right steam pressure and thermal flux or under fast flux and low pressure sodium. These tests can be made in existing reactors, as e.g. in the Kahl Experimental Superheat Loop[6] and in the Dounreay Fast Reactor. In the Kahl Loop four fuel pins are presently under irradiation in thermal neutron spectrum (since September 1967) and have reached a peak burnup of more than 30,000 MWd/t at 425 W/cm peak linear rod power. Preparations are made to insert 12 fuel pins in the DFR at October 1, this year.

A year ago in San Francisco we described a Mixed Spectrum Reactor which provides a central test zone with the right conditions. In the meantime, however, it was found that because of the demand to fit the core into the existing pressure vessel of the Thermal Superheat Reactor (HDR) at Großwelzheim[7], the fast core portion became too small and had unwanted nuclear characteristics.

A new design with the same condition of using the HDR pressure vessel was made: an all fast core with 75 MW thermal. The pressure vessel is built for 90 atmospheres operating pressure. In order to arrive as close as possible at the pressure of the prototype design, a pressure tank to house the reactor core is placed into the HDR pressure vessel. Figure 7 shows a cross section through the vessel and its internals. The cooling works as follows: the saturated steam compressed by the 4 blowers which are flanged to the top of the HDR vessel flows down to the core tank, enters first the reflector downwards and goes then through the core upwards, leaving the core tank through steam pipes which prevent the water of the water jacket around the tank from entering the core. The superheated steam now goes two ways: the biggest part enters the water cups of the steam generator which surround the central space in the shape of torusses, the other part of the steam leaves the vessel to the main turbine and the drive turbines of the blowers. The saturated steam generated in the water cups flows upwards and enters the four suction ports of the blowers after having been dryed in the head of the vessel.

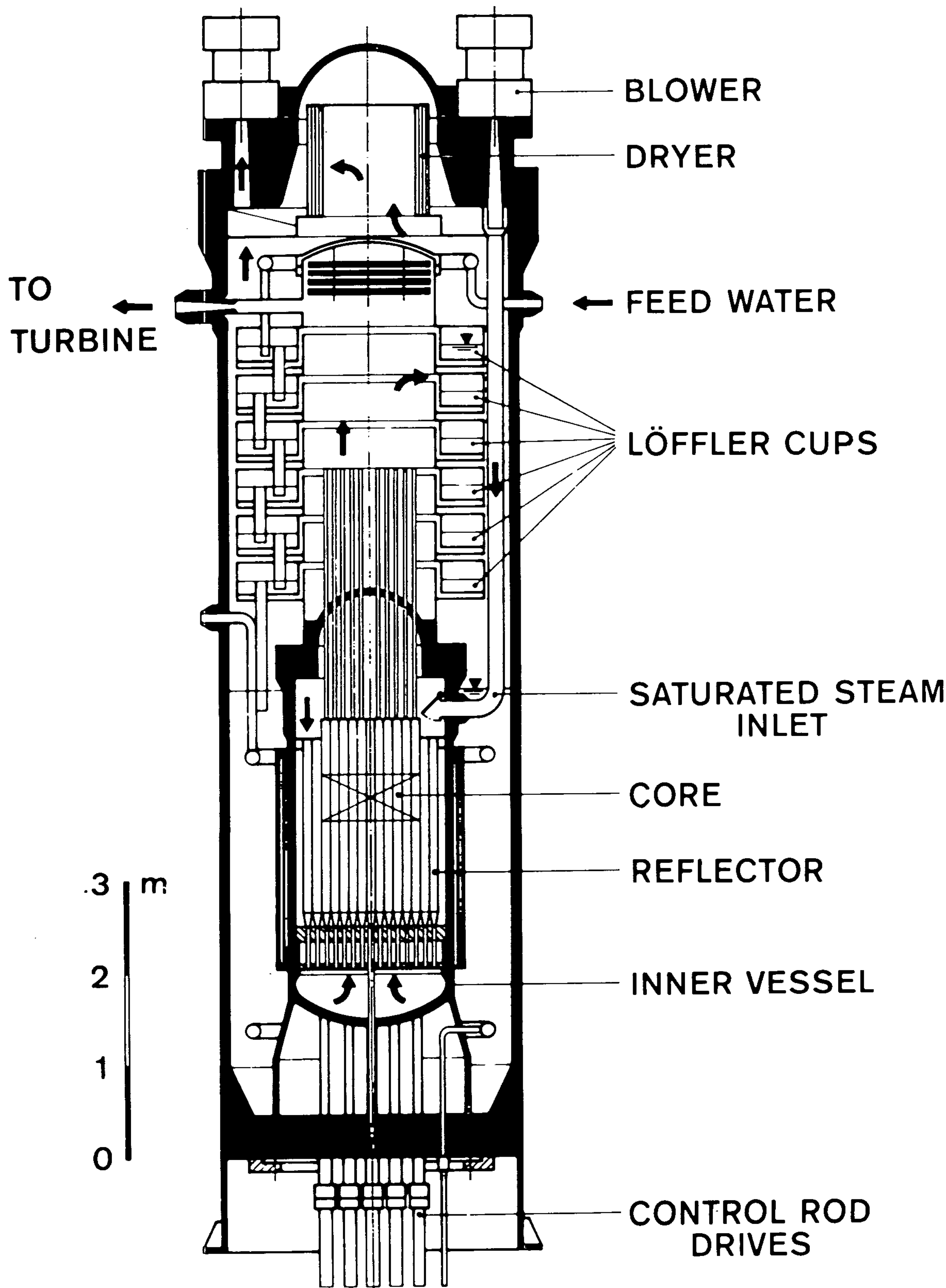

Fig. 7 DSR Testbed

Pressure Vessel with Internals

The arrangement of the internals is such that for normal refueling, it is not necessary to remove the water cups. The lid of core tank provides an opening sufficient to remove the fuel, but not the reflector which is pure nickel. The blowers are flanged to the pressure vessel head such that no external piping is necessary and that a pipe break has not to be considered in the accident analysis. This is not the case for the piping of the drive turbines. The cups are fed with water from the top. The highest cup is working only as a preheater while the other which get their feed water through an overflow from above are generating steam. The water jacket is working as shielding and also as a water reservoir.

The following table gives the main values for the reactor. It indicates that the core is not considered typical as a whole for testing the prototype fuel but that rather a central section of 6 fuel bundles represents the testzone in which the maximum specific power in the fuel is representative for the prototype fuel.

TABLE 4

DSR Testbed: Reactor Data

Total Reactor Power	75	MWt
Power of Testzone	8.6	MWt
Power of Driver	65.2	MWt
Power of Reflector	1.2	MWt
Feedwater Inlet Temperature	115	°C
Reactor Inlet Pressure	103	ata
Reactor Inlet Temperature	325	°C
Reactor Exit Pressure	90	ata
Reactor Exit Temperature	405	°C
Steam Flow	800	t/h

The total power of the reactor is determined essentially by the space available in the vessel. Both, the space for the core and the available height for the Löffler with a sufficient central opening for refueling limited the power to a maximum of about 80 MW. The reactor inlet pressure was achieved by adding the total pressure drop to the exit pressure which is governed by the existing pressure vessel. The temperature of the superheated steam is with 405°C fairly low. The reason lies in the assumption which was made with this first design that turbulence promoters should not be applied - except in the test zone. It was also assumed that the only available spacers are spiral wrapped wires, a design which has some drawbacks and gives a higher pressure drop than integrally finned tubes.

The main core data are given in Table 5. They are very similar to those of the prototype design except for the core diameter. For a better comparison of the fuel data in the prototype design and in the testbed Table 6 lists the main values which show apart from the mentioned difference in the pressure level, two other essential differences (a) The fast flux in the testbed is by a factof of 2 smaller than in the prototype, which is a consequence of the higher fissile concentration, and (b) the number of pins per bundle is only one fourth of that in the prototype design.

We consider these differences to be not detrimental for the representative testing of fuel for a fast steam cooled reactor. It is planned to start the operation of the testbed early 1972.

TABLE 5

DSR Testbed: Core Data

Core Height	0.5 m
Lower Reflector Height	0.4 m
Upper Reflector Height	0.3 m
Equivalent Diameter	
Testzone	0.26 m
Driver	0.94 m
Reflector	1.43 m
Number of Test Elements	6
of Driver Elements	72
of Control Elements	19
Number of Pins per Element	91
Outer Pin Diameter	7 mm
Pitch of Pins	8.2 mm
Canning Wall Thickness	0.4 mm
Canning Material	Inconel 625

TABLE 6

Comparison of Fuel Design Data for
the DSR Prototype and Testbed

	Prototype	Testbed
System Pressure (at)	120	103
Max. Linear Rodpower (W/cm)	490	465
Flux 1.4 MeV (n/cm^2 sec)	$6 \cdot 10^{14}$	$3 \cdot 10^{14}$
Active Coreheight (cm)	44	50
Pellet Diameter (mm)	6.02	6.02
Number of Pins per Bundle	397	91
Max. Can Temperature (Centre of Wall) (°C)	680	680
Max. Steam Velocity (m/sec)	45	70
Composition of Oxyde fuel	25% Pu 75% U nat	30% Pu 20% U 235 50% U nat

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DISCUSSION

G.B. Melese d'Hospital (Gulf General Atomics) - In view of the low net thermal cycle efficiency that you mentioned, which is probably due to the very high blower power, the low breeding ratio that you quote - and it is still not clear whether you included the new alpha value of plutonium which might affect this number, and the high fuel cycle costs that you also mentioned in your talk, it is not too clear for what reason there is much work being done in Germany right now on steam cooled reactors. I wondered if you would care to compare this work with the work also being done at Karlsruhe on sodium reactors.

P. Kilian - The second question should be answered first. We did not include the effect of the high alpha values; we used the old cross section tables of the AEN set. The breeding ratio is low because of the high nickel content of the cladding materials and it would be possible to use Incoloy instead of Inconel as has been suggested by many other groups. That would be all right affording 10 or 20 points in breeding ratio. There is considerable pumping power, a drawback for this design, which makes all of the loss of efficiency and which is undesirable. The third question concerning fuel cycle costs; these numbers have not been made to show economic potential. The corresponding figures should be taken from the B-1 design and the sodium-1 design of Karlsruhe and we make private assumptions which don't belong to an established market in fast breeders and try to take into account the permutation of costs which might be expected for an assumed prototype in Germany and for highest interest rates and so on. So the second inference is from the higher plutonium content and is not representative of a large plant. It was possible to make a comparison of the fuel cycle costs for the large plants and this one and it seemed to be consistent.

D.C. Schluderberg (Babcock & Wilcox) - I was wondering if you would care to tell us the maximum clad surface temperature that you have in mind for use with Inconel-625 and also if you could give us some additional information about the fuel assembly design philosophy, for instance, the spacer grid, the spacing of the pins, what sort of fuel boxes do you plan to use, and so on. Incidently, in your defense, the use of Inconel-625 is a very conservative approach because the nuclear cross section may be twice as high as it would be for type 316 stainless and this should be taken into account when you compare a gas design with a steam design as you described. If you use 316 stainless, you would certainly get a considerable improvement in breeding ratio.

P. Kilian - This latter inference has been identified already and we evaluated comparative designs with Inconel and even with stainless steel and found a considerable increase in breeding ratio. The fuel element design rests on the concept that the fuel pin should have six integrated pins, spiral pin spacing and they will be compressed together by a cellular channel which is a suitable structural material, Inconel or Incoloy. This channel has considerable thickness according to the present thinking. Maximum cladding temperature has been allowed to be 650°C which is a very low number, and this has been applied to the very hot spot including all statistical hot channel factors. This makes the numbers look rather poor, but we feel that we should have a conservative approach to get this thing working and separate from this, there must be concept studies for large plants which have a little more optimistic view.

G.A. Englesson (General Electric) - Is this a proposal or a contract to run a fuel element system in the superheat loop?

P. Kilian - At the moment, the numbers which I showed is an intermediate step in the direction necessary to get such a design and we adopted the approach that we should make the plunge and later care for the fall. This work is in progress and will be finished in August, approximately. Our contract is that we make a proposal at the end of 1969 so there are some funds with which to work and we hopefully can use it for improvement of the core characteristics.

A UTILITY COMPANY PERSPECTIVE ON FAST-BREEDER DEMONSTRATION PLANTS

C.B. Zitek

Commonwealth Edison Company
Chicago, Illinois

There are compelling reasons for the electric utility company's interest in developing full-scale fast breeder reactor power plants. The greatly accelerated growth rate of thermal reactor installations has made it urgently desirable for the utility to pursue fast breeder technology.

The prime reason behind the utility's interest in fast breeders is the prospect of lower electric generating costs. Fast breeders offer vastly more efficient use of fuel, including the ability to use plutonium and, ultimately, the depleted uranium discharge from thermal reactors. Commercially feasible fast breeders seem to have the potential to lower fuel costs and make nuclear kilowatthours largely independent of U_3O_8 costs. Stretch-out of uranium reserves is also in the national interest.

Full-scale fast breeder reactor power plants probably cannot be successfully built without going through the demonstration phase. Therefore, we at Commonwealth Edison Company think that at least two different types of demonstration plants should be undertaken in the next few years for operation in the mid-1970's.

The decision to proceed, however, must await successful completion of the demonstration plant design studies now under way. There is much uncertainty about fast breeders and it is still too early to abandon any of the concepts receiving active consideration today. By about 1970, the utilities should be ready to commit themselves to at least the first demonstration plant. We believe such a plant should be financed jointly by the owning utility, its partners and the manufacturer, perhaps with government assistance in the form of research and development support. Operation should be by the owning utility. Since these demonstration plants will be looked upon as prototypes of full-scale commercial plants to be built later, they must first demonstrate to the utility industry the safety, reliability and potential economy of operation which is necessary in generating facilities.

Commonwealth Edison's proposed approach to the fast breeder demonstration plant is similar to that which we followed in arriving at the decision to build Dresden Nuclear Power Station in 1955. In 1951 a study group was set up to study the feasibility of building a nuclear power plant. After making studies of the various reactor concepts being proposed, one of these, the boiling water reactor, was selected for closer study. Two members of the study group were loaned to Argonne National Laboratory; one to work with Argonne personnel on the Borax Reactors and the other to work on the EBWR design. Three other engineers were also loaned to Argonne during start-up and initial operation. When, in 1955 General Electric Company proposed a 180 MWe boiling water reactor design to Commonwealth Edison Company, because we had confidence in the basic reactor concept, the contract to build Dresden Station was consummated.

Another study group was then assigned to follow the assist in the detailed design work.

Early in 1967 Commonwealth Edison Company set up the Advance Reactor Study Group to follow developments of fast breeder reactor technology and to actively engage in sodium-cooled fast breeder reactor demonstration plant design studies. In 1967 we entered into fast breeder reactor design study contracts with both General Electric Company and Westinghouse Electric Corporation. An engineer was assigned to each company to work with the manufacturers' engineers on their demonstration plant design studies. The objective of each of these study contracts is the design of a sodium-cooled fast breeder reactor demonstration plant which could be committed to construction within two or three years. A third member of the Advance Reactor Study Group was assigned to follow the AEC's LMFBR program and was loaned to ANL to become acquainted firsthand with the operation of EBR-II. As the GE and Westinghouse studies are completed, our objective will be to evaluate the potential for successful achievement of their design goals along with the progress in fast breeder technology. Before making a decision to build a fast breeder demonstration plant, our Company and the manufacturer must have full confidence in the plant design.

As to demonstration plant design, we are in accord with the LMFBR program's choice of plant size. Dresden Nuclear Power Station's original power level of 180 MWe was a scale-up of about a factor of forty, compared to EBWR and VBWR. The present fast breeder demonstration plant design is shooting for about 300 MWe, which is a scale-up of a factor of about twenty from EBR-II, or only a factor of three or four if compared to the Fermi plant. However, the risk seems to be about the same considering the state of the fast reactor and sodium technology compared to that of the thermal reactors and water technology in 1955.

There are four principle areas that the demonstration plants must prove out to satisfy the needs of the electric utility companies. They are:

1. Safety
2. Operability
3. Reliability
4. Potential economics

First: Safety of the prototype sodium-cooled fast reactor power plant is paramount. No utility company will consider building a demonstration power plant that is not safe. All of you know that a requisite of the demonstration plant design is that it will be licensable by the AEC. The individual manufacturers cannot be sure that design of the safeguard equipment will be acceptable to the AEC until they have had a session or two with the AEC Division of Reactor Licensing and perhaps even with the ACRS. The safety of a fast reactor of necessity includes instrumentation. The LMFBR program calls for an extensive research and development program on instrumentation which extends into 1974. One of the items we will be looking at is the need for in-core instrumentation which at the present time has limited technology at the high temperatures being considered.

The instrumentation installed in the Dresden 1 thermal reactor has proved itself at operating temperature 550°. However, the fast reactors are aiming at 1050°F and higher. This is one area in which the increase is probably too great. Perhaps it would be prudent to take a smaller jump in temperature unless adequate instrumentation performance in the reactor core or vessel can be proven through other developments.

Second: The demonstration plant must prove out the overall operability of the sodium-cooled fast reactor power plant. Operability of course includes startup and shutdown, operation at steady state, maneuverability, refueling, and maintainability.

A. Plant startup and shutdown should be smooth and not have nuclear or thermal characteristics which impose hazards to the reactor or plant components. The instrumentation of the safety system should prove out the absence of nuisance or spurious trips. The dual safety system of Dresden 1 proved very effective in this area.

B. Operation at full-power steady-state conditions sounds easy but unless the individual plant components have been properly engineered, designed and built, this state of operation may impose severe limitations on plant output. Again, Dresden 1 operation proved the manufacturer's ability to design the individual components with a good degree of accuracy.

C. Operation of the demonstration plant must prove capability for maneuverability. Electric load pickup and rejection must be done smoothly and quickly. Again we can use Dresden 1 as an illustration. Dresden 1 was operated base loaded during its first fuel cycle but then was put on a load following schedule along with the conventional fossil-fired units. It proved itself very well and actually was more easily controlled than a conventional coal-fired unit. The magnitude of the load swings was limited by that of the turbine-generator unit. We will expect the fast breeder demonstration plant to prove itself in the same manner.

   There are some electric utility companies which may not require this load following capability in a 300 MWe plant. When the full scale plants of 1000 MWe are built, however, the load following capability will be a necessity and must have been demonstrated in the prototype plant.

D. Refueling of the reactor core actually is part of plant operation. This is an item of great concern and the method of refueling and the refueling equipment of the prototype plant will have to be proven out. Although our operating personnel are experienced with direct viewing of the refueling operation of thermal water reactor cores, we have no pre-conceived notions regarding open-head versus through-head fueling. We hope our study efforts will help us determine the best method. It will be important to accomplish refueling in as short time as possible.

E. Last, but not least, maintainability of the sodium-cooled fast breeder reactor system must be demonstrated. Maintenance and possible modification of sodium systems must be done in the minimum amount of time. Worker safety and radiation limitations make this a challenge. Generally there are three phases of maintenance work in bringing in a new power plant. (1) During the first phase of operation, the amount of maintenance is high because of getting the "bugs" out of the systems and possibly making modifications. During this period, the radiation problem is slight and possibly non-existant. (2) During the next few years, the amount of maintenance should be low and the radiation problem starts increasing. (3) The next phase of maintenance will increase in amount and the radiation problem will increase, particularly if the plant continues operation with "leaking" fuel elements. It is difficult to predict how long after plant startup this phase of maintenance will begin.

Experience at Dresden 1 has indicated that a demonstration power plant has to be in operation at least five years or even longer before a true assessment of maintenance problems and costs can be made. Utility management will want to view fast breeder demonstration units in operation for four or five years before deciding to build commercial plants. Therefore, the current thinking of making the decision to build a full scale fast breeder power plant after only two to three years of the prototype's operation is open to question.

The third area of concern which has to be satisfactorily demonstrated by the prototype fast breeder power plant is reliability. The availability factor should be high -- about 90%. The plant should not be plagued with nuisance or spurious shutdowns. A 300 MW unit tripout on a 14,000 MW electrical system is a small percentage and probably would not cause loss of service to customers or initiate a "black out" in the area. However, when the full scale plants come into operation, the problem takes on greater significance.

There is another serious problem resulting from spurious tripouts. Each time the unit is tripped out from full load, stresses will be imposed on individual components that may eventually cause damage to equipment and possibly the fuel also.

The demonstration plant should be reliable according to the dictionary definition also. When the load dispatcher asks for a load pick-up, the operator should be able to increase the unit's output with a flick of a switch. When a scheduled startup is underway, the unit should be on the line at the scheduled time, not hours late.

The last of the four principal areas to be proven by the prototype power plant is the potential economics. It will be difficult for the first two prototype plants to demonstrate the expected economics of the full scale fast breeder power plants. First of all, the capital costs are expected to be high and the first core fuel cost will not be lower than that of the thermal water reactors. However, the utility companies will expect the fast breeder reactor costs to follow the pattern set by the rapid decrease in cost of the thermal reactor power plants, but again it will take more than two or three years' operation to prove out the expected lower fuel costs and eventually lower capital costs.

In conclusion, Commonwealth Edison Company believes that fast breeder reactor technology is advancing at a rate that will in the next few years hopefully justify at least two demonstration or prototype fast breeder power plants to be undertaken. However, the designs will not have to be optimized to the n'th degree, nor will the operating temperatures have to be pushing the upper limits. The designs will have to prove out the four principle areas of concern:

1. Safety
2. Operability
3. Reliability
4. Potential economics

DISCUSSION

G.L. Weil (Consultant) - I just wanted to ask whether the Commonwealth Edison Company has considered the reprocessing of the fuel, in other words, since pipe line inventory is an important part of your economics, are you looking at both reprocessing at plant site as well as the shipment to some reprocessing center?

C.B. Zitek - I am sure that both of our design studies have not reached that point. Just off hand, I would say that we would not consider on-site reprocessing and right now, as far as I know, the fast reactor oxide fuel should be able to be reprocessed in the conventional aqueous reprocessing facilities.

F.A. Smith (ANL) - You mentioned that there is about the same element of risk for the sodium fast breeder as there was for Dresden and especially in the area of sodium technology. Would you care to amplify your definition of sodium technology especially with emphasis on the words risk associated with it.

C.B. Zitek - I thought that comment may cause some concern. This is really a personal feeling that sodium technology has not advanced to a great degree in the past six or seven years. When I got out to EBR-II, I still had the reaction that they had not learned a lot about sodium since the EBR-I days. Perhaps I am wrong.

G. Wensch (U.S. AEC) - First of all I wish to commend the speaker for a very realistic presentation. I have two questions - the first one. What is the criteria which made you select load following characteristics for your demonstration plant rather than put it on a base load condition?

C.B. Zitek - I think the operation of Dresden I illustrates our philosophy that whenever we put a unit on the system, we expect it to be able eventually to operate like a conventional unit, and regardless of how low the fuel costs are in a fast breeder reactor, a utility company will not be able to say we are just going to base load that unit and take all the swings on some other units - it just doesn't work that way.

G. Wensch - That is why I raised the question - because the fuel costs are low, it seems to me you would put it on a base load average. The other question I have for Mr. Zitek is a fairly simple one. You talk about potential economics of the plant. Are you saying that the first demonstration plant must be economic? Or would you look towards some other arrangement with the reactor manufacturers or someone?

C.B. Zitek - Well, I think when we come to the position of making a decision, the finances will probably be handled in much the same manner as we did for Dresden I. The capital costs may be partially covered by research money, but I doubt very much that we would expect the demonstration plant to be able to compete with the other units.

4 TECHNICAL AND ECONOMIC CONSIDERATIONS AFFECTING THE DESIGN OF AN EARLY COMMERCIAL LMFBR

By

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ABSTRACT

Atomics International is one of five contractors participating in the Atomic Energy Commission's Liquid Metal Fast Breeder Reactor Follow-on Study Program. This paper reports on the major considerations, trade studies, and analyses leading to the selection of the AI 1000-Mwe base design LMFBR for Task I of the study. The major trade studies relate to the selection of: (1) a primary system arrangement, (2) the number of heat transfer circuits, (3) the core height and major process temperatures, and (4) the fuel handling concept. The selected design features a mixed Pu-U fueled regular geometry core, a loop concept with three heat transfer circuits, and a double rotating plug fuel handling system. The reactor inlet temperature is 760°F, and the outlet temperature is 1060°F. The steam cycle is 2400 psi, 900°F, with reheat to 900°F.

INTRODUCTION

Atomics International (AI) is one of the five contractors on 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 objectives of this study are to identify the research and development (R&D) necessary to lead to a safe, reliable, and competitive LMFBR, and to establish relative values of proposed R&D. The study objectives will be met through the development of a reference plant design and evaluation of the design to determine the R&D program necessary to proceed with the final engineering and construction of the plant. This paper reports on the major considerations, trade studies, and analyses leading to the selection of the AI 1000-Mwe base design LMFBR (Task I of AI contract).

AI, in performing Task I, has drawn heavily from the Company FBR program and from the recently completed study made for The Empire State Atomic Development Associates (ESADA) of the technical feasibility and economic performance of a near-term 1000-Mwe FBR. The relationship of these

*ANL Contract No. 31-109-38-1966

programs and the proposed plan leading to large LMFBRs is shown in Figure 1. The demonstration plant will be a prototype of the large unit. The AI demonstration plant program will utilize much of the on-going AEC LMFBR effort, as well as the FBR development work in progress in the United Kingdom and other European programs.

PLANT DESCRIPTION

The basic flow diagram is shown in Figure 2. Heat is transferred from the radioactive primary circuit to the nonradioactive secondary sodium circuit for the production of steam. The primary coolant system is composed of three identical heat transfer loops, operating in parallel, with a common inlet and discharge from the reactor vessel. The primary coolant enters at 760°F; and, with a 300°F temperature rise, it exits at 1060°F. The energy is transferred from the primary system to the secondary system in the intermediate heat exchanger. The secondary system consists of three independent loops, one in series with each primary loop. The secondary sodium is pumped through the shell side of the intermediate heat exchanger and steam generator. Steam, at 2400 psig/900°F/900°F is generated in single-wall, shell-and-tube heat exchangers of the modular type. The turbine-generator complex is similar to that used in fossile fuel plants.

The reactor building, shown in Figure 3, is of conventional reinforced concrete, with a steel liner to control leakage. The building houses the reactor and fuel handling equipment, and all portions of the primary sodium system, including the intermediate heat exchangers (IHX). Double containment is provided by the building liner and the cells which enclose the reactor and primary system, to protect against the accidental release of radioactivity.

The steam generators are located in a separate, enclosed, steel structure. 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 paragraphs briefly describe the major portions of the plant.

Reactor

The major reactor characteristics are shown in Table 1. The reactor consists of a central cylindrical array of fuel elements (i.e., the core), surrounded by two rows of blanket elements. The core fuel material is PuO_2-UO_2, and the blanket material is depleted UO_2. Tantalum control rods are used.

The reactor is shown in elevation in Figure 4. The vessel has three large inlet and outlet main coolant loop pipe penetrations, and three small inlet penetrations to provide a small amount of flow to the low-pressure region below the grid plates. This latter supply of coolant is used for thermal convection cooling of the control rods and spent fuel elements in storage. Sodium enters the reactor at the bottom high-pressure plenum, and flows upward past the fuel and blanket elements. Fixed orifices are used for control of flow through the fuel elements. The elements are held down against the upward flow of coolant by a hydraulic holddown arrangement in the lower plenum.

The upper closure of the reactor vessel is a double rotatable shield plug which acts as part of the fuel handling system. The reactor is refueled on a six-month cycle, with one-third of the core, one-sixth of the inner blanket, and one-ninth of the outer blanket being replaced at each refueling. Two refueling machines are used, one for in-vessel fuel handling and one for ex-vessel handling.

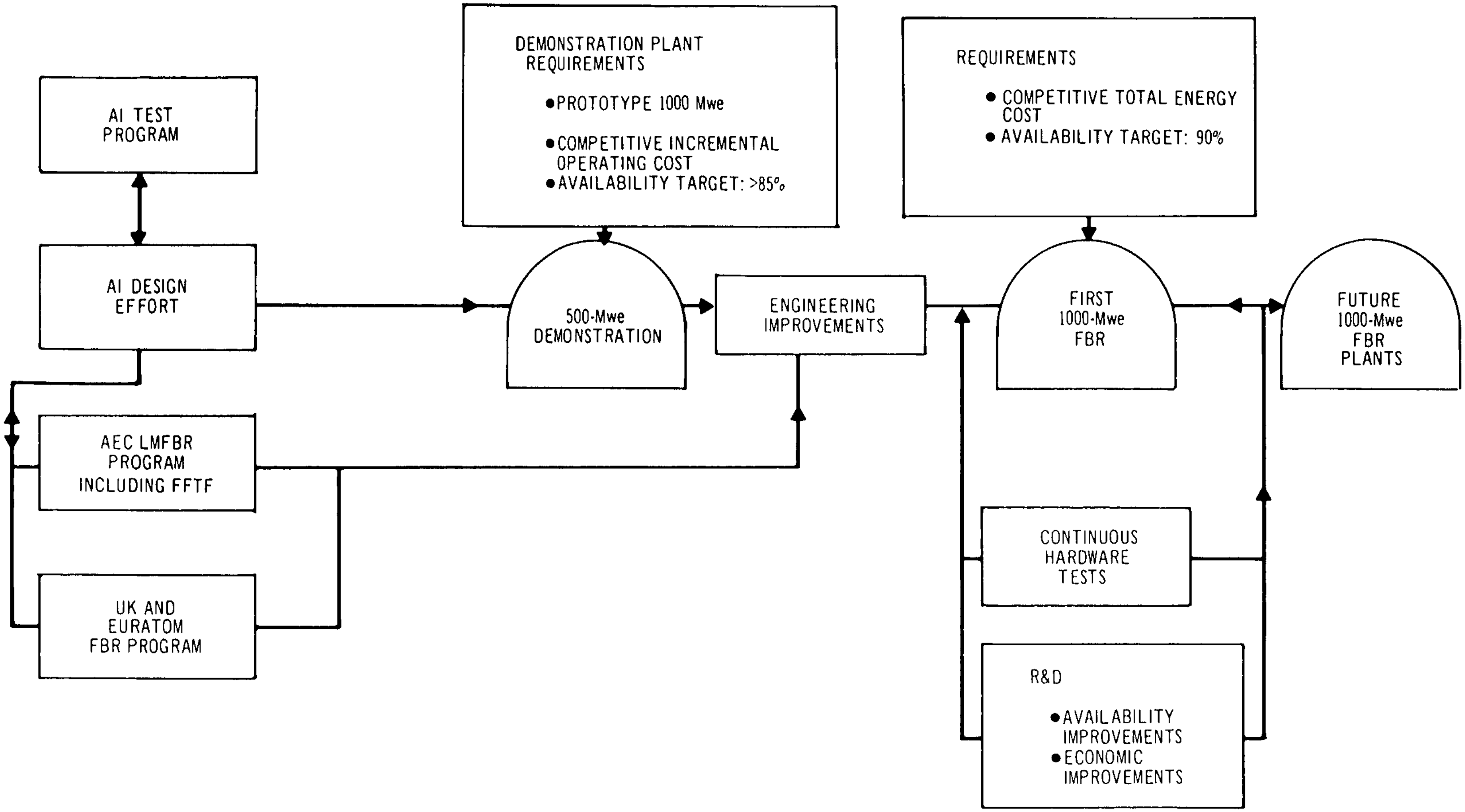

FIGURE 1. 1000-Mwe FBR Program Logic

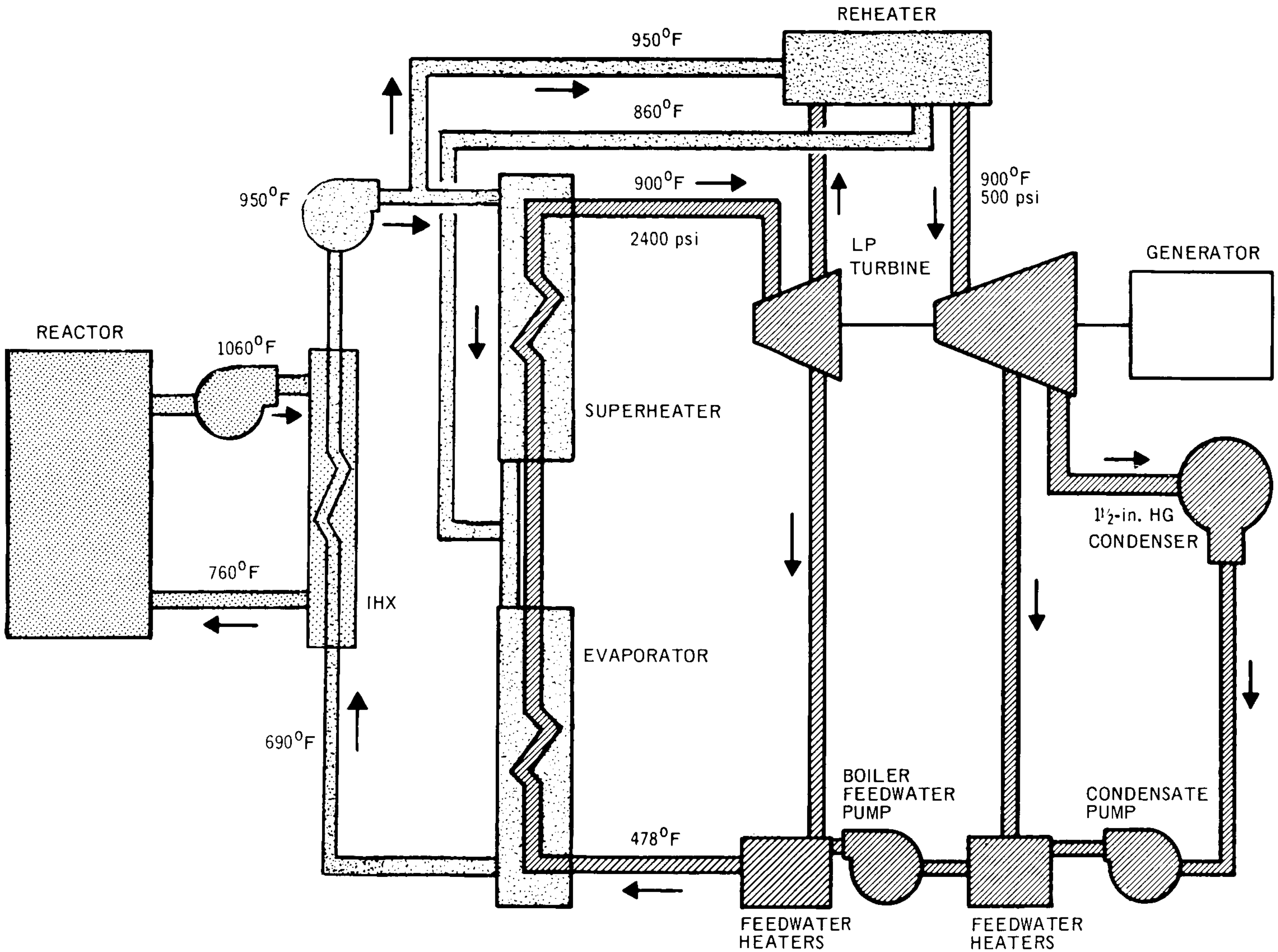

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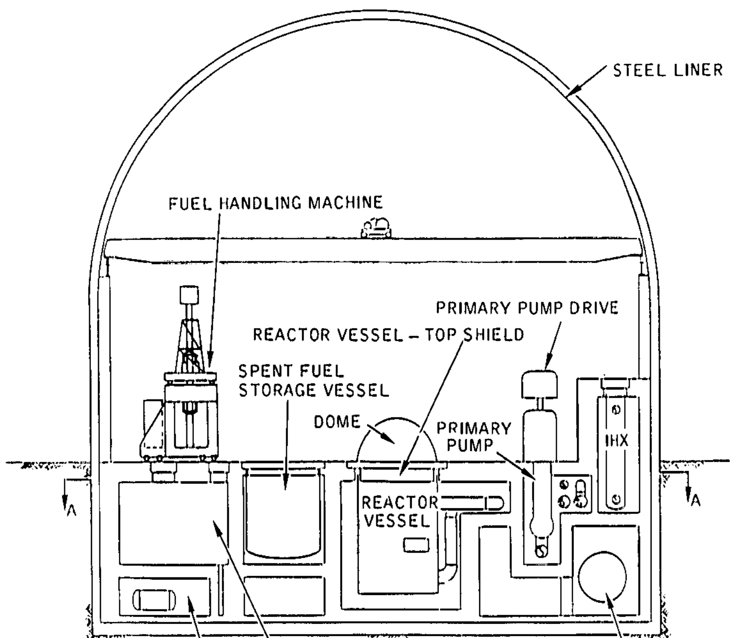
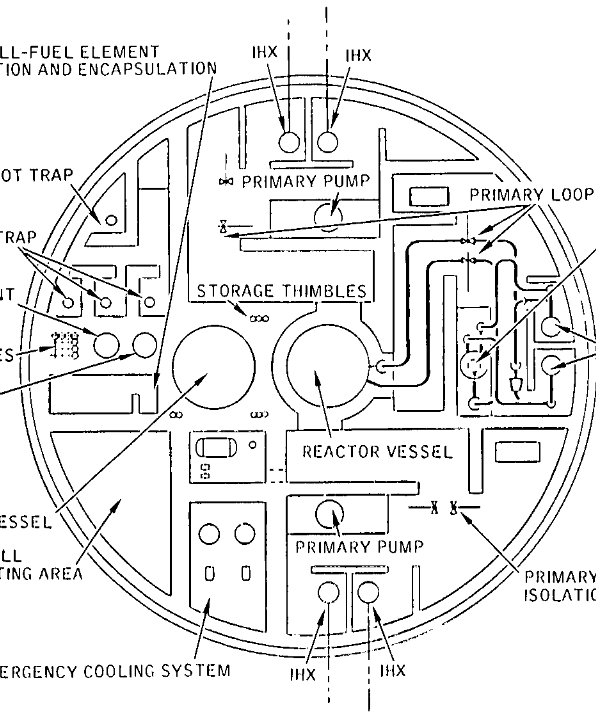
FIGURE 2. Basic Flow Diagram





PLAN A-A

FIGURE 3. Reactor Building

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TABLE 1

MAJOR REACTOR CHARACTERISTICS

Thermal-Hydraulic Performance	
Total reactor thermal power, Mwt (nominal)	2500
Peak nominal linear rod power, kw/ft (100% power)	15
Overall power peaking factor	1.58
Reactor mix-mean temperature rise, °F	300
Reactor outlet coolant temperature, °F	1060
Maximum nominal cladding inner surface temperature, °F	1130
Hot spot cladding inner surface temperature, °F	1200
Core pressure drop, psi	100
Mid-Cycle Equilibrium Nuclear Characteristics	
Number of fuel elements (Pu, wt %)	
Inner zone	118 (11.6)
Outer zone	138 (16.0)
Number of radial blanket elements	
Inner zone	60
Outer zone	66
Core height, in.	50
Axial blanket length (upper and lower), in.	12
Radial blanket height (inner/outer), in.	62/50
Number of control rods	
Shim-safety	9
Safety	6
Fissile masses, kg	
Core	1760
Radial blanket	330
Axial blanket	100
Total	2190
Breeding ratio	1.3
Initial loading, kg (Pu + U)	
Core	13,300
Radial blanket	15,200
Axial blanket	7400
Doppler constant (T dk/dt)	-0.0094
Prompt (Doppler) power coefficient,* $10^5 \Delta k/k/\%$ power	-5.10
Delayed (sodium) power coefficient*	+0.75
Overall power coefficient*	-4.35

*At full power

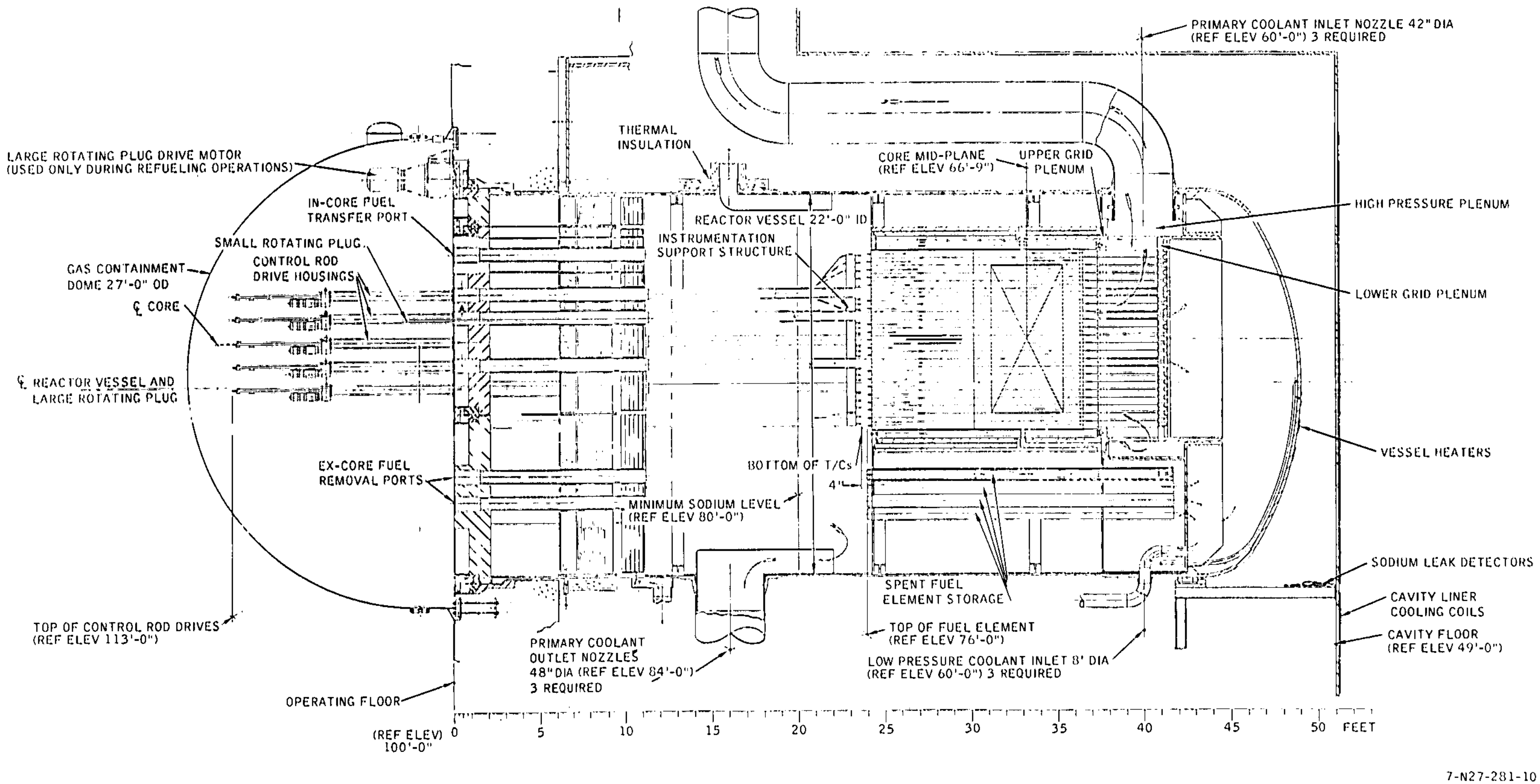

FIGURE 4. Reactor Elevation

Sodium Heat Transfer System

Three one-third capacity heat transfer circuits are provided. The characteristics are given in Table 2. The IHXs are elevated, such 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.

The sodium pumps are variable-speed, free-surface, single-stage centrifugal units. The main loops also 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 IHXs are counterflow, shell-and-tube heat exchangers, with the primary sodium on the tube side.

The evaporator, superheater, and reheater modules are all of similar design, varying only in the number of tubes per module and the tube size. These are straight shell-and-tube heat exchangers, with the steam on the tube side. Relief piping is provided to vent the reaction products in the event of a large water-to-sodium leak.

Turbine-Generator Plant

The turbine-generator is a 3600/1800 rpm, cross-compound quadruple flow, 43-in. Last Stage Blading (LSB) unit. The rated capability is 1066 Mw at 0% makeup and 1.5 in. Hg abs. Steam conditions are 2400 psig/900°F at the throttle with 900°F reheat.

SAFETY REQUIREMENTS

The AEC General Design Criteria have formed the basis for the main safety guidelines and safety design features incorporated in the Base Plant. A summary of those guidelines most influential in the design, together with brief statements of the manner of their implementation, is described below. 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 (Figure 5).

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% O_2 maximum) during reactor operation.

TABLE 2

SODIUM HEAT TRANSFER SYSTEM CHARACTERISTICS

Primary Heat Transfer Loops, number	3
Pumps per loop	1
Pump flow, gpm	76,000
Pump head, ft	355
IHXs Per Loop	2
Secondary Heat Transfer Loops, number	3
Pumps per loop	1
Pump flow, gpm	86,000
Pump head, ft	250

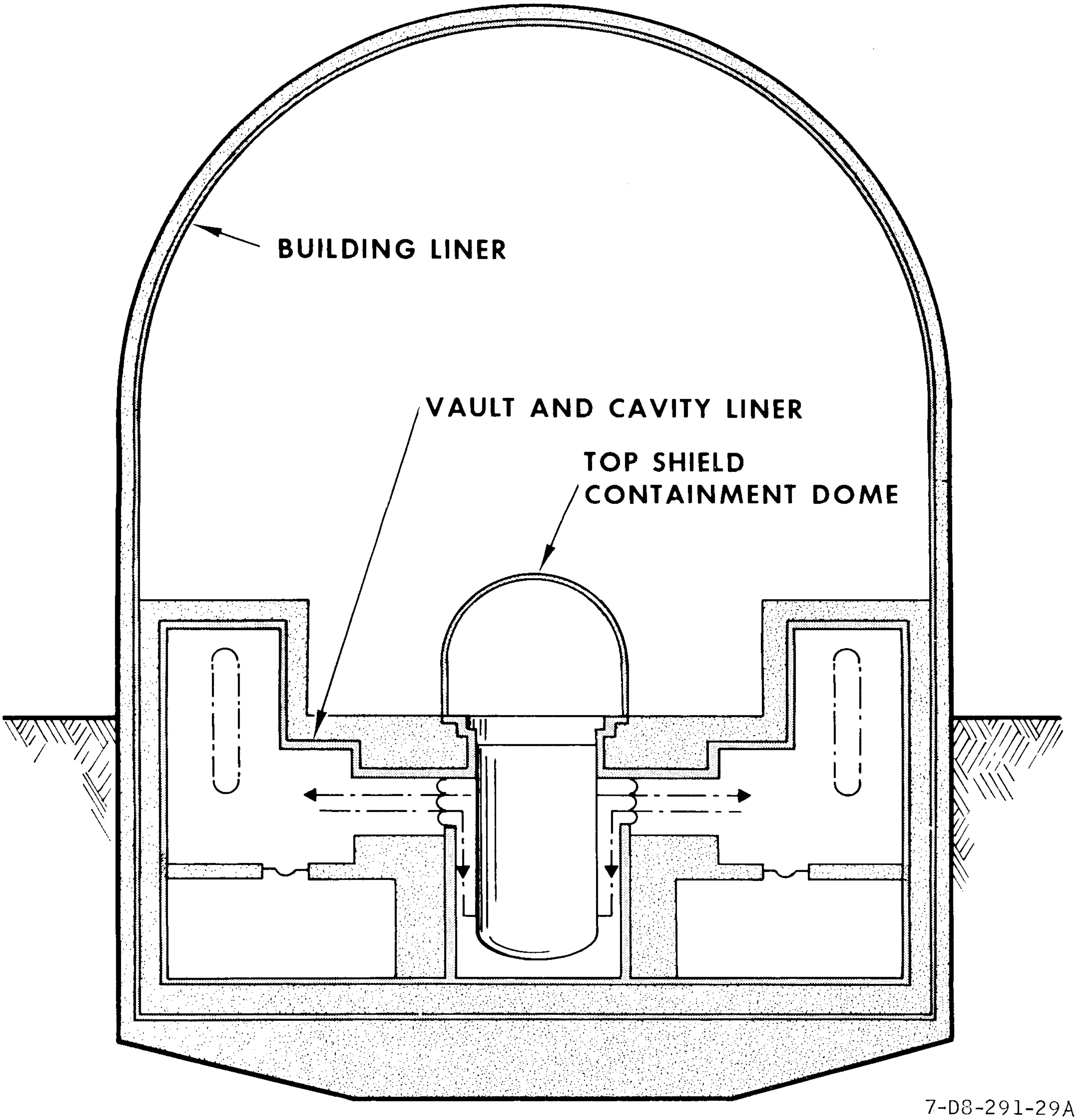

FIGURE 5. Reactor Containment

Engineered safeguards are provided to maintain the effectiveness and integrity of the containment boundaries, during and following the design base 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 which 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.

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.

Reactor Coolant Loss Protection – A reactor containment tank and an elevated loop assure that, in the event of a pipe rupture or reactor vessel failure, 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; and 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 result 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 two exit thermocouples to detect abnormal temperatures and to permit control against damage propagation. The element housing thickness is adequate to prevent failure propagation.

POOL vs LOOP

An early major decision with which a sodium-cooled fast reactor designer is faced is the choice of primary system configuration [i.e., should the reactor, pumps, and intermediate heat exchangers all be located within a single vessel ("pool" design), or should they be separated by a piping system ("loop" design)]. AI's conclusions and underlying considerations in this regard have already been reported at last year's FBR Topical Meeting. The "loop" is the favored concept, for reasons briefly outlined below.

Both concepts have their particular advantages. The loop concept permits the maximum accessibility for maintenance, provides a configuration which is straightforward to analyze for hydraulics, stress, and thermal behavior, and permits equipment improvement and modification with minimum effects on the remainder of the system. This equipment separation also eases development and construction schedule constraints.

The pool concept provides a simple tank design, with few if any penetrations, a high degree of assurance of maintaining core cooling at all times, a less complicated, more compact, less costly arrangement of primary system components, and a smaller reactor building.

A comparison of the capital costs of these two concepts was made for a 1000-Mwe, sodium-cooled fast breeder reactor power plant, as indicated in Table 3. It was found, as expected, that the pool concept is likely to be about $\$1.1 \times 10^6$ lower in first cost than the loop concept. This is primarily due to substantial savings in reactor building and primary piping system costs. These savings overshadow the higher costs associated with the large vessel and its internal shielding. The pool concept requires a much larger sodium inventory than the loop concept, however, and this reduces its cost advantage to about $\$0.7 \times 10^6$.

If it were possible to design the pool type IHX with as few tubes and as long as that for the loop concept, the two plants would be expected to have approximately equal availabilities. However, a method for achieving this goal is not readily apparent. Therefore, based on the best failure rate data available at this time, it is estimated that the pool concept would have a plant availability about 1.8% lower than that of the loop concept.

TABLE 3

PLANT CAPITAL COST COMPARISON
$1000s

Item	Loop	Pool
Reactor Building and Sub-Structure	Base	-$2,000
Reactor Vessel	Base	+600
Neutron Shield	Base	+985
Cavity Liner/Insulation	Base	+70
Primary Piping System	Base	-2,450
Pumps	Base	-570
IHXs	Base	+2,600
Preheating	Base	-300
Subtotals	Base	-$1,065
Sodium Inventory	Base	+400
Total	Base	-665

A qualitative review of the safety aspects of the pool and loop designs discloses no reason why either concept should not be capable of meeting the AEC safety criteria. A primary advantage of the pool concept is its more compact and simpler primary coolant boundary, which should make it easier to meet coolant system integrity criteria. On the other hand, the loop concept, through its larger containment building and large inert atmosphere vaults, can easier meet the ultimate containment and radiological release criteria.

Both concepts carry with them technical risks which should be reduced by further analysis and/or development testing. The risks associated with the loop concept are in connection with large valves, large diameter piping integrity and support, gas entrainment, and fission product migration and deposition. The pool concept risks are in connection with pump and IHX size constraints, possible thermal distortion of components, IHX reliability, core and primary loop instrumentation, prediction of thermal/hydraulic performance and structural behavior, and vessel fabrication.

On an overall basis, the loop concept continues to appear more attractive to AI (although initially slightly more costly), because of its greater maintainability and reliability. The differential overall energy cost is estimated at about 0.02 mill/kwh in favor of the loop concept. An additional advantage of the loop concept is its flexibility to take advantage of component improvements (e.g., elimination of the secondary system, a less costly pump or heat exchanger). In view of above considerations and AI's past experience, the loop concept has been retained for the 1000-Mwe plant.

NUMBER OF HEAT TRANSFER CIRCUITS

In any plant design, one of the major decisions that has to be made is the number of heat transfer circuits which will be used. Into the decision, one has to factor safety, economics, availability, and the state of the art at the time of construction. The LMFBR, as any other plant, required a decision as to the number of circuits; therefore, a comparison study of two, three, and four heat transfer circuits was conducted.

In the case of the four-circuit and three-circuit systems, a failure in one circuit (e.g., a pump drive failure) does not prevent continued reactor operation at reduced load on the remaining circuits. If access to the pipe vaults is required, the affected circuit can be shut down, and the primary loop isolated from the reactor by closing the block valves. After decay of Na-24, which requires about 10 days, the reactor can be shut down and the vault entered for inspection and repairs.

In a two-circuit plant, both circuits must be in operation. If one circuit must be shut down for repairs, the entire plant must be shut down. This approach is based on the philosophy that there must always be one heat transfer circuit available for decay heat removal.

The two-, three-, and four-circuit heat transfer systems were compared by estimating heat transfer system cost and plant availability differentials associated with them. Cost favors the two-circuit concept, because overall heat transfer system costs decrease (although not strongly) with the number of circuits, and also because a decrease in reactor building size is expected to accompany a reduction in the number of heat transfer circuits. A cost summary is shown in Table 4.

Plant energy unavailability, due to forced outages of the heat transfer system equipment, was also estimated. As would be expected, the two-circuit plant suffers a greater unavailability than either the three- or the

TABLE 4

HEAT TRANSFER SYSTEM DIFFERENTIAL COSTS

Number of Circuits	Heat Transfer System ΔCost \$	Building ΔCost \$	Total ΔCost \$
4	$+1.9 \times 10^6$	$+0.4 \times 10^6$	$+2.3 \times 10^6$
3	Base	Base	Base
2	-2.0×10^6	-0.8×10^6	-2.8×10^6

four-circuit system, in spite of the fewer number of critical components, because of the requirement that the entire plant be shut down if one circuit shuts down. A summary of estimated heat transfer system unavailabilities is shown in Table 5. Assuming that the cost of unavailability is 2 mills/kwh, a cost comparison can be made as shown in Table 6.

On this basis, it would appear that a two-circuit plant is slightly more economical than a three-circuit one. However, the three-circuit plant has better availability than either a two- or four-circuit design. In addition, the risks associated with extrapolation of component size to the two-circuit plant are much greater. The risks relative to guillotine pipe rupture in the two-circuit plant also may be greater. Therefore, based on these considerations, the three-circuit design was chosen, since it offered the best compromise of all factors; even though, on an economic basis, it was not the best.

CORE GEOMETRY

The area which perhaps has received the greatest attention by U.S. designers of sodium-cooled FBRs is that of the influence of the positive sodium void worth on core geometry selection. Concern for potential safety implications of large void worth has led to numerous studies of "compromised" core geometries (pancake, modular, annular). The results of our preliminary studies in this area were reported in detail at last year's ANS Fast Breeder Reactor Topical Meeting in San Francisco.[1] At that time, AI had selected a modular core for the demonstration plant.

These preliminary studies revealed no advantage for such compromised geometries, other than possibly that attached to a low void worth. On the contrary, disadvantages in both safety and economics have consistently been found in high-leakage cores. The most important disadvantage of the high-leakage geometry core is a substantial reduction in Doppler effect, which results from a requirement to increase fuel enrichment and thereby harden the neutron spectrum. A second disadvantage of spoiling the geometry is the associated reduction in in-core breeding ratio; this results in a requirement for more excess reactivity in the newly refueled core. There are accident situations, such as the plugging of one or a few fuel elements or passage of gas bubbles through the core, in which a positive sodium effect can be beneficial in providing a sensitive signal source for informing the operator of the malfunction.

The results of the continuing studies at AI on the significance of a positive void worth in large oxide-fueled FBRs continue to confirm these conclusions. In summary, a positive void worth is of no substantial disadvantage, and can have the advantage of forewarning the operator of the onset of fuel damage. It may also reduce the destructive potential of postulated severe accidents. The regular geometry core has a larger negative Doppler coefficient, and requires less excess reactivity for operation. We therefore have selected a regular cylindrical geometry core for the Task I base design, with height and diameter limited only by considerations of performance and economics.

REFERENCE

1. H. Dieckamp et al, Atomics International, Atomics International Demonstration Fast Breeder Reactor, in ANS Northern California Section National Topical Meeting, San Francisco, April 1967

TABLE 5

HEAT TRANSFER SYSTEM UNAVAILABILITY

Number of Circuits	Unavailability	ΔUnavailability
4	0.062	+0.003
3	0.059	Base
2	0.069	+0.010

TABLE 6

ECONOMIC COMPARISON MATRIX
mills/kwh

Parameter	Number of Circuits		
	2	3	4
Capital Cost	-0.077	Base	+0.063
Unavailability	+0.020	Base	+0.006
Totals	-0.057	Base	+0.069

CORE HEIGHT - ΔT - SYSTEM STUDIES

The combined reactor - heat transfer - energy conversion system has been analyzed to determine the effects of core height, core ΔT, and steam cycle on system costs. The purpose of this study was to confirm that the design selected for Task I was near-optimum. A matrix of nine core configurations and three steam cycles was studied. Core heights of about 3 ft, 3-1/2 ft, and 4 ft, with core ΔTs of 260, 280, and 300°F, were considered. The steam cycles considered were 2400 psi, 850°F/850°F, 900°F/900°F, and 950°F/950°F. The study ground rules are summarized in Table 7. The differential costs, as a function of the system variables, were determined by an AI-developed system survey computer code.

Shorter cores and higher ΔTs result in lower sodium volume fractions. This reduces fissile inventory and improves breeding ratio slightly. A reduction of core height, however, also results in higher fabrication costs and increased fuel handling times.

Figure 6 shows the fuel cycle cost as a function of ΔT for the different heights. The fuel cycle costs shown are based on an assumption of a constant peak fuel burnup. If cladding ductility is assumed to limit fuel burnup, the resulting fuel cycle costs will be nearly flat with core height. If constant average burnup is assumed (fixed refueling schedule), there is a slight penalty for shorter cores. Thus, the conclusion drawn from this analysis is that, within the framework of our present state of knowledge of fuel burnup limitations, the fuel cycle costs are insensitive to these core geometry changes.

These fuel cycle costs were then used as input to the system survey code, which combines the fuel cycle costs with the costs of the reactor, primary and secondary loop, heat exchanger, turbine-generator plant, reactor building, and plant unavailability to determine differential power costs for the various combinations selected.

The parameter of principal interest in this study was core height. Information was also generated on primary and secondary ΔTs and steam cycles. Figure 7 shows the differential power costs as a function of core height for the 300°F reactor ΔT and 2400 psi/900°F/900°F base case. These trends are generally typical of all the cases studied. The capital cost differential with core height is negligible, as decreases in reactor height are largely offset by increases in diameter. The main item affecting the selection of core height is the fuel cycle cost. Figure 7 shows the effect of the previously mentioned fuel burnup models on power costs. It is our opinion that the cladding-ductility-limited burnup model is probably the most realistic. Considering this model, there is a slight incentive toward cores of about 38 to 40 in., instead of the reference 50 in., but it is clearly within the limits of uncertainty (<0.01 mills/kwh). The costs are based on estimates of future fabrication capability. Consideration of the costs for the demonstration plant, which is shown as a dashed line, and other possible near-term plants favor taller cores. Because of the desired prototype relationship between the 1000-Mwe and the demonstration plants, the 50-in. core was retained for the present time for the 1000-Mwe plant design, as this provides the most economical solution for the early LMFBRs.

Figure 8 shows differential power costs for the specific steam cycles selected. The curve, of course, is not continuous, but is shown to more clearly indicate that the 2400 psi/900°F/900°F cycle gives minimum costs for the base reactor conditions.

TABLE 7

STUDY GROUND RULES

Basic System	
	1060°F Reactor Sodium Outlet
	950°F Steam Generator Sodium Inlet
	2400 psig Reheat Steam Conditions
Core	
	~15 kwt/ft Maximum Linear Heat Rate
	~30 fps Maximum Coolant Velocity
	6 month Refueling Cycle
	~75,000 Mwd/MT Average Fuel Burnup
	3 year Inner Blanket Residence Time
	6 year Outer Blanket Residence Time
	12-in. Axial Blankets
Economic	
	13% Annual Capitalization Rate
	2 mill/kwh Unavailability Cost
	0.80 Plant Factor

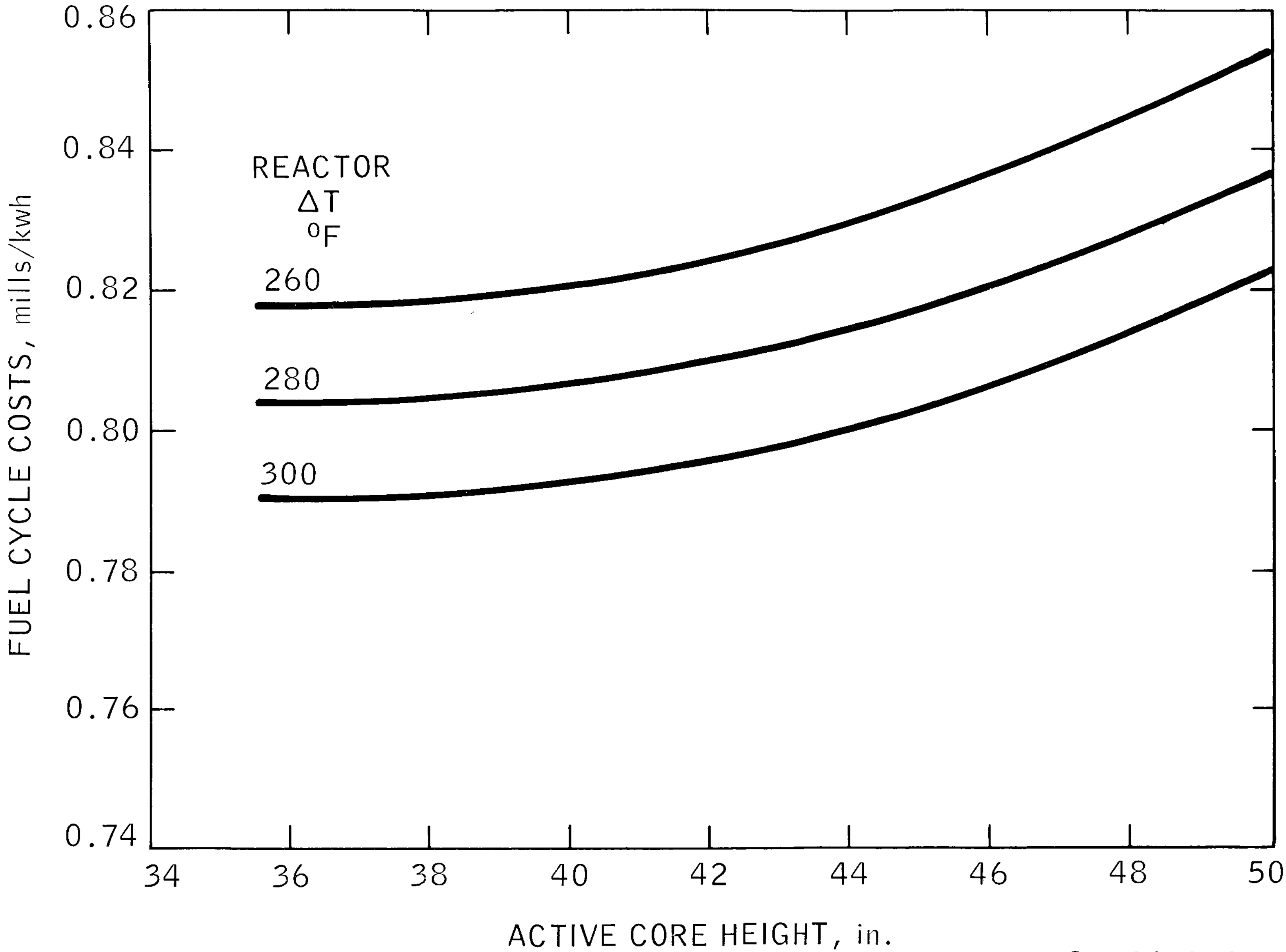

FIGURE 6. Total Mid-Equilibrium Fuel Cycle Costs

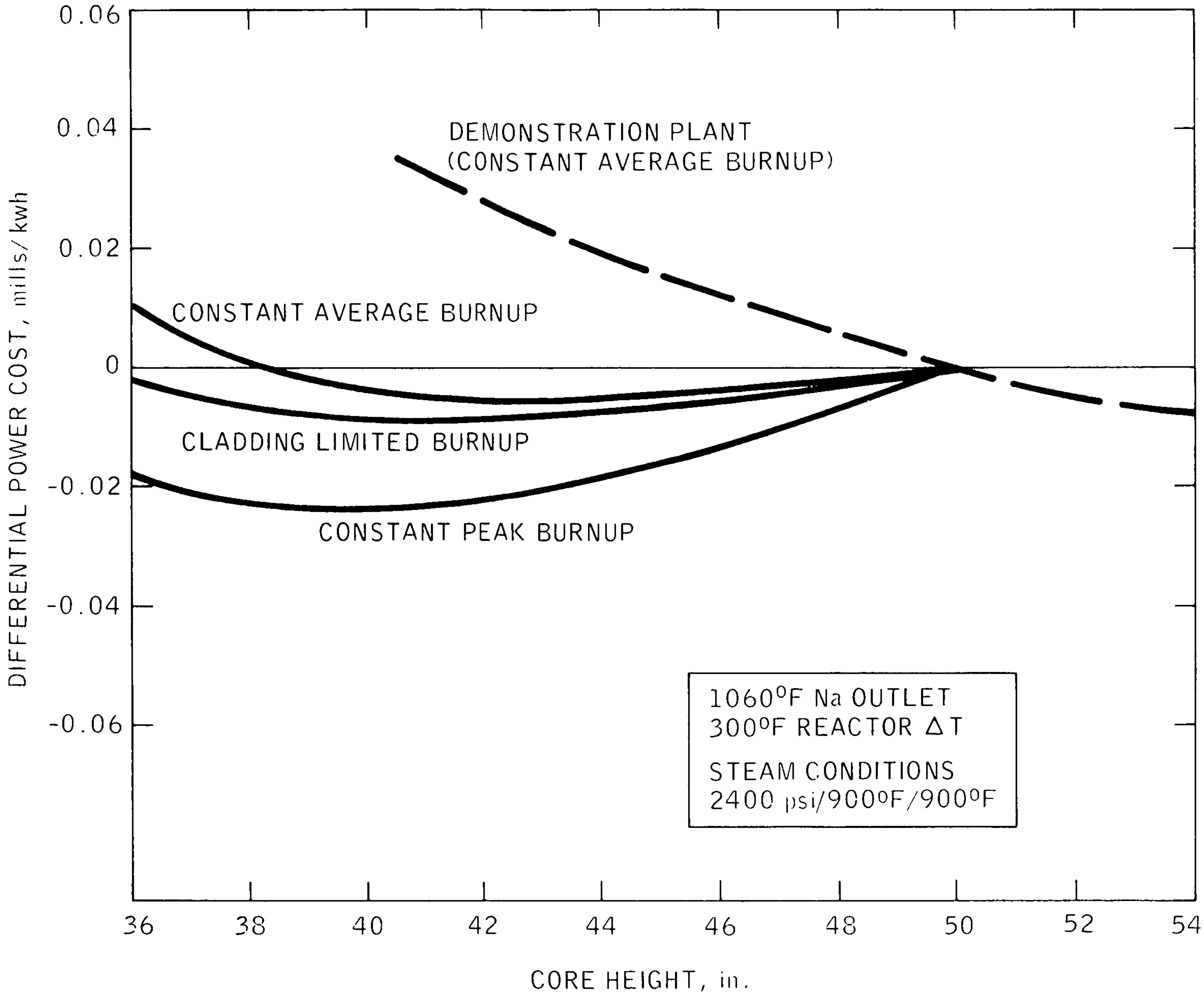

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FIGURE 7. Differential Power Costs as a Function of Core Height

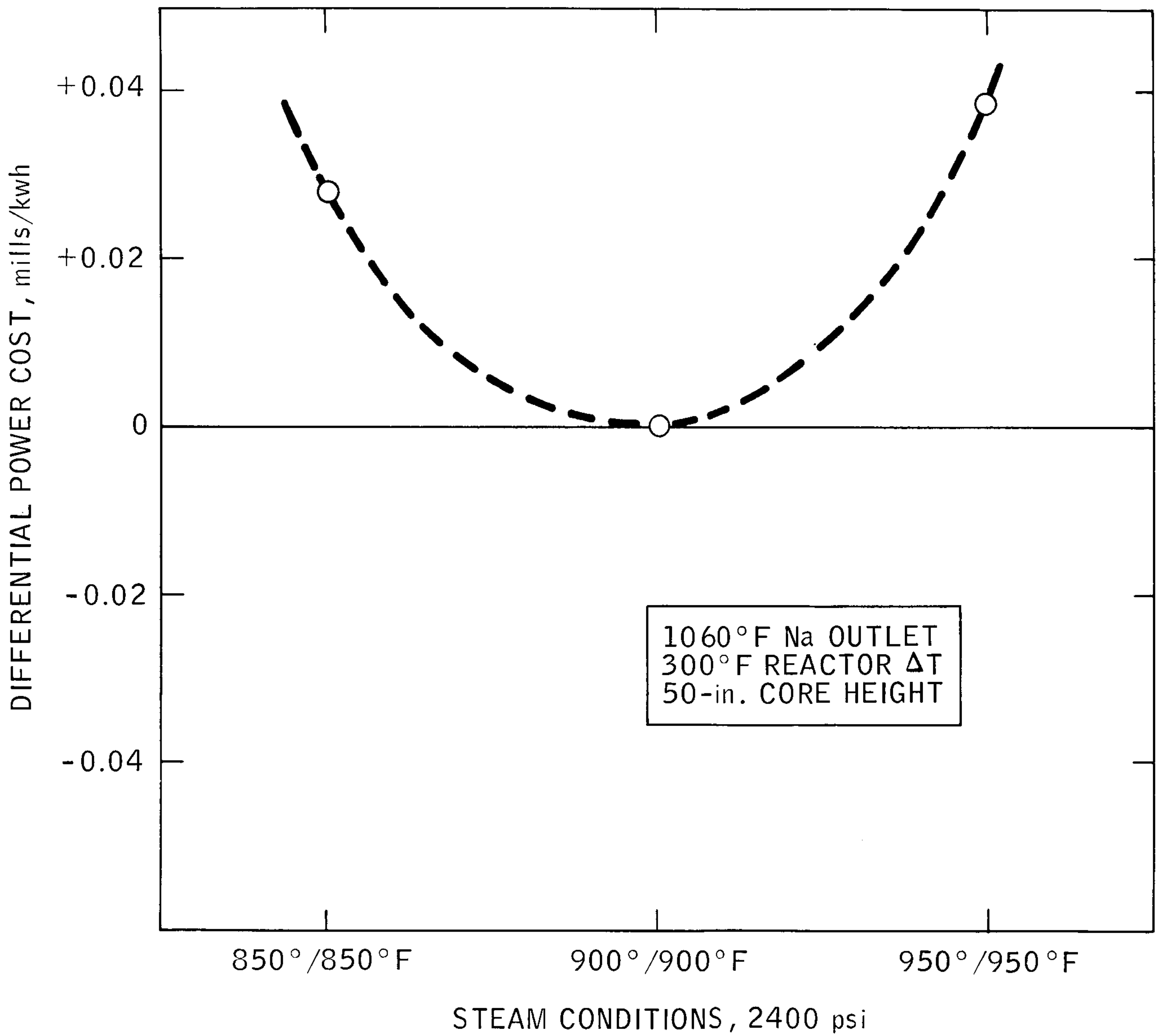

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FIGURE 8. Differential Power Costs for Various Steam Conditions for Reference Reactor

Additional studies of the effects of principal parameters on the plant design and on economics will be carried out later in the program. These future studies may indicate changes to be made for the final 1000-Mwe reference design which may also influence the demonstration plant.

FUEL HANDLING

The time required for refueling operations in nuclear power stations is one of the major contributers to plant unavailability. Several fuel handling approaches have been examined to select the most attractive system. These systems were discussed in Reference 1. Early studies narrowed the field down to the following four, which were described in Reference 1 (see Figure 9):

1) Double rotating plug with in-vessel storage

2) Fixed top shield with three rotating and articulatory arm handling machines and in-vessel storage

3) Single rotating plug with a single nonrotating articulating arm handling machine and in-vessel storage

4) Removable plug in a hot cell with in-vessel storage.

The factors involved in comparing these concepts were plant availability, simplicity, maintainability, safety, development requirements, and cost.

With regard to plant availability, the double rotating plug concept, with all mechanisms external to the reactor vessel, was favored. The articulating arm approach has a lower rating, because of the need to raise the control rod actuators to the underside of the top shield before moving elements. Also, the articulating arm is a complex mechanism, with which little operating experience has been obtained. Similarly, the hot cell approach is penalized by radioactive sodium contamination of the cell and the handling equipment. In addition, the anticipated advantages of visual access for handling are achieved at the expense of slower operations, characteristic of hot cell manipulations. These considerations favor the double rotating plug concept, from the standpoint of maintenance operations. Safety considerations showed no distinct advantage for any concept.

The double rotating plug concept, with a separate machine for in-core fuel transfer and a separate machine for ex-core operations, involves the design features employed in the SRE and Hallam fuel handling machines, and the ex-core machine designed and fabricated by AI for Fermi. Based on this past experience, new development will be minimal. The hot cell concept also has minimum development requirements, because of simplicity of handling operations. The articulating arm fuel handling machines will require significant development efforts.

These factors are reflected in the economic evaluation summarized in Table 8. Although the fixed plug and single rotating plug concepts have the lowest capital costs, these savings are more than offset by the estimated availability penalty and additional development costs. The economic analysis indicates the double rotating plug is the best selection; however, the hot cell concept is also a very attractive approach. The economic difference between the two is probably within the error band of our estimates. The double rotating plug was selected over the hot cell on the weight of past AI development and operating experience, and the basis of more easily maintainable equipment.

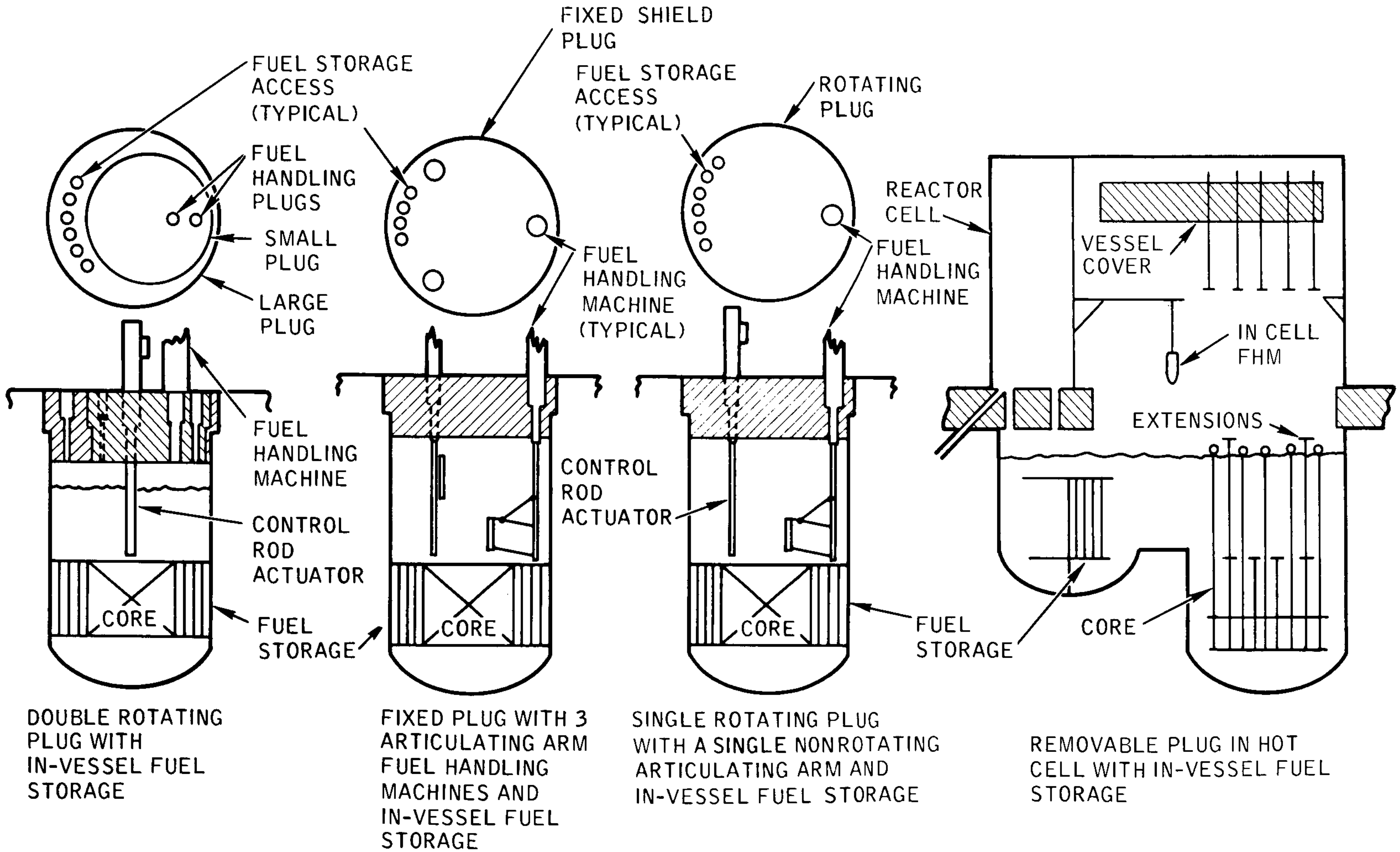

FIGURE 9. Fuel Handling Concepts

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TABLE 8

LMFBR FUEL HANDLING ECONOMIC ANALYSIS
DIFFERENTIAL COSTS
mills/kwh

	Double Rotating Plug	Fixed Plug, Articulating Arms	Single Rotating Plug, Articulating Arm	Hot Cell
Capital Cost	Base	-0.027	-0.019	+0.013
Availability	Base	+0.060	+0.073	+0.005
Total	Base	+0.033	+0.054	+0.018

FUEL SYSTEM

The principal requirements that the fuel system must meet are:

1) Availability of a sufficient technological base by 1976 to 1977 to permit the design of a highly reliable core with satisfactory performance characteristics (i.e., hot spot cladding temperature of >1200°F, maximum nominal linear power rating of >15 kwt/ft, and average core fuel exposure of >75 Mwd/kg)

2) The ability to take maximum advantage of the projected LWR fuel cycle industry, so as to minimize fabrication and reprocessing costs during the early LMFBR introduction period; the target for combined core fabrication plus fuel recovery costs is ~$300/kg by 1985.

With respect to the availability of a sufficient technological base by 1976 to 1977, mixed oxides is the preferred fuel system. Considerable related operational and production experience is becoming available from LWR UO_2 use. Also, there will be a great deal of applicable information and experience from Pu-recycle in LWRs.

The first LMFBR ceramic fuel experience on a large-scale basis will be in FFTF, which will use mixed oxide through the 1970s. The AI demonstration plant will provide additional experience, starting about 1975. These two facilities, plus the planned AEC development program, will provide the production and technological bases necessary for the early commercial plant. In addition, all the prototypes currently planned and under construction by other countries will use mixed oxide. These programs will add significant knowledge regarding mixed oxide fuel.

The ESADA 1000-Mwe study showed that mixed oxide fueled LMFBRs can achieve satisfactory economic performance if the listed requirements are met. The fuel cycle costs for the base design 1000-Mwe LMFBR for various burnup levels are shown in Table 9. Based on estimates of fuel cycle costs for LWRs, achievement of a burnup of 75,000 Mwd/MT will give the LMFBR a fuel cost advantage of about 0.6 mill/kwh. This fuel cycle cost advantage will permit the LMFBR to incur a capital cost disadvantage of up to about $30/kwe above the LWR before reaching the breakeven point.

TABLE 9

FBR FUEL CYCLE COST vs CORE BURNUP
1986 to 1990 Economy

	mills/kwh		
	Core Burnup		
	50,000 Mwd/MT	75,000 Mwd/MT	100,000 Mwd/MT
Fabrication			
Core	0.40	0.28	0.24
Blanket	0.07	0.07	0.06
Fuel Recovery	0.25	0.18	0.16
Ex-Core Inventory	0.20	0.13	0.10
Net Pu Credit (After 2% Loss)	(0.32)	(0.29)	(0.26)
Fuel Expense	0.60	0.37	0.30
In-Core Inventory	0.34	0.35	0.35
Total Fuel Cycle Cost	0.94	0.72	0.65

DISCUSSION

C.B. Zitek (Commonwealth Edison Company) - Did I misunderstand? I heard that the 1000 megawatt reactor is going to be ready for construction in 1975.

R. Aronstein - Our time scale which we have selected for the ANL follow-on studies calls for the sale of the plant about 1975-76, which according to the ground rules which you laid down, 4-5 or 6 years operating time, there isn't that much time between completion of the demonstration plants and our schedule.

C.B. Zitek - When are the demonstration plants to start up?

R. Aronstein - The schedule for the demonstration plant start-up, I believe, is about 1975-76.

DISCUSSION - SESSION II

D.A. Minner (Babcock & Wilcox) - My question is directed to Mr. Beekman. Mr. Beekman, in the EEI reports is there any commentary, or can you tell us anything, about the potential for recycling plutonium in the thermal reactors vs saving it up for use in the early breeders?

M.C. Beekman - Yes, the EEI report does comment on this. Based on the growth expansion for plutonium production rates that I was speaking of this morning and the fact that commercial fast breeder reactors could come in between 1980 and 1985, it appears as though there will be large amounts, maybe 60, 80, or 100 tons of fissile plutonium that will be recycled in thermal reactors. The problem that comes here is when the plutonium is generated in a water reactor whether the operator decides to hold his plutonium until he can sell it for the fast reactor, presumably at a higher price, or whether he recycles it in his own reactor. Based on economics with some thought including the present worth effect of holding and hoping to get a higher price later on, it looks like the thermal reactor operator might hold this material for 3 to 5 years - 5 years as a maximum - before he actually loses money and that would come assuming that he gets a $3 per gram differential between his own value for recycled plutonium and what he might sell it for to a fast reactor operator.

K.A. Trickett (U.S. AEC) - After listening to these papers and realizing how difficult and tenuous some of these choices have been in design concepts, I wonder if someone from Argonne would like to tell us what criteria they are going to use when they evaluate these various studies. We started out with the assumption that when all this was done they were going to evaluate these. I'm not really sure what that means.

L.W. Fromm (ANL) - Actually I can't say too much about that at this point. As far as evaluation of these reports that we are receiving, we are primarily not attempting a comparison. The principle of our evaluation is based upon looking at each individual design in itself and evaluating it with regard to check calculations and this sort of thing to check that the conclusions reached and basis for the design are reasonable and as accurate as can be expected with the analytical methods that we use.

K.A. Trickett - The essential part of my question is that Argonne mentioned one of the reasons for these studies was to determine the technological problems that were a prerequisite to the designer having some of these choices we've heard. It's been my experience that the number of experimenters and things to be experimented upon exceed the available funds like the typical market. I really wondered to what degree Argonne's evaluation is going to be used to focus on which technology gets developed and which doesn't.

L.W. Fromm - I think what we are going to do along those lines is to look at each of the contractors recommendations and analysis with regard to the cost benefit that can result from performing a given piece of R&D in comparison with the cost of performing that research and development. Obviously, with five contractors there are certain research and development programs that will be called for by all five as compared with other areas in which research and development needs will be unique to the particular design. We will attempt in our evaluations to sort all of this out and to check the basis for the assertions made by the various contractors and indicate what we think are the R&D routes that will prove most profitable. I certainly agree with you that the potential for possible R&D to be performed always exceeds the funds available.

SESSION III

April 3, 1968

MATERIALS FOR FAST REACTORS

Chairman: James F. Schumar
Argonne National Laboratory

Local Co-ordinator: Layton J. Wittenberg
Monsanto Research Corporation

CERAMIC FUELS FOR FAST REACTORS

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Richland, Washington

ABSTRACT

Fast reactor fuels are subjected to considerably more severe operating conditions than are thermal reactor fuels.

Differences in service conditions and design parameters for fast reactor and thermal reactor fuels are described.

Currently, three uranium-plutonium ceramic compounds - oxides, carbides, and nitrides - all have high potential as fast reactor fuels. The relative merits of these compounds are assessed by comparing their thermal properties, swelling behavior, and compatibility characteristics. The design factors affecting performance of fast reactor ceramic fuels are identified.

INTRODUCTION

The economic success of fast breeder reactors depends upon developing reactor fuels capable of operating to long exposures at high specific powers in high fast neutron fluxes. Currently, ceramic fuels are most likely to meet these conditions. Of the ceramic fuels, oxides, carbides, and nitrides have been developed to the point where a preliminary assessment of their value as fast reactor fuels can be made by comparing key material characteristics. In this presentation some of the more important properties of these ceramic fuel materials are broadly compared. In addition, key design factors for ceramic fuels are identified, and the performance potential of each type of fuel is assessed.

CRITERIA FOR FAST REACTOR FUEL

The operating conditions imposed on fast reactor fuels require, if not new technologies, considerable advances in existing technologies. These conditions for the most part are beyond the range which permit prudent extrapolation from existing experience. A few of the service conditions for fast reactor fuels are listed in Table 1.

Table 1
Typical Service Conditions
For Fast and Thermal Reactor Oxide Fuels

	Thermal Reactors	Fast Reactors
Flux, Total, nv	10^{14}	$0.3 - 1.0 \times 10^{16}$
Fraction with $E > 0.1$ MeV	0.3 - 0.4	0.6 - 0.8
Burnup, MWd/ton	10,000-20,000	100,000
Cladding Dose, n/cm^2	$10^{20} - 10^{21}$	$10^{23} - 10^{24}$
Specific Power, kW/Kg (U-Pu)	25-30	200-250
Coolant Outlet Temp., °F	500-600	800-1200

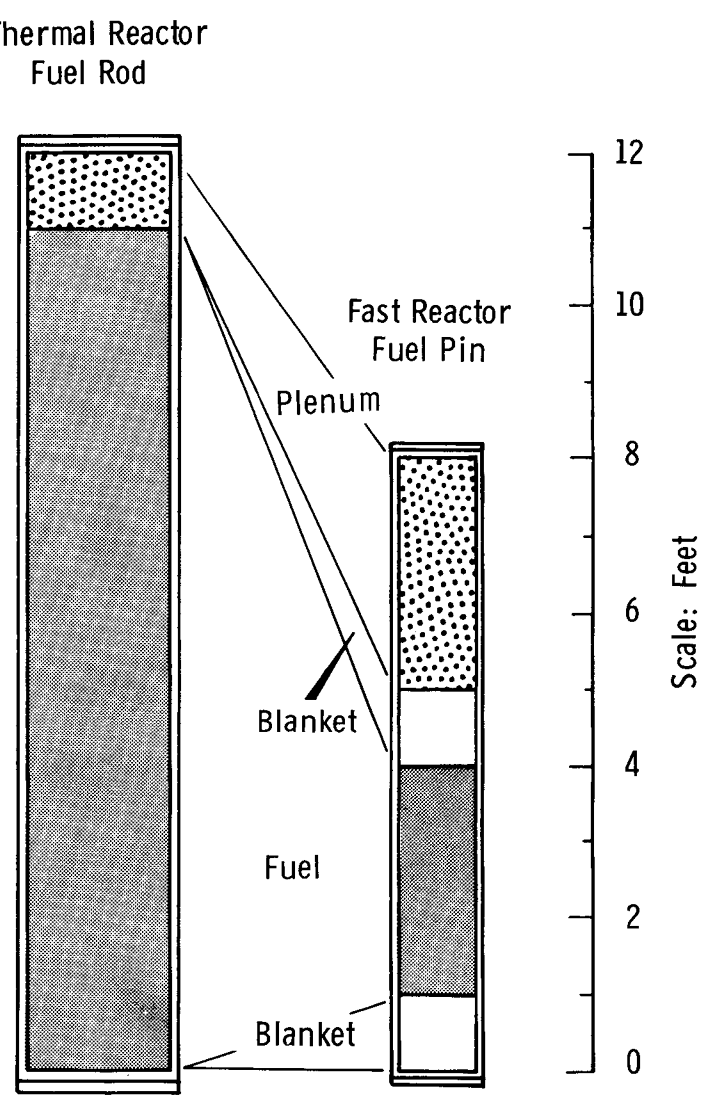
As seen in this table, the flux levels and neutron fluences to which the fuel and cladding materials are exposed in a fast reactor may be several orders of magnitude greater than in the thermal reactor. Fuel burnup may be between 5 and 10 times greater. Specific power in a fast reactor may be 7 to 10 times greater than that encountered in thermal reactors. The coolant temperature in a sodium cooled fast reactor is about twice as high as in a water cooled thermal reactor.

Figure 1 contrasts the physical differences in thermal and fast reactor fuels. The schematic representation of the fuel rod and the fuel pin are shown to a relative scale so that the components of each are in proportion. The 1/2 in. diameter thermal fuel rod is ~12 ft long while the 1/4 in. diameter fast reactor fuel pin is ~8 ft long. The smaller diameter fuel pin is necessary to allow for the higher specific power at which fast reactor fuels operate. The fuel column in a thermal reactor fuel occupies about 90% of the rod length. In a fast reactor pin, the fuel column length is only about half the pin length. The fast reactor fuel pin contains an extra component, a blanket material located above and below the fuel column to enhance breeding gain. Because of the higher burnup and greater fission gas concentration obtained in a fast reactor fuel, the ratio of plenum length to fuel column length in a fast reactor fuel pin is about an order of magnitude greater than in a thermal reactor fuel rod.

In addition to these differences in service conditions and design features, there are very significant differences in the distribution of power and temperature in fast and thermal reactor fuels. In a thermal reactor, the flux is depressed or attenuated by the fuel material, and consequently, a larger number of fissions occur in the peripheral region of the fuel rod than in the central region. This flux depression affects the power profile in the fuel, as shown in Figure 2. The specific power at the surface of the fuel is about 50% greater than at the center of the fuel. In a fast reactor, practically no flux depression occurs, and the specific power is uniform across the radius, thus giving an essentially flat power profile.

As shown in Figure 3, the maximum fuel temperature and the temperature profile in a thermal reactor fuel rod and a fast reactor fuel pin are not the same because of differences in power profiles and in surface temperatures. At a linear heat rating of 16 kW/ft the maximum fuel temperature is about 800 °C higher in a fast reactor fuel pin than in a thermal reactor fuel rod. For the example shown here, about half of this difference is due to the lower surface temperature of thermal reactor fuel and half to the flux depression.

If fuel temperature is plotted as a function of fuel radius rather than fraction of the radius, the gradient in the smaller diameter fast reactor pin as compared to the thermal reactor fuel rod would be even greater than that shown in Figure 3.



Design Parameters		
Length, ft	~12	6-8
Diameter, in.	0.5	0.25-0.30
Fuel Col. Lgth. ft	~11	3-4
Blanket Lgth. in.		~10 Top & Bottom
Ratio-Plenum to Fuel Column	0.08	1-1.5
Cladding Material	Zircaloy	Stainless Steel

FIGURE 1: THERMAL AND FAST REACTOR FUEL DESIGN PARAMETERS

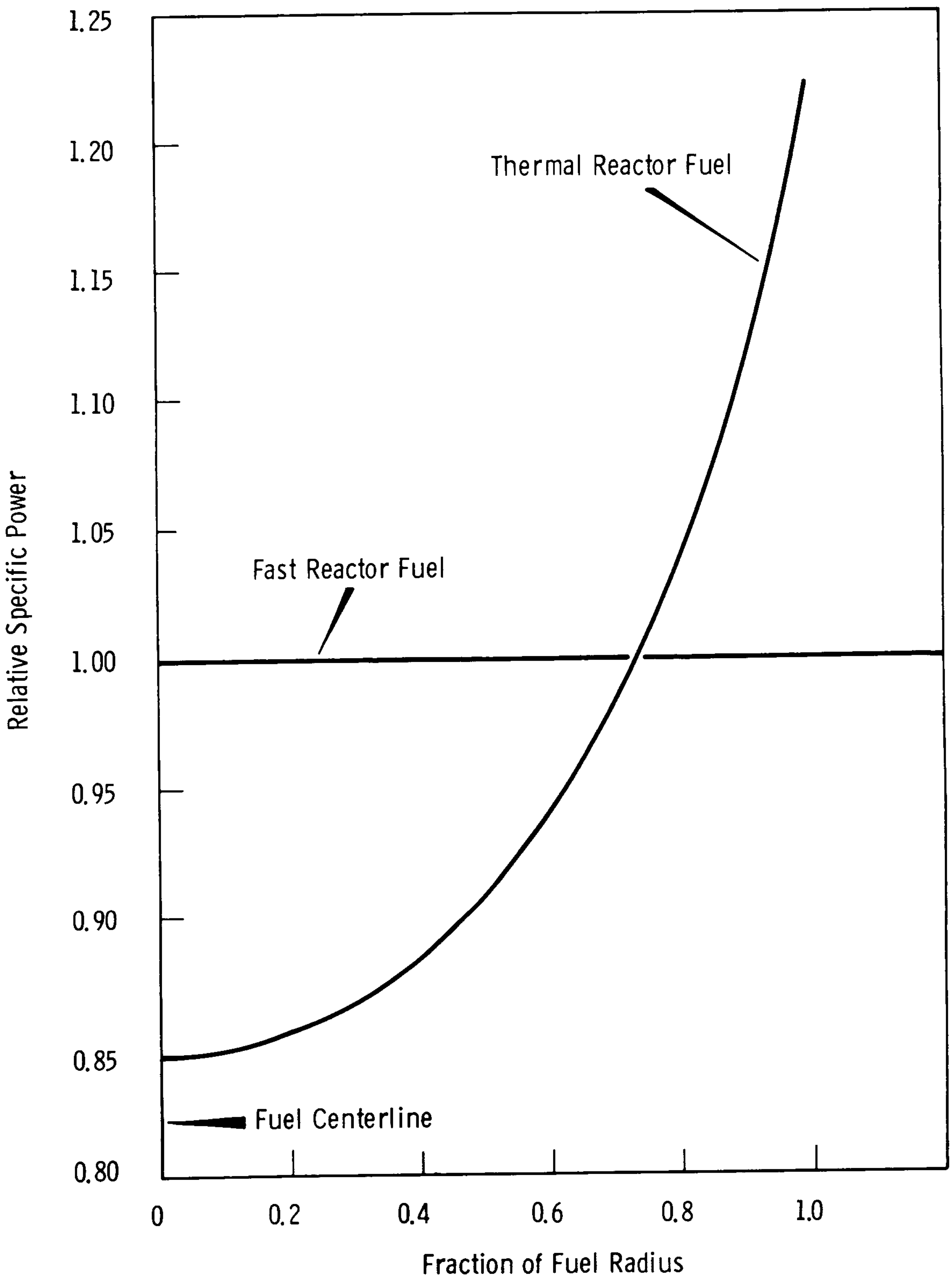


FIGURE 2: POWER PROFILES FOR FAST AND THERMAL REACTOR FUELS

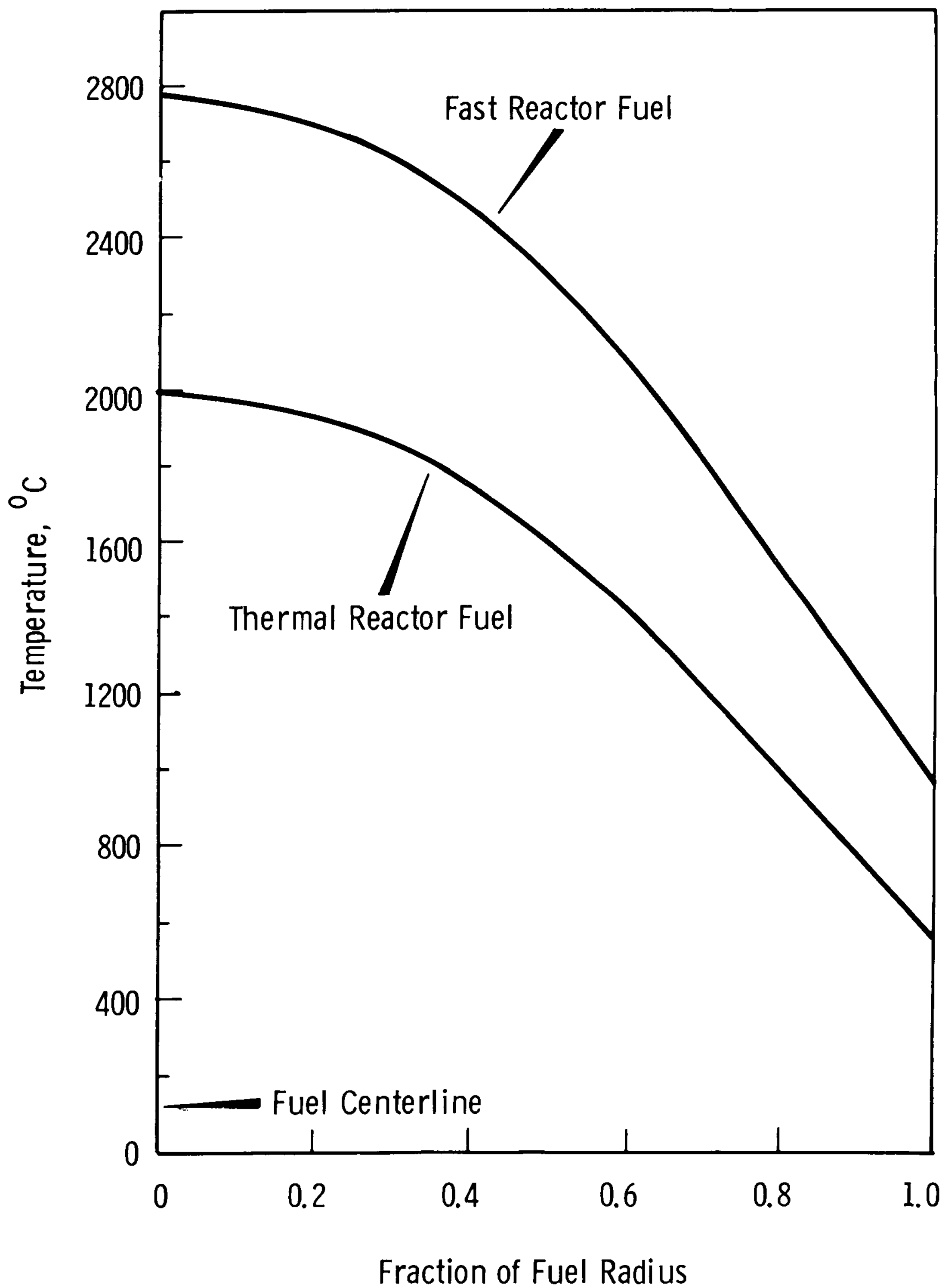


FIGURE 3: TEMPERATURE PROFILES FOR FAST AND THERMAL REACTOR FUELS
(OXIDE FUEL @ 16 kw/ft)

MATERIAL CHARACTERISTICS

Of the ceramic fuels being considered for fast reactor use, the uranium-plutonium oxides are by far the most developed, and consequently may be expected to be used in the first fast breeder reactors. Such properties as chemical and mechanical stability, compatibility with cladding, and high melting point, which have made oxide the preferred fuel material for thermal reactors, are also desirable for fast reactors. However, the relatively low thermal conductivity of oxides limits permissible pin powers. With the "no melting" criteria imposed on reactor fuel, low thermal conductivity severely limits oxide fuel performance. Additionally, the breeding gain with oxide fuel, while acceptable for early fast breeder reactors, may be too low to provide optimum economic performance.

The uranium-plutonium carbides and nitrides, while not as fully developed as the oxides, do have considerably higher thermal conductivity, and also by virtue of their higher metal density, have the potential for providing higher breeding gains. The metal densities of $(U_{0.8}Pu_{0.2})C$ and $(U_{0.8}Pu_{0.2})N$ are 12.9 and 13.5 gm/cc as compared to 9.7 gm/cc for $(U_{0.8}Pu_{0.2})O_2$. Thus, carbide and nitride fuels are expected to yield breeding gains somewhat greater than oxide fuels. For these reasons, the carbides and nitrides are considered excellent candidate materials for fast reactor fuels. Considerably more testing and evaluation of these materials, however, are required to assess their real worth. Fuel-cladding compatibility and swelling behavior of carbide fuels are the principal areas of concern. Swelling behavior of the nitrides is expected to be about the same as in the carbides, but compatibility should not be a problem. Because of the higher capture cross-section of nitrogen compared to carbon, the breeding gains with nitrides may be somewhat less than with carbides. The difference, however, is marginal, and it is not expected to be a significant factor in evaluating the two fuel materials. Undoubtedly, many of the existing reservations regarding carbides and nitrides are speculative and arise from insufficient irradiation experience with these fuel materials. It is interesting to note that in some cases the same property data are lacking for oxides that are lacking for carbides and nitrides. In the case of oxides, however, this lack is partially offset by empirical relationships obtained through the extensive use of oxides as thermal reactor fuel.

To a large extent, the relative merits of oxides, carbides, and nitrides can be assessed by comparing their thermal properties, swelling behavior, and compatibility characteristics.

Thermal Properties

Melting point, thermal expansion, and thermal conductivity are the thermal properties of principle interest.

The uranium-plutonium oxides, carbides, and nitrides all are refractory compounds with melting points ranging between 2470 and 2800 °C. Melting points for the stoichiometric composition of these materials are shown in Table 2. Because of their high thermal conductivity, the melting temperatures of the carbides and nitrides are not as critical in limiting fuel performance as in the oxide case. Without an initial nitrogen overpressure, the nitride starts to decompose at temperatures below the true melting point. In a closed system, as a free metal (liquid) phase is formed, nitrogen is released, thus providing a nitrogen over pressure which retards further decomposition. With one atmosphere of nitrogen the temperature at which decomposition occurs, 2720 °C,(1) is within 50 °C of the congruent melting point. Decomposition temperature in one atmosphere of helium without any nitrogen overpressure is still sufficiently high, 2435 °C,(2) to prevent thermal decomposition from limiting nitride fuel performance.

The melting point for oxides varies with stoichiometry, reaching a maximum at an O/M ratio of 1.97 to 1.98.(3) Although maximum thermal conductivity is obtained with stoichiometric oxide compositions,(4) the 100 °C higher melting point obtained at an O/M ratio of 1.97 to 1.98 has a greater influence on maximum permissible specific powers. For this reason, the slightly hypostoichiometric oxide is considered the preferred composition.

Thermal expansion values for uranium-plutonium ceramic fuel materials are quite similar, largely in the range of 9.0 to 12.0 x 10^{-6}/°C. Uranium dioxide undergoes a 9.6% volume increase upon melting.(5) The extent of volume increase on melting in mixed oxides, nitrides, and carbides has not been determined.

Thermal conductivity, because of its role in establishing fuel temperature, is by far the most important of the thermal properties. Depending on temperature, stoichiometry, and plutonium content, the thermal conductivity of carbides and nitrides is 3 to 6 times higher than that of the oxides. Figure 4 presents a comparison of the thermal conductivities of these three materials as a function of temperature. The thermal conductivity of mixed oxides reaches a minimum between 1600 °-1800 °C.(6-9) The thermal conductivity of the carbide and nitride fuels increases with temperature, indicating a fundamental difference in the mechanism of heat conduction.(10-12) The similarity of chemical bonds in nitrides and carbides results in essentially identical thermal conductivity at the temperatures of interest for fast reactor fuel designs. For mixed oxides, the spread in the low temperature data represents the effect of stoichiometry on the conductivity of the single phase oxide.(13) At temperatures above 1400 °C, effects of stoichiometry have not been determined and the spread represents the range of uncertainty in reported data.(7-9) The general upswing in UO_2 conductivity above ~1800 °C should also occur for mixed oxide. In fact, plutonium additions to about 20 vol% do not markedly change the thermal conductivity from that of UO_2.(6) Mixed carbides and nitrides, however, show a definite decrease in thermal conductivity relative to pure UC and UN with the addition of plutonium.(14)

Table 2
Melting Points of Ceramic Fuel Materials

Material	Melting Point, ° C	
$(U_{0.8}Pu_{0.2})O_{2.00}$	2800	(Liquidus)
$(U_{0.8}Pu_{0.2})C$	2475	(Liquidus)
$(U_{0.8}Pu_{0.2})N$	2770	(Congruent melt 10-15 atms N_2)
	2720	(Decomposition 1 atm N_2)
	2435	(Decomposition 1 atm He)

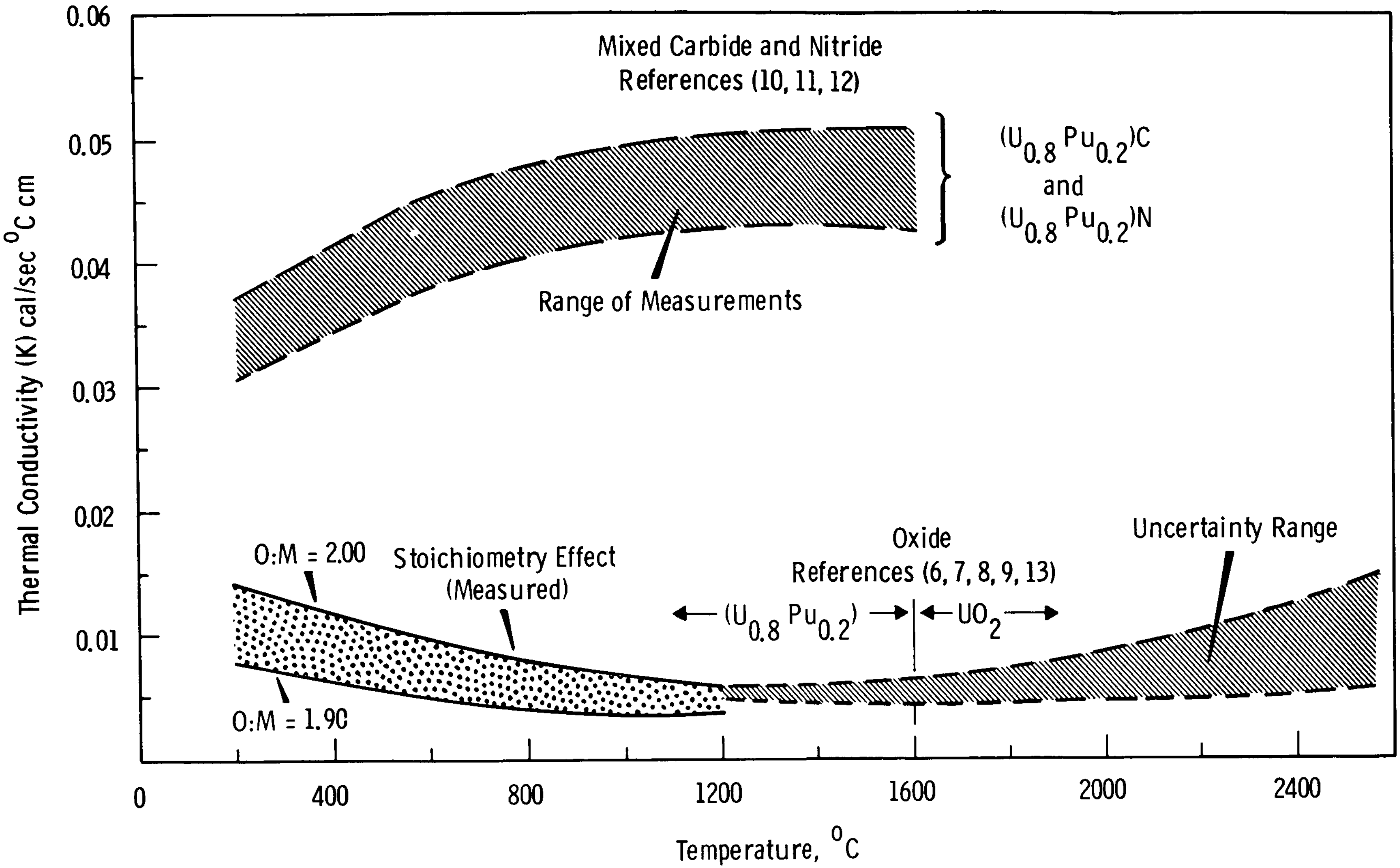


FIGURE 4: THERMAL CONDUCTIVITY OF CERAMIC FUELS (> 95% TD)

In Figure 5, the effect of stoichiometry on the thermal conductivity of these ceramic fuel materials is shown. With the mixed oxides, maximum thermal conductivities are obtained with a stoichiometric composition.[4]

It should be recognized that deviations from stoichiometry in oxides result in crystalline defects in a single phase material. In carbides or nitrides, departure from stoichiometry affects thermal conductivity by the formation of a second phase in addition to the monocarbide or mononitride. This second phase is either (U,Pu) metal in a hypostoichiometric composition, or a higher carbide or nitride phase in the hyperstoichiometric composition.

At room temperature, maximum thermal conductivities are obtained with carbides having about 4.9 weight per cent carbon.[10] In mixed nitrides, thermal conductivity apparently decreases with increasing nitrogen content.[14] The composition limits of this trend for nitride have not been defined.

Tests to determine burnup effects on thermal conductivities of ceramic fuel materials are currently in progress. No significant data from these tests are yet available. Generally, it is assumed that thermal conductivity of the fuel decreases with burnup. However, the extent of decrease may not be the same for carbides and nitrides as it is for oxides.

Swelling Behavior

Potentially the most limiting factor in ceramic fuel performance is fuel swelling. Fuel swelling is caused by an accumulation of solid and gaseous fission products in the fuel material. While the amount of fission products formed is a function of burnup, the resultant swelling is related to fuel temperature, fuel porosity creep strength, and mechanical restraint. High temperatures tend to reduce swelling by releasing fission gases and by increasing fuel plasticity, thereby accommodating fuel swelling in available internal porosity.

In Figure 6, three temperature regions in oxide fuel are identified and related to fuel swelling. In the central region, practically all of the fission gases are released and are able to move freely through the highly plastic fuel material to the plenum or fission gas vent. Thus, fission products formed in this region causes no swelling. The boundary temperature of this region is not well-defined but is considered to be between 1600 and 1800 °C. In the adjacent region, which has a lower temperature of about 1400 °C, the fuel is still somewhat plastic; fission gases are released from the lattice and coalesce to build up stresses which plastically deform the fuel. Force is thus applied to the outer region of the fuel by this plastic deformation. In this outer region practically all of the fission gases are retained, but the fuel material has sufficient strength to restrain swelling. However, in the "cracked fuel" swelling model it is assumed that the fuel in this outer region is cracked and thus transmits the forces from the adjacent plastically deformed region to the cladding. An alternate model

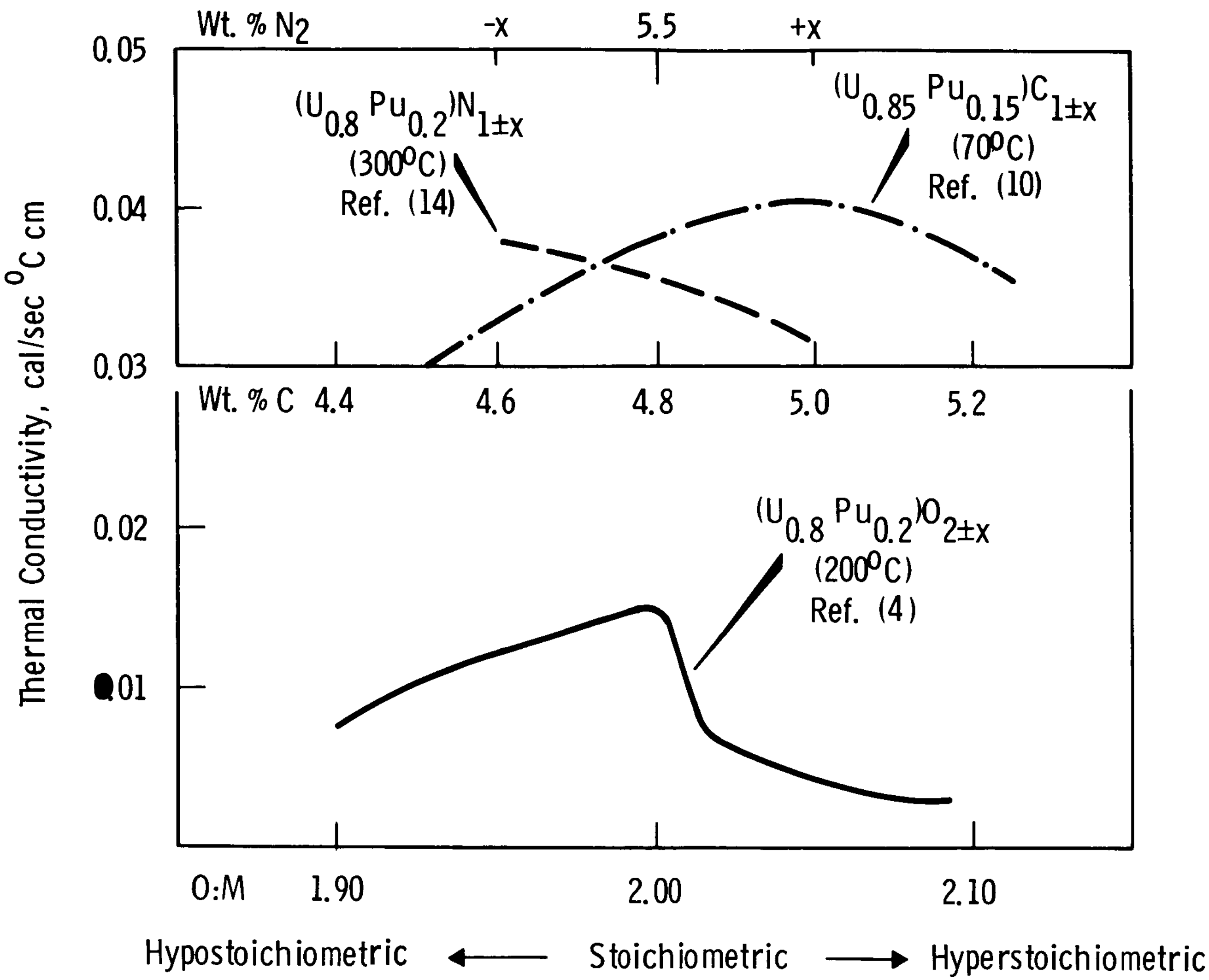


FIGURE 5: STOICHIOMETRY EFFECT ON THERMAL CONDUCTIVITY

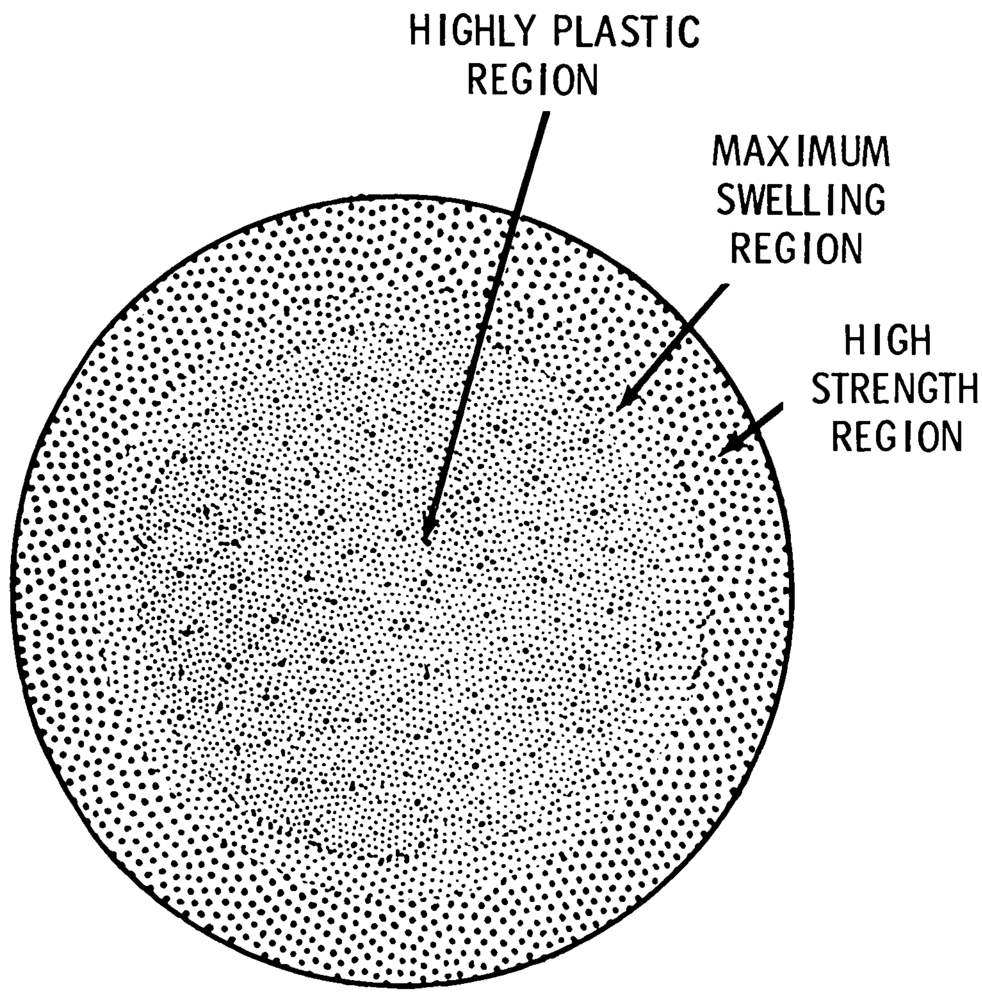


FIGURE 6: TEMPERATURE REGIONS FOR FUEL SWELLING MODEL
UO_2-20 WT. % PuO_2 FUEL @ 15 kw/ft

assumes that plastic deformation can occur from this intermediate region to the edge of the fuel. In this model, stress is uniformly applied to the cladding.

Swelling rates equivalent to 1% ΔV/at% burnup have been measured for mixed oxides.(15) This rate is considerably lower than the rate of 1.7 ΔV/at% burnup measured for UO_2 fuel.(16)

Limited swelling data are available on mixed carbides. Swelling rates between 1.1 and 1.7 ΔV/at% burnup in highly restrained specimens have been reported for hyperstoichiometric mixed carbides over a maximum temperature range of 790 to 1150 °C.(17) Swelling rates obtained in hyperstoichiometric mixed carbides in the Dounreay Reactor were 2% ΔV/at% burnup at 1500 °C and 0.5% ΔV/at% burnup at 950 °C.(18) The large increase in swelling rate between 950 and 1500 °C appears to indicate that the "breakaway" swelling which has been observed for UC in this temperature range will also occur in mixed carbides.

The swelling behavior of mixed nitrides has not been determined. Swelling data from tests of uranium mononitride indicate rates of approximately 1.4 to 2.0% ΔV/at% burnup at temperatures to 1200 °C.(19) A similarity of swelling behavior for carbides and nitrides is to be expected because of their similarity in chemical and physical properties. In general, measured and inferred rates for both the carbides and nitrides appear to be somewhat higher than for the oxides. The higher swelling rates may be caused by the lower temperatures at which carbide and nitride fuels operate rather than by any inherent characteristic of the material. For example, at 15 kW/ft, maximum fuel temperature in carbides would be about 1050 °C with a helium bond and 800 °C with a sodium bond. At twice the linear power, 30 kW/ft, the maximum fuel temperature obtained in the fuel would be about 1500 ° with a helium bond and about 1050 °C with a sodium bond. These temperatures are probably too low to provide significant plasticity in the fuel or a high fission gas release rate. Thus, at specific powers which appear feasible for current fuel design somewhat more swelling can be expected with carbide and nitride fuels than with oxide fuels.

Compatibility

A key criterion for LMFBR fuel material is compatibility with cladding and with sodium. Oxide fuels are generally considered to be compatible with stainless steel; however, there is evidence that some reaction between the oxides and stainless steel does take place.(20) There is no evidence that the limited reaction observed is detrimental to fuel performance.

Although oxide fuels are expected to utilize a helium bond, concern regarding oxide-sodium compatibility exists because contact between these materials might result from cladding failure or, in the case of a vented fuel, from entry of sodium through the vent. Out-of-reactor tests have shown that stoichiometric and hypostoichiometric mixed oxides do not chemically react with sodium at 750 °C. Hyperstoichiometric mixed oxide pellets in contact with sodium at 750 °C were reduced to powder.[21] In-reactor tests have shown slight reaction of mixed oxides with sodium, but no detrimental effects have been observed.

It is difficult to generalize on the compatibility problems of mixed carbide fuels with stainless steels. Recent experiments have shown that problems anticipated on the basis of uranium carbide experience may not exist with mixed carbides. When composition and impurities in the mixed carbides were closely controlled, no compatibility problems with stainless steel were encountered. However, the possibility for carbon transfer to stainless steel is always present if departure from the monocarbide composition occurs.

The higher carbides $(U,Pu)_2C_3$ and $(U,Pu)C_2$ occurring in hyperstoichiometric fuel apparently are solely responsible for transfer of carbon from the fuel to other portions of the system. Reaction with stainless steel always occurs in the presence of dicarbide $(U,Pu)C_2$ but is quite limited or absent if only the sesquicarbide $(U,Pu)C_3$ is present in concentration near 10 vol%.[22] Within a composition range limited to the stoichiometric or slightly hyperstoichiometric carbon contents, the mixed carbides appear stable at 800 °C in both direct contact and sodium bonded designs.[23] Hypostoichiometric compositions do not pose problems associated with high carbon activity, but the free metal phase can react with cladding to form intermetallic compounds or low melting eutectic compositions. As with the hyperstoichiometric fuel, the reactivity is greatest with large departures from stoichiometry. Slightly hypostoichiometric compositions (∿4.7-4.8 wt% C) have been shown to be stable in contact and sodium bonded systems with stainless steels.[24]

One reason for the attractiveness of nitride fuels is their favorable compatibility characteristics as compared to the carbides. Virtually all tests to date indicate that stoichiometric nitrides are compatible with stainless steel in both direct contact and a sodium bond.[25][26] Apparently the actual difference between nitrides and carbides in this respect involves the relative ease with which nitride stoichiometry can be controlled during fabrication and the somewhat simpler phase diagram relationships in nitride systems. In tests in which nitrides, deliberately synthesized to contain a large excess of nitrogen (i.e., containing a sesquinitride phase) some nitriding of stainless steel was observed. The nitrides appear to be particularly stable in a sodium bond, even when penetrated by sodium to the interior of the fuel at temperatures above 1000 °C.[27]

DESIGN FACTORS INFLUENCING FUEL PIN PERFORMANCE

Before reliable designs for ceramic fuels can be established, information is needed on fuel pin behavior under steady-state, off-standard, and transient conditions. To a large extent, this information must be obtained on prototype or near prototype fuel pins with temperatures and burnups representative of those obtained in LMFBR's. Fuel development efforts underway and planned are focused on obtaining performance data needed in the near future for designing mixed oxide fuels for the Fast Flux Test Facility (FFTF) and early demonstration reactors. The longer range aspects of some of these efforts are expected to provide data for designing oxide and carbide or nitride fuels for commercial LMFBR's.

An important factor in any fuel design is the manner in which released fission gases are handled. Providing a plenum as an integral part of the fuel pin is probably the simplest method of handling released fission gases. However, the gas pressure buildup in such a pin design will (a) affect cladding strength requirements, and (b) could, in the event of cladding failure, cause serious consequences. A vented fuel system, while eliminating the problems associated with the plenum, raises problems with the handling of vented radioactive gases and possible backflow of coolant sodium into the pin. Fuel designs for the FFTF and early demonstration reactors will probably utilize gas plenums. Commercial LMFBR's will probably have to employ vented fuel designs to achieve desired high burnups at high specific powers.

Sodium bonding provides a means of enhancing effective thermal conductivity and providing an annular space (without significantly increasing fuel temperatures) for accommodating fuel swelling. The performance potential of sodium bonded fuel may be seriously affected, however, by degradation or partial loss of the bond during irradiation. The effectiveness of the sodium bond as a heat transfer medium may be reduced by the buildup of fission products in the sodium. Also, released fission gases may form bubbles in the bond, thus creating hot spots. In high thermal conductivity fuels such as carbides or nitrides, voids or bubbles in the sodium bond may not be as detrimental as in oxide fuel.

The potential problem resulting from fuel failures must be determined. Until the consequences of fuel pin failures can be established, fuel designs and operating limits must be conservative. Items of concern are:

- Failure propagation in the closed packed fuel pin subassemblies;
- Ejection of the sodium at a defect by the pressure of contained fission gases in sodium bonded unvented fuel;
- Formation of a gas blanket around the defected and adjacent pins by fission gases forced out of gas bonded, unvented fuel,
- Necessity for immediate reactor shutdown to remove failed fuel; and
- The extent to which fuel material may be washed out by the sodium coolant.

Smear density and void deployment may be key factors in accommodating fuel swelling. Void volume in fuel pins may be obtained by the use of (a) an annular gap with high density pellets, (b) cored pellets, (c) low density pellets, and (d) vibratory compacted fuel particles. The relative merits of uniformly distributed void volume (as obtained with low density pellet or vibratory compacted particles) and "concentrated" void volume (as obtained with high density pellets with annular gaps or core pellets) have not been determined. Smear density and void deployment can also affect effective thermal conductivities; thus, specification of smear density and void deployment must consider fuel temperature limitations as well as fuel swelling behavior.

SUMMARY AND CONCLUSIONS

Ceramic compounds offer the best possibility of meeting the stringent performance requirements of LMFBR's. Although there are large gaps in the material properties data and irradiation experience available for oxides, carbides, and nitrides, there appears to be no unfavorable characteristics inherent in these materials that would prevent their use as fast reactor fuels. Realizing their potential performance capabilities will be largely dependent upon developing designs capable of exploiting favorable characteristics of the materials.

Oxides

Oxide fuels are to be used in the FFTF and early demonstration reactors. Development efforts underway and planned, if reasonably successful, should provide designs capable of achieving 50,000 MWd/tonne. With additional development effort and experience obtained from these early oxide cores, fuel designs capable of attaining 100,000 MWd/tonne should be available for commercial LMFBR's. The economic performance of oxide fuels may be limited by its low thermal conductivity and low breeding ratio. Permissible burnup may be limited by fuel swelling.

Carbides and Nitrides

Uranium-plutonium carbides and nitrides, because of their high thermal conductivity and metal atom densities, are considered excellent materials for providing economic LMFBR fuels. The high specific power potentially achievable with carbides or nitrides could reduce fuel cycle costs significantly. Figure 7 shows maximum fuel temperatures as a function of specific power for helium bonded oxides, and helium bonded and sodium bonded carbides (or nitrides) in 1/4 inch diameter fuel pins. Because thermal conductivity values for nitrides and carbides are sufficiently close, one curve can be used to represent both fuel materials. In oxide fuels, specific powers are limited by maximum fuel temperatures (i.e., fuel melting). Specific powers in carbides or nitrides will probably be limited by surface heat flux. At a surface heat flux of 2×10^6 Btu/hr/ft^2, a value which is expected to be limiting in current designs, maximum specific powers with carbides (or nitrides) will be 1.5 to 2 times greater than in oxide fuels. Maximum fuel temperatures at limiting specific powers will be 1750 °C with a helium bond and 1150 °C with a sodium bond. Fuel designs

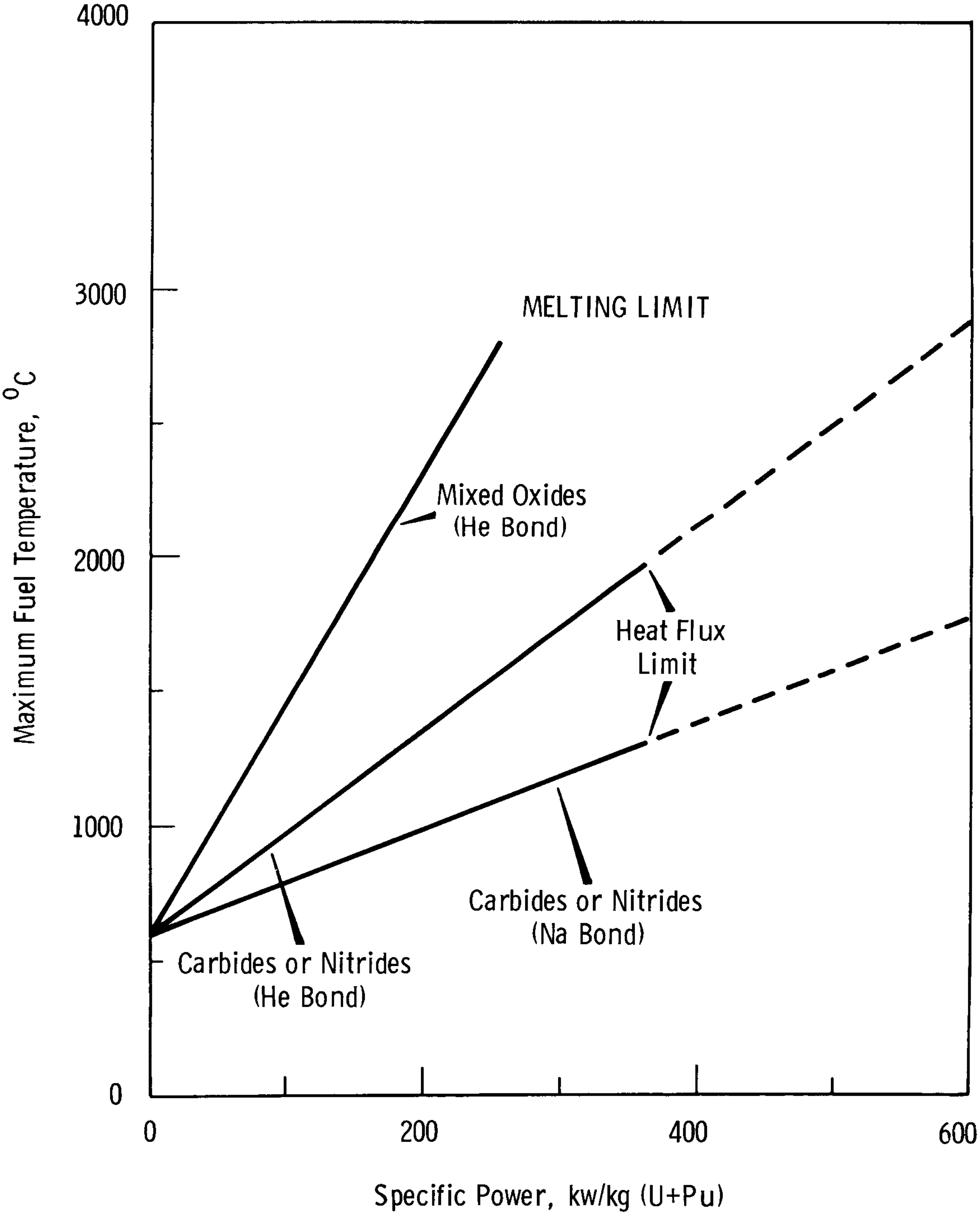


FIGURE 7: FUEL TEMPERATURE AS AFFECTED BY SPECIFIC POWER
(0.25 INCH DIA. PIN)

are needed which can better utilize the thermal conductivity advantage of carbides and nitrides. Sufficient information for developing such designs is not now available.

The limited data available on the properties of carbides and nitrides will permit only very tentative conclusions regarding their most restrictive characteristics. Performance of carbide fuels may be limited by high swelling rates and fuel-clad compatibility problems. Swelling is also of concern with nitride fuels, but there should be no compatibility problems with stainless steels.

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DISCUSSION

C.A. Anderson (Westinghouse) - On your comment on uranium-plutonium carbide breakaway swelling - I may have been misleading in my talk yesterday when I showed a very large scatter in the swelling vs. temperature data, but since then we have looked at this again and what we have done is sort of like taking a random audience and saying that 50% of the men and women are wearing dresses. But then later on if you break apart the population, you find that 100% of the women are wearing dresses and none of the men. On the uranium carbide swelling data we found that the breakaway swelling which occurred was mostly in the hypostoichiometric material. The population was essentially the same at low temperatures, but we got only 5% probability that the populations were the same for hyper and hypo above the breakaway swelling point. We find no breakaways for hyper at temperatures up to about 1400°C. Similarly, we expect the chromium carbide modified hypostoichiometric fuels that we are using in our design to behave similarly to the hyperstoichiometric. We don't expect these large swelling rates above the breakaway temperatures.

Another comment on the venting mechanism and your worry about cleanup of gas in sodium. I don't think this is serious because if you are going to hypothesize the commercial design, you are going to have to tolerate some leakage of fuel element at sometime in plant life. Our general idea is something like 1% of the core failures sometimes toward the end of the core life. Once you have done that you have the problem anyway.

C.B. Zitek (Commonwealth Edison Company) - Is there any chance that some of the early work that was done on the oxide fuel is still classified? I am primarily concerned with the problem of propagation of failure. Do you know, has all the previous work on oxide fuels been declassified?

S. Goldsmith - I don't know of any classified information on oxide fuel failures which would add a great deal to what we already know and is already available in the unclassified literature.

C.B. Zitek - Has anyone made a survey of the previous work? I know way back there was a lot of work done on oxide and I was wondering whether all of this has been declassified or not.

S. Goldsmith - To my knowledge the vast majority of the information has been declassified.

P. Cohen (Westinghouse) - If you are referring to the progressive failure tests of the water reactors, there was a program in connection with the Shippingport plant for which I was responsible and a number of progressive failure tests were run. We came to the conclusion that it was not a significant factor in the Shippingport design. I don't want to imply, since I have no basis for saying so, that there isn't such a problem in the liquid metal reactors, but I would suggest that a considerable amount of out-of-pile work be undertaken before any in-pile tests on this problem are done because it is just one hell of a problem.

S. Goldsmith - Yes, I agree. Actually the problem in LMFBR may be somewhat more severe than in thermal reactors because of the closer packing of the fuel pins.

(Anonymous) - I was wondering - when you compare swelling with UO_2, UN, and UC, since it is pretty strongly temperature dependent, shouldn't you really compare them at about the same temperatures or equivalent temperatures? For instance, with a UN fuel pin, you probably go to no helium bond or maybe a larger diameter pin, so for swelling comparison purposes, shouldn't it really be done at about the same temperatures?

S. Goldsmith - Yes, this is my contention that most of the data on the carbide and nitride have been obtained at temperatures considerably lower than the oxide and, of course, I think the reason for this is that the actual operating temperatures for carbides and nitrides will probably be considerably lower; so that while you are not getting a true comparison of the material characteristics by comparing them at these different temperatures, you are getting a comparison of the fuel performance properties.

J.F. Schumar - Cliff, one piece of information - the ANS and the AEC are having a monograph prepared by Massoud Simnad at General Atomic on all the operational histories available on fast reactor fuel elements. I don't know when the monograph is coming out.

IRRADIATION BEHAVIOR OF CLADDING AND STRUCTURAL MATERIALS

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ABSTRACT

The effects of irradiation on the mechanical and physical properties of materials to be used as cladding and structural components in fast reactors are of great interest to the reactor designer. In this paper the general aspects of the problem are discussed in terms of the observed changes in properties and microstructure and the possible mechanisms that might explain the observed effects. The discussion is concerned primarily with the austenitic stainless steels and with changes in mechanical properties which occur at test temperatures near the irradiation temperatures. For convenience the problem is divided into three ranges of irradiation temperature: low temperatures, $T < 0.40\ T_m$; intermediate temperatures, $0.40\ T_m < T < 0.55\ T_m$; and high temperatures, $T > 0.55\ T_m$. (T_m is the melting point on the absolute temperature scale.) On the basis of data presently available the damage appears to be significantly different for each temperature range. In the low-temperature range there is an increase in yield strength and reduction of work-hardening coefficient and uniform strain. These effects result primarily from the interaction of dislocations with irradiation-produced defects. At intermediate temperatures irradiation-produced changes in the precipitation process become important. In this same temperature range the formation of voids and dislocation loops after irradiation to high fast neutron fluences cause large increases in yield strength and large reductions in ductility parameters. At high-irradiation temperatures strength properties are not affected; however, ductility is severely reduced. These effects result from helium produced by various (n,α) reactions.

INTRODUCTION

Changes in mechanical and physical properties of fuel cladding and reactor structural components which occur as a result of neutron irradiation are of major importance to the reactor designer. For example, large reductions in either the strength or ductility of the material used as a fuel cladding would severely limit its ability to withstand the imposed stresses without excessive deformation or fracture. Materials used in a fast reactor system must retain adequate strength properties under rather severe operating conditions. The fuel cladding will operate at temperatures between 400 and 700°C, will be exposed to fast neutron fluxes of 1×10^{15} to 1×10^{16} neutrons cm^{-2} sec^{-1} and during its lifetime in the reactor will receive fast neutron fluences in excess of 10^{23} neutrons/cm^2. Other structural components may operate at somewhat lower temperatures and neutron fluxes but because of their longer residence time in the reactor they may receive significantly higher neutron fluences.

Data describing the effects of such irradiation conditions on the mechanical and physical properties of materials are very limited. It is thus necessary to combine the relevant data obtained from irradiations conducted in thermal reactors with the data from fast reactor irradiations in order to evaluate the expected changes in mechanical and physical properties.

We shall restrict our discussion mainly to the behavior of austenitic stainless steels and include results from other alloy systems only to demonstrate general conclusions. This limitation is imposed because the first liquid metal fast breeder reactors will be constructed of these alloys and because the effects of irradiation on mechanical and physical properties are best understood in these alloy systems.

PRODUCTION OF DEFECTS

Neutron irradiation of a crystal has two basic effects. First, neutrons collide with lattice atoms and may displace some atoms. A single displacement leaves one lattice site vacant, a vacancy, and locates one atom in an off-lattice position, an interstitial atom. The second effect, transmutation, is initiated by a neutron capture and results in a changed mass number of the capturing atom.

Vacancies and interstitials are produced primarily as a result of collisions between moving particles (neutrons or displaced atoms) and lattice atoms. Assuming that such collisions can be treated as elastic collisions between hard spheres, the maximum energy transferred when a particle of mass m_1 and energy E strikes a particle of mass m_2 at rest is

$$E_{max} = \frac{4m_1m_2}{(m_1 + m_2)^2} E_1 \qquad (1)$$

Since the neutron has a mass number of 1, this becomes

$$E_{max} \approx 4E_1/A_2 \qquad (2)$$

where A_2 is the mass number of the struck particle. The average energy transfer is half the maximum amount. Now, if the energy transfer to the struck atom exceeds some threshold value, usually estimated to be about 25 ev, the atom will be displaced from its lattice site. Such an atom, termed a primary knock-on, will interact with lattice atoms in its vicinity, possibly displace some of them, and gradually come to rest. If the struck atom receives a large amount of energy, its more loosely bound electrons will be stripped from it, leaving it highly ionized. Under these conditions it will initially lose energy primarily through electronic interactions, but as it slows down it will make frequent collisions with lattice atoms, the frequency increasing as the energy of the knock-on decreases.

Calculation of the total number of displaced atoms produced is obviously a complex problem. To illustrate the order of magnitude of the number we will follow the treatment of Kinchin and Pease.(1) They assume that the knock-on loses energy entirely by ionization above some cutoff energy approximately equal to the mass number of the struck atom in thousands of electron volts and entirely by elastic collisions with lattice atoms below this cutoff energy.

The number of additional displaced atoms produced per primary knock-on atom is approximately

$$N_d = \frac{E}{2E_d} \text{ for } 2E_d < E < E_i \tag{3}$$

and

$$N_d = \frac{E_i}{2E_d} \text{ for } E > E_i \tag{4}$$

where

E = the energy of the primary knock-on,

E_d = the threshold displacement energy, approximately 25 ev for metals,

E_i = the energy of the primary above which it is assumed that only ionization and no displacements are produced.

For example, if an iron atom (M = 56) is struck by a 1-Mev neutron, the maximum energy transmitted to the primary is [by Eq. (2)]

$$E_{max} = \frac{4 \times 1}{56} \approx 0.07 \text{ Mev}$$

This is above the ionization energy, so the number of displacements per primary is [by Eq. (4)]

$$N_d = \frac{56,000}{2 \times 25} \approx 10^3 \text{ displacements}$$

It is important to realize that the displaced atoms are not produced homogeneously throughout the material. For an individual collision the defects reside in a small volume around the track of the primary knock-on, which typically extends a few tens or perhaps hundreds of angstroms. This volume is termed a displacement cascade, but in reality it may be composed of subcascades produced by secondary knock-ons. Note too that the distribution of vacancies and interstitial atoms within a cascade is not uniform. In general, the interstitials are displaced outward, leaving a vacancy-rich core in the center of the cascade.

Such regions are generally unstable and some dynamic recovery occurs. The amount of recovery and the final configuration of the defects depend critically on the irradiation temperature. At temperatures of interest for normal reactor operation, both the interstitials and vacancies have sufficient thermal energy to migrate through the lattice. Many of the original defects are destroyed by recombination, trapping at impurities, or absorption by dislocations and grain boundaries. Those which survive cluster together to form stable configurations. At temperatures in excess of approximately one-half the absolute melting point (0.5 T_m) vacancies have sufficient thermal energy to overcome the binding energy of clusters and to migrate freely through the lattice. Thus at sufficiently high irradiation temperatures defects are annihilated continuously without cluster formation.

Transmutation reactions, in particular those which produce gaseous species, may also have important effects on properties. Table 1 lists the reactions and their approximate cross sections for a number of important cases. We see that helium and hydrogen may be produced in metals through neutron reactions both with impurities in the metals and with the major alloying elements. Alter and Weber[2] have made calculations of the amounts of hydrogen and helium produced in various materials and concluded that for the iron- or nickel-base alloys used as fuel cladding, approximately 100 ppm He and a few thousand parts-per-million hydrogen would be produced in a fast reactor in a few years operation. In addition to these transmuation reactions producing gaseous products, other possibilities exist in which solid impurities are produced.

EFFECTS OF IRRADIATION ON MECHANICAL PROPERTIES

Changes in mechanical properties produced by neutron irradiation are a sensitive function of both irradiation and test variables. Important irradiation variables include irradiation temperature, thermal neutron fluences, fast neutron fluences, and possibly fast neutron flux. Important test variables include test temperature and strain rate. Other factors such as preirradiation heat treatment (in order to control grain size, dislocation structure, and precipitate distribution) and time at temperature (thermal aging) either before or following irradiation have been shown to be important. Because of the large number of variables and the vast amount of information which has been published in this area we will not attempt a complete literature review. Rather we will restrict our discussion to a general class of metals and alloys (those having a face-centered cubic crystal structure) and will be concerned primarily with the mechanical properties at test temperatures near the irradiation temperature. For convenience we define the following temperature ranges: low temperatures, $T < 0.40\ T_m$ (where T_m is the melting point of the alloy in degrees absolute); intermediate temperatures, $0.40\ T_m < T < 0.55\ T_m$; and high temperatures, $T > 0.55\ T_m$. Our approach to the subject will be to summarize the observed changes in properties, point out the important variables, illustrate changes in microstructure and where possible correlate these changes with specific mechanisms.

Table 1. Transmutation Reactions in Metals

Nucleus	Reaction	Cross Section (barns)[a]	Neutron Energy Associated with Cross Section
^{14}N	(n,α)	41	Fission
^{10}B	(n,α)	3800	Thermal
	(n,α)	635	Fission
^{56}Fe	(n,α)	0.35	Fission
	(n,p)	0.87	Fission
^{58}Ni	(n,α)	0.5	Fission
	(n,p)	111	Fission

[a]1 barn = 10^{-24} cm^2.

Low Temperatures

Tensile deformation of face-centered cubic metals at low temperatures is usually terminated by a plastic instability, termed necking, which leads to the development of a local reduced diameter region followed by a shear fracture in this necked region. This local necking limits the elongation of the material. The conditions under which this instability occurs can be represented analytically.[3] Assuming constant volume and a power-law relationship between true stress ($\bar{\sigma}$) and true strain ($\bar{\varepsilon}$) of the form

$$\bar{\sigma} = k\bar{\varepsilon}^{n} \tag{5}$$

where k is a constant, it can be shown that the plastic instability occurs when the work-hardening exponent

$$n = \frac{d \ \ln \bar{\sigma}}{d \ \ln \bar{\varepsilon}} = \frac{\bar{\varepsilon}}{\bar{\sigma}} \frac{d\bar{\sigma}}{d\bar{\varepsilon}} \tag{6}$$

equals the true strain,

$$n = \bar{\varepsilon} \tag{7}$$

Figure 1 shows that Eq. (7) is reasonably well obeyed for type 304 stainless steel, but that n is not constant over the entire test.

When austenitic stainless steels are irradiated and tensile tested in this low-temperature range, there is a large increase in yield stress and large decreases in true uniform strain and work-hardening exponent.[4,5] Figure 2 shows the room-temperature yield stress of type 304 stainless steel after irradiation to 7 x 10^{20} neutrons/cm^2 (E > 1 Mev) and 9 x 10^{20} neutrons/cm^2 (thermal) at various temperatures. For irradiation at temperatures between 93 and 300°C (approximately 0.35 T_m) the yield stress was increased by approximately a factor of 3. Typical stress-strain curves from this investigation are replotted in Figure 3. For irradiation temperatures of 93 and 300°C the true fracture stresses and true strains were approximately the same as the unirradiated specimen. Values of engineering elongation were somewhat less for the irradiated specimens. After irradiation at 454°C the elongation has increased again, but the fracture stress and strain were somewhat lower than in the other tests, indicating that a different mechanism is operating at 454°C than at the lower temperatures. Figure 4 shows that the work-hardening exponents in the plastic range are consistent with the uniform and total elongation values as predicted by Eqs. (5) through (7).

Before examining the effects of neutron fluence, test temperature etc., we should first consider the behavior in terms of microstructural changes and the interaction of dislocations with the irradiation-produced defect clusters. At irradiation temperatures of approximately 350°C and lower "black spots" on the order of a few tens of angstroms in diameter are observed in the microstructure of irradiated specimens. An example of this type of damage for irradiation at 93°C is shown in Figure 5. At higher irradiation temperatures the spots have a larger size and decreased density, as shown in Figure 6. After irradiation at 371°C both the spot density and yield stress (see Figure 2) are decreased markedly. Iregularly shaped planar defects,

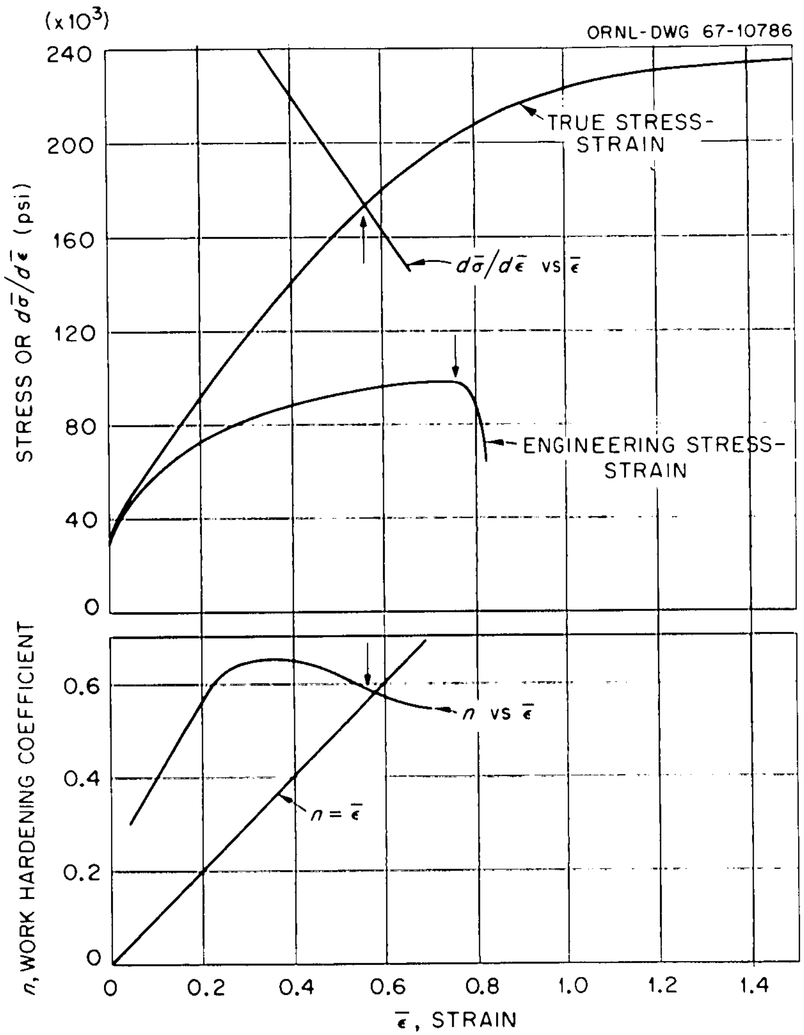
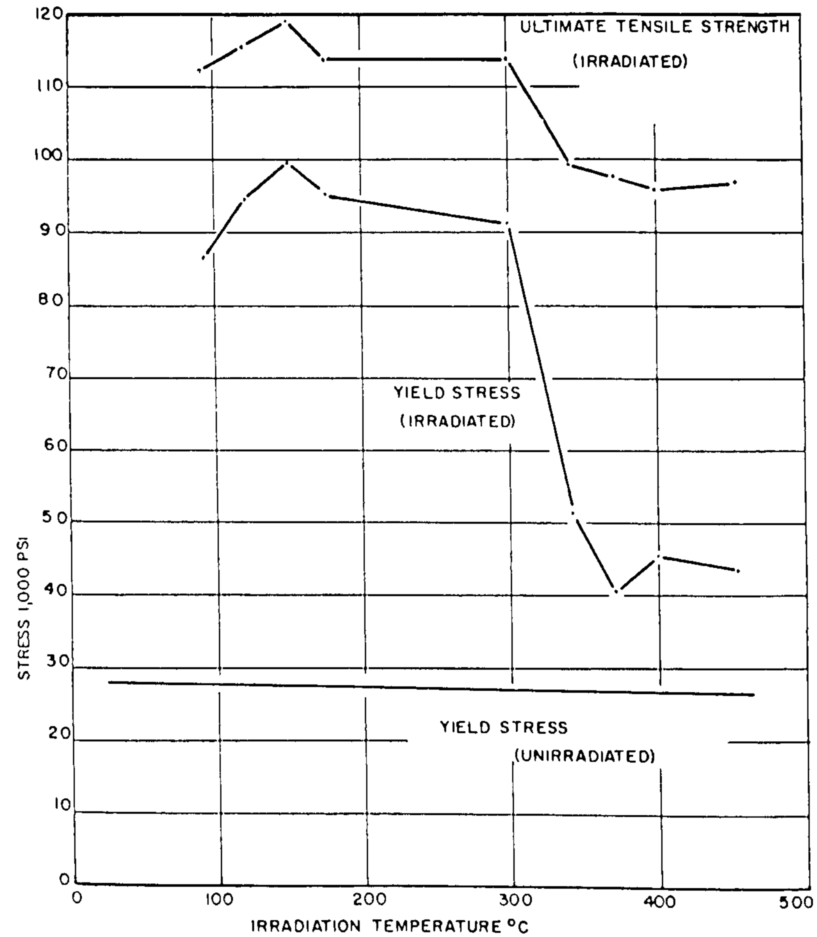


Figure 1



ROOM TEMPERATURE TENSILE PROPERTIES OF IRRADIATED TYPE 304 STAINLESS STEEL (7×10^{20} nvt E > 1 MEV)

Figure 2

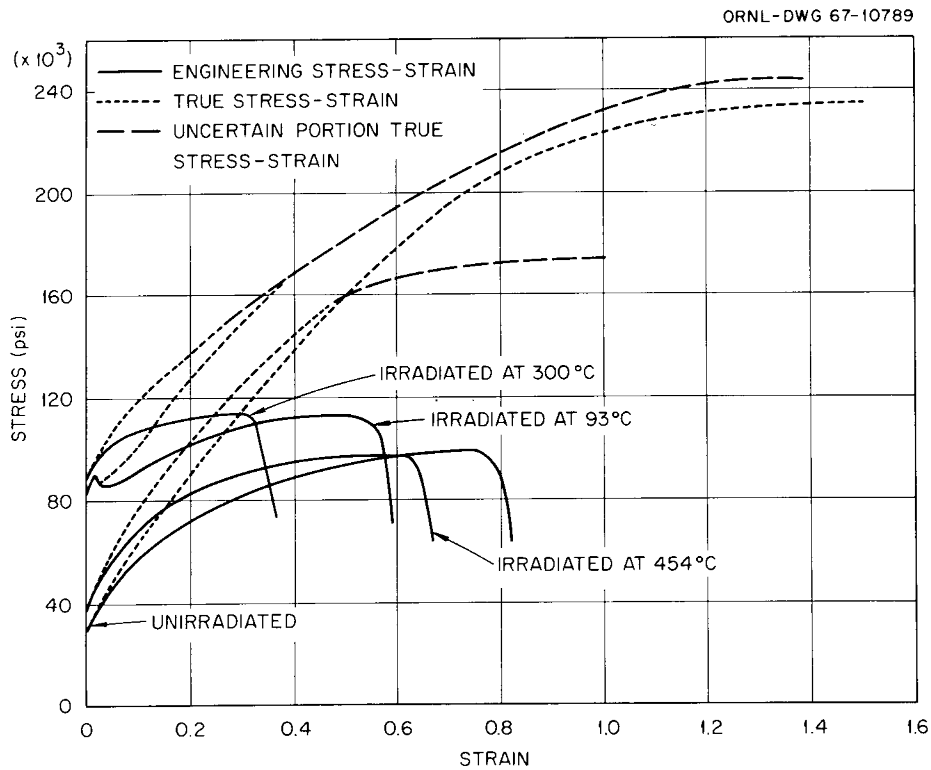


Figure 3

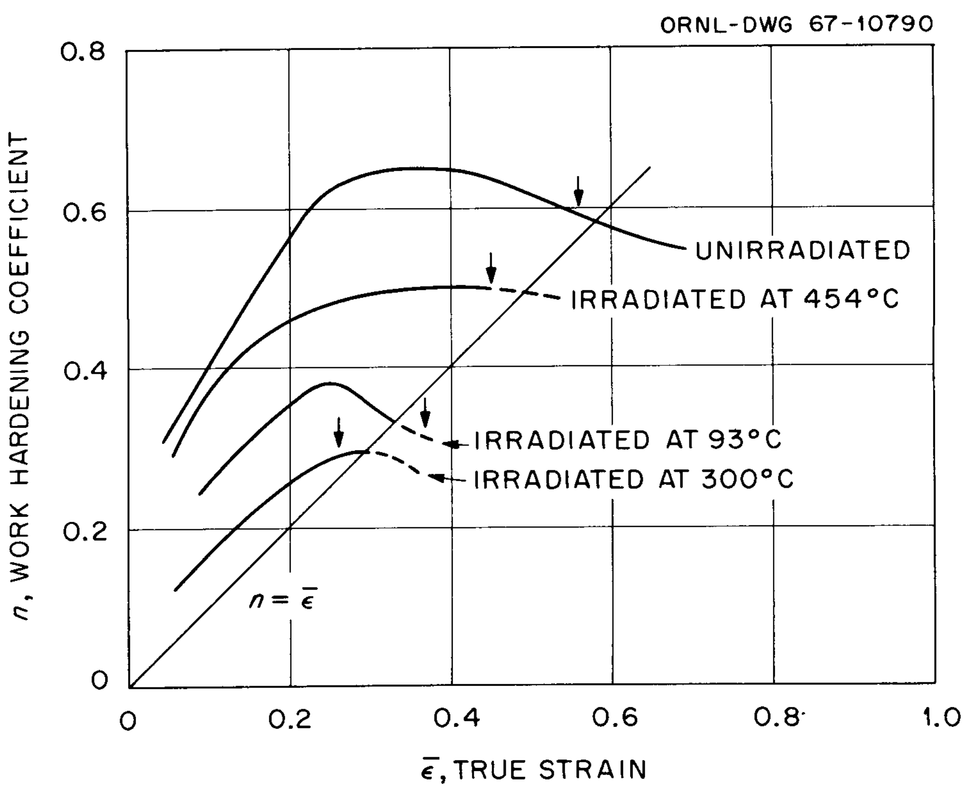


Figure 4

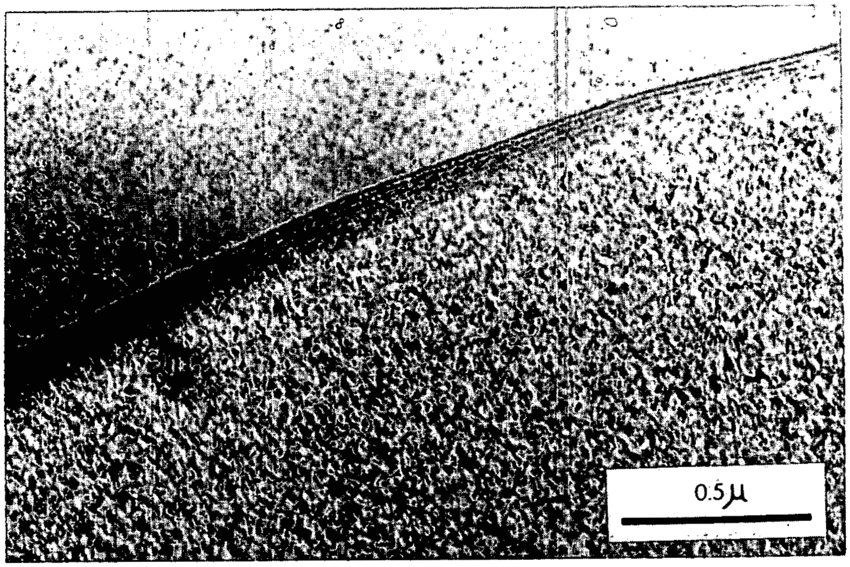


Figure 5

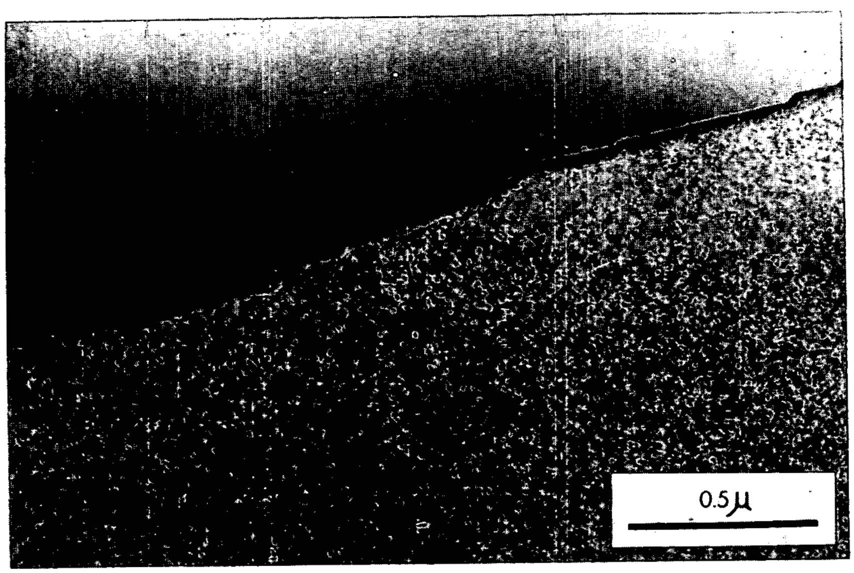


Figure 6

probably precipitates, developed, but these were widely enough spaced that they did not affect the yield stress. At an irradiation temperature of 454°C the dot-like defect clusters were completely absent. As shown in Figure 7, there was extensive precipitation at this temperature, including a heavy precipitate layer and an associated denuded zone at the grain boundaries.

These observations are in good agreement with those of Armijo et al.[6] who detected a dot-like damaged structure in the same material irradiated at 43 and 343°C to fast neutron fluences of 10^{20} and 10^{21} neutrons/cm^2, respectively. These authors report that the defects were considerably larger in the specimen irradiated to the higher fluence at the higher temperature.

Recent quantitative electron microscopy studies of irradiated face-centered cubic metals have at various times claimed the dot defects to be exclusively vacancy clusters and loops,[7,8] intersititial clusters and loops,[9,10] or mixtures composed of small vacancy clusters and larger, resolvable interstitial loops.[11,12] As these differences still have not been resolved, we must at this point conclude that all can probably be formed but that experimental circumstances (irradiation temperature, flux, and fluence) determine the proportions in which each occur.

Transmission electron microscopy[13-16] of post-irradiation deformed single crystals of copper and molybdenum has shown channels in which the radiation-induced defect structure has been eliminated. The interpretation is that glide dislocations sweep out or in some manner remove the radiation-induced defects. The channels are generally clean except for deformation-induced tangles and dipoles. The radiation defects are completely eliminated from the channels[16] and not simply pushed to the edge of the channel, as was originally suggested.[13] Sharp[16] examined annealed specimens containing channels and found no development of structure within the channels, as would be expected if they contained a high density of point defects or point-defect clusters below the resolution limit of the microscope. The mechanism by which the moving dislocations destroy the radiation-produced defects has not been determined. The slip associated with the channels, determined by measuring the slip line offsets, corresponds to the passage of two or three dislocations on each plane within the channel, so ample opportunity exists for dislocations to remove all the defects present.

The channels gradually fill with tangles and deformation-induced debris, through normal work-hardening processes, and this ultimately halts deformation in the channels. Sharp[16] observed a higher density of debris existing on a smaller scale in the channels than in unirradiated material, but attributed this to the higher stress at which the slip band developed. During the latter stages of deformation the slip line pattern of irradiated crystals appears similar to that of unirradiated materials.

Seeger[17] suggested that the defect clusters harden the lattice by providing obstacles which moving dislocations must cut with the combined aid of the applied stress and thermal fluctuations. As a result of this chopping, the defects are gradually reduced in strength and ultimately destroyed or eliminated by the dislocations, leading to the channels that are observed.

Makin and Sharp[18] pointed out that in irradiated materials relatively

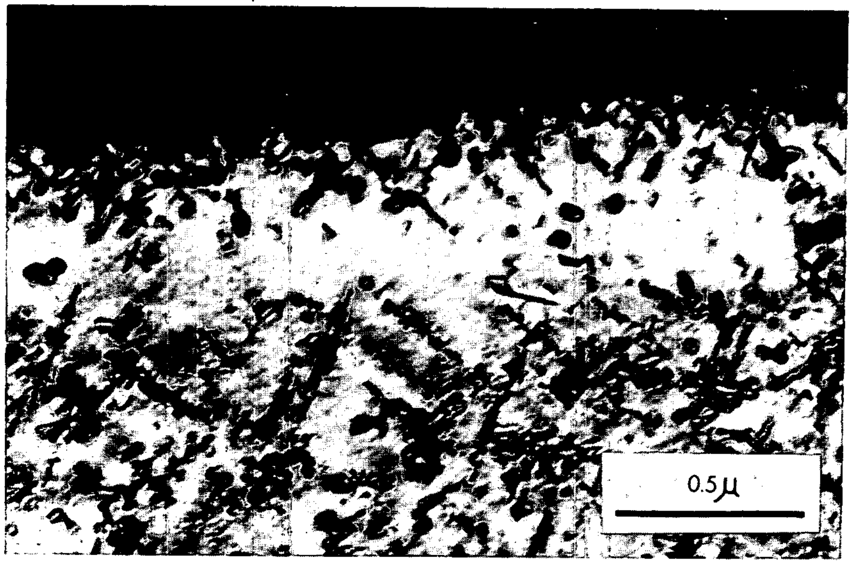


Figure 7

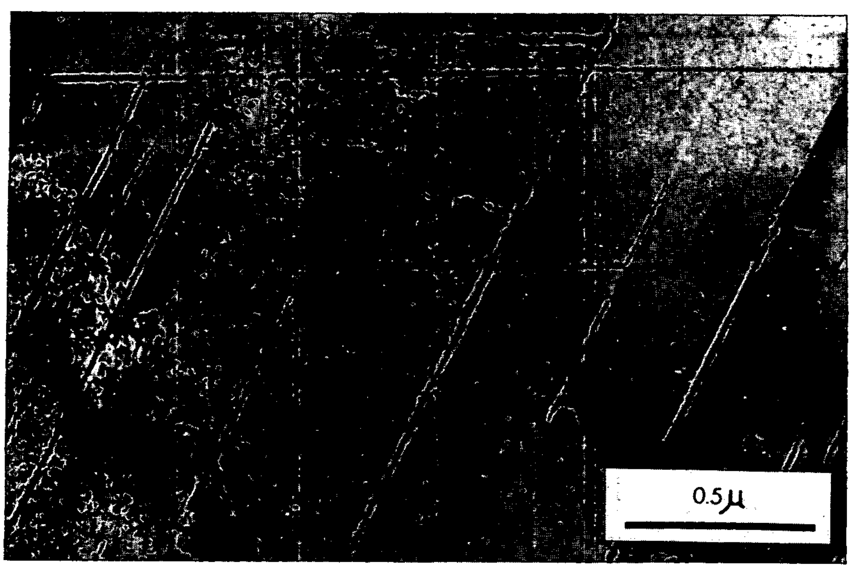


Figure 8

few slip lines are observed, indicating that few sources are activated, that full-grown slip lines form dynamically in times of the order of a millisecond, and that partially formed slip lines are not observed. They proposed on the basis of elimination of the defects by moving dislocations that the critical stress to form a slip band is the stress required to operate a source in the environment of the defect structure. Subsequent loops can be formed more easily, since the first one clears a path for them. A pileup then forms and expands, creating the cleared channel very rapidly at the high stress levels necessary to generate the first dislocation. The result is creation of a soft zone in a hardened material in which extensive localized shear occurs in a short time until work hardening halts the deformation.

These observations provide a qualitative explanation for the reduced work-hardening coefficients, increased yield stress, and low uniform elongations in irradiated materials. The channeling produces a soft zone in a very hard material, zones in which extensive slip occurs. Because of the limited number of sources or slip systems the dislocation tangling and interactions which normally lead to work hardening occur more slowly and result in a reduced rate of hardening. Figure 8 illustrates the narrow regions to which slip is confined in stainless steel irradiated at 121°C and deformed 10% by rolling at room temperature. The defect structure is still clearly visible in the regions between slip bands. The magnification is not high enough to reveal defect-free slip channels.

Within the low-temperature range changes in mechanical properties are a function of fast neutron fluence and irradiation temperature.[4,19-20] Figure 9 shows the effects of fast neutron fluence on the yield stress and elongation for various irradiation temperatures. Note that the increase in yield stress and reduction in elongation are greatest for irradiation temperatures in the range of 160 to 290°C, but that differences do not develop until the material has received fast neutron fluences of approximately 1×10^{20} neutrons/cm^2. This suggests that the defect clusters grow more complex with increasing neutron fluence. Without further direct evidence one can only state in qualitative terms that the importance of irradiation temperature stems from its influence on the mobility of various defects. At the lowest temperatures vacancy mobility is insufficient to allow the formation of vacancy clusters. This is supported by the observations of Wilsdorf and Kuhlmann-Wilsdorf[21] that no detectable defect clusters formed in type 304 stainless steel irradiated at ambient reactor temperature to 10^{19} neutrons/cm^2 and by the observations of Bloom et al[5] that for irradiation at 93°C the defect clusters were small and showed extremely weak contrast while at 121°C their size and contrast had increased significantly.

The nature of the damage in the low-temperature range is apparently unchanged at very high fast neutron fluences. Cawthorne and Fulton[22] report that for an austenitic stainless steel irradiated to fast neutron fluences of up to 5×10^{22} neutrons/cm^2 at temperatures between 270 and approximately 350°C "black spot" defects are present in the microstructure. On post-irradiation annealing the defects grow into dislocation loops. These loops finally disappear on annealing at about 700°C.

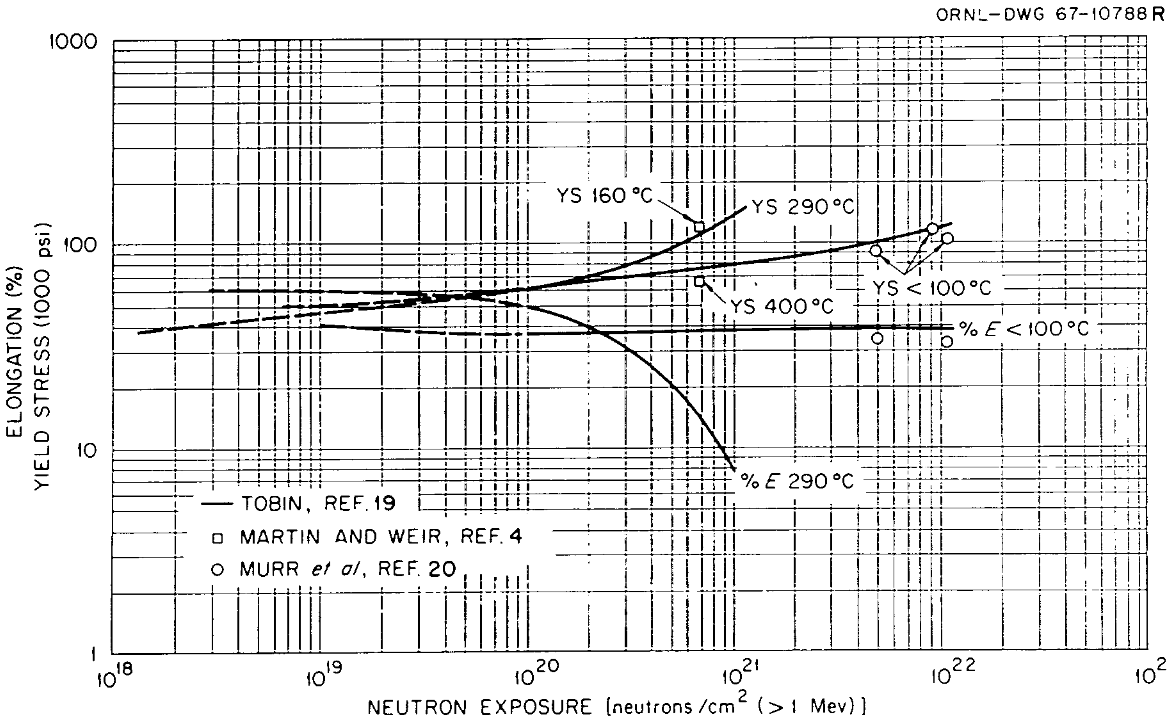


Figure 9

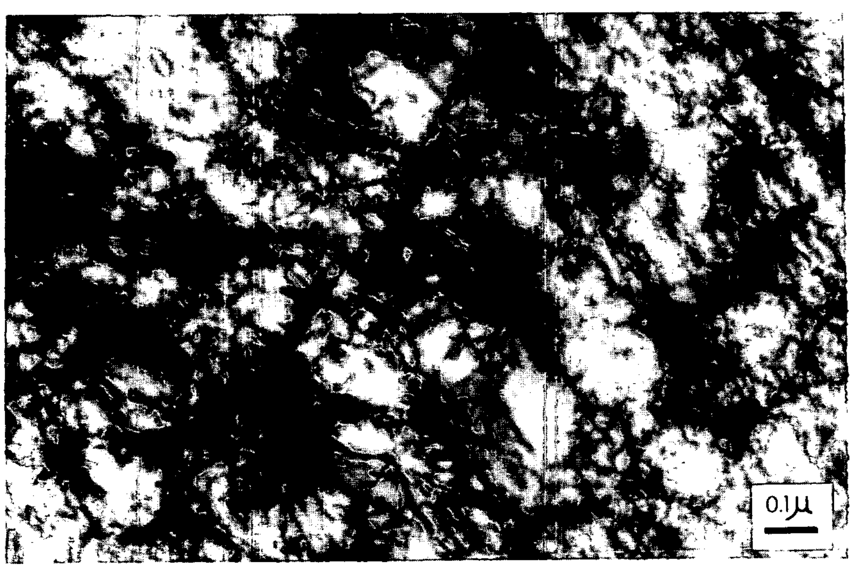


Figure 10

Intermediate Temperatures

Temperatures in the range of approximately 0.40 to 0.55 T_m (380 to 550°C for austenitic stainless steels) are particularly important to the first generation fast breeder reactors. It is also in this temperature range that radiation-damage phenomena are least understood. Two separate effects have been observed. The first involves precipitation and thus will be dependent on the alloy system. The second effect is related to displacement processes and appears to be important at high fast neutron fluences.

As discussed in the previous section, irradiation of type 304 stainless steel at 454°C to 7 x 10^{20} neutrons/cm^2 (E > 1 Mev) resulted in an increase of the room-temperature yield stress from approximately 30,000 to approximately 43,000 psi and small reductions in the fracture stress and strain.(5) Examination of the microstructure of this specimen revealed extensive precipitation, including a heavy layer along grain boundaries. Unlike the defect clusters formed at lower temperatures, such precipitates are not removed by dislocations but rather provide permanent obstacles and sites for tangling. Deformation thus leads to the tangled dislocation configurations shown in Figure 10.

Arkell and Pfeil(23) showed that precipitate structures in a niobium-stabilized stainless steel irradiated at temperatures between 450 and 750°C were significantly different than those present in unirradiated samples with identical thermal histories. Irradiated samples exhibited enhanced precipitation within the grains.

Martin and Weir(4) reported the effects of irradiation temperature on the post-irradiation stress-strain behavior of types 304 and 347 stainless steel irradiated to 7 x 10^{20} neutrons/cm^2 (E > 1 Mev) and 9 x 10^{20} neutrons/cm^2 (thermal). For an irradiation temperature of 400°C an increased yield stress was observed for test temperatures up to approximately 600°C. The strength increase for type 347 stainless steel which contains approximately 1% Nb was significantly larger than that which occurred in type 304 stainless steel (unstabilized). Since niobium is a strong carbide former, it might be postulated that precipitation processes are involved in the hardening mechanisms.

More recently it has been observed(22,24-27) that irradiation of austenitic stainless steels at temperatures between 350 and 600°C to high fast neutron fluences (> 10^{22} neutrons/cm^2) results in large changes in both properties and microstructures. Cawthorne and Fulton(22,25) used transmission electron microscopy to examine the fuel cladding from experimental fuel pins and tensile specimens irradiated in the Dounreay fast reactor to neutron fluences up to 6 x 10^{22} neutrons/cm^2 at temperatures between 270 and 600°C. At irradiation temperatures above approximately 350°C voids which varied in size from the smallest resolvable to approximately 500 A were present. Voids constituted 1 to 2% of the volume of the material and could be eliminated by annealing at 900°C.

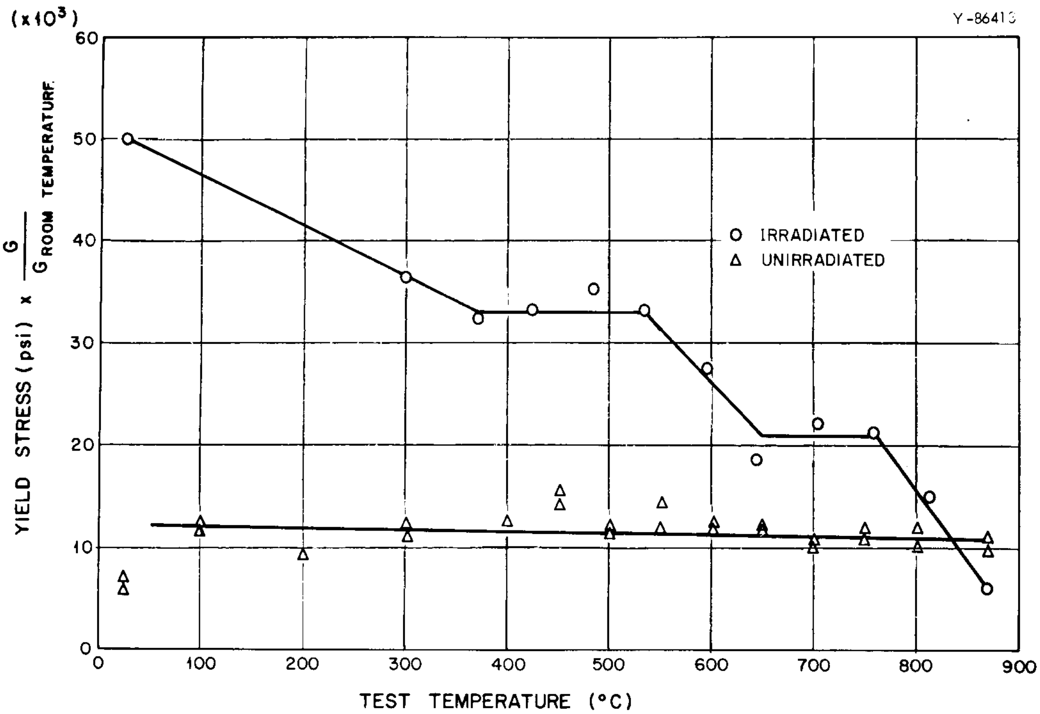
Data obtained by Murphy and Strohm(26) and Holmes et al(24) have demonstrated that this type of damage causes large increases in the yield strength and large reductions in ductility parameters. Holmes et al(24) have correlated the changes in yield strength of type 304 stainless steel irradiated at approximately 530°C to 1.4 x 10^{22} neutrons/cm^2 (E > 0.18 Mev) with the irradiation-produced defect structure. The as-irradiated structure consisted of Frank sessile dislocation loops, about 400 A in diameter and with a density of 3.7×10^{15} loops/cm^3, and polyhedral cavities approximately 150 A in diameter and about 2×10^{14} cavities/cm^3 in number. Figure 11 is a plot of the yield stress (corrected for temperature dependence of the shear modulus) as a function of test temperature. At test temperatures less than 380°C the yield stress shows a thermally activated temperature dependence. The athermal yield stress component is attributed to the strengthening expected from the Frank sessile loops. Above approximately 538°C the sessile Frank loops transform to glissile perfect loops which interact to form a dislocation network upon annealing at 593°C. Above 648°C the cavities or a combination of cavities and dislocation network account for the athermal strength increase that persist to 760°C. Full recovery of the yield strength was observed at 816°C where neither the cavities nor the dislocation network was detected.

Murphy and Strohm[26] have conducted tube burst tests on irradiated EBR-II type 304L stainless steel fuel cladding following irradiation to approximately 1×10^{22} neutrons/cm^2 (fast). Over the length of the cladding tube there is a temperature gradient such that the temperature ranges from 370°C at the bottom to 500°C at the top. In addition, there is a gradient in the neutron flux that ranges from about 1×10^{15} neutrons cm^{-2} sec^{-1} at the top and bottom to 2.5×10^{15} neutrons cm^{-2} sec^{-1} at the midplane. In tests at 500°C the irradiated tubes exhibited a large increase in burst strength and large reduction in ductility as measured by diameter increase at the edge of the fracture. Figure 12 is a plot of ductility as a function of test temperature. Between room temperature and approximately 600°C the ductility is reduced to extremely low values, on the order of 1 to 2%. At 700°C and above there is some recovery of ductility but the values remain much lower than the unirradiated values. Irradiated specimens which were given a pretest anneal at 900°C and then tested at 500°C recovered all the preirradiation ductility and the strength was reduced to that of the unirradiated tubes.

Stiegler et al[27] have examined the type 304L stainless steel cladding from a similar EBR-II fuel element. Two structural features, namely voids and dislocation loops, were present in all specimens. Table 2 lists the approximate irradiation temperatures, neutron fluences, and void densities for each section examined. A comparison of the results for sections 1 and 5 and 2 and 4 indicates that for the conditions examined the void density decreases with increasing irradiation temperature for a constant fluence.

Figure 13 shows a histogram of the void sizes observed in section 3. On the basis of this void size distribution and the number of voids per unit volume listed in Table 2, it was calculated that the cladding density was decreased 0.17% by irradiation. Figure 14 shows examples of voids observed for three different irradiation conditions. It is readily apparent that void size increases with increasing irradiation temperature.

The distribution of the voids was remarkably homogeneous. Variations observed between different micrographs probably reflect differences in foil thickness. It is significant, however, that no voids were present in the grain boundaries. In fact, the void density within about 0.1 μ of the boundary was reduced, probably by annihilation of voids contacting the boundary or the influence of the boundary on the void-formation process.



YIELD STRENGTH OF AISI 304 STAINLESS STEEL AFTER IRRADIATION TO 1.4×10^{22} NEUTRONS/CM² AT TM/2

Figure 11

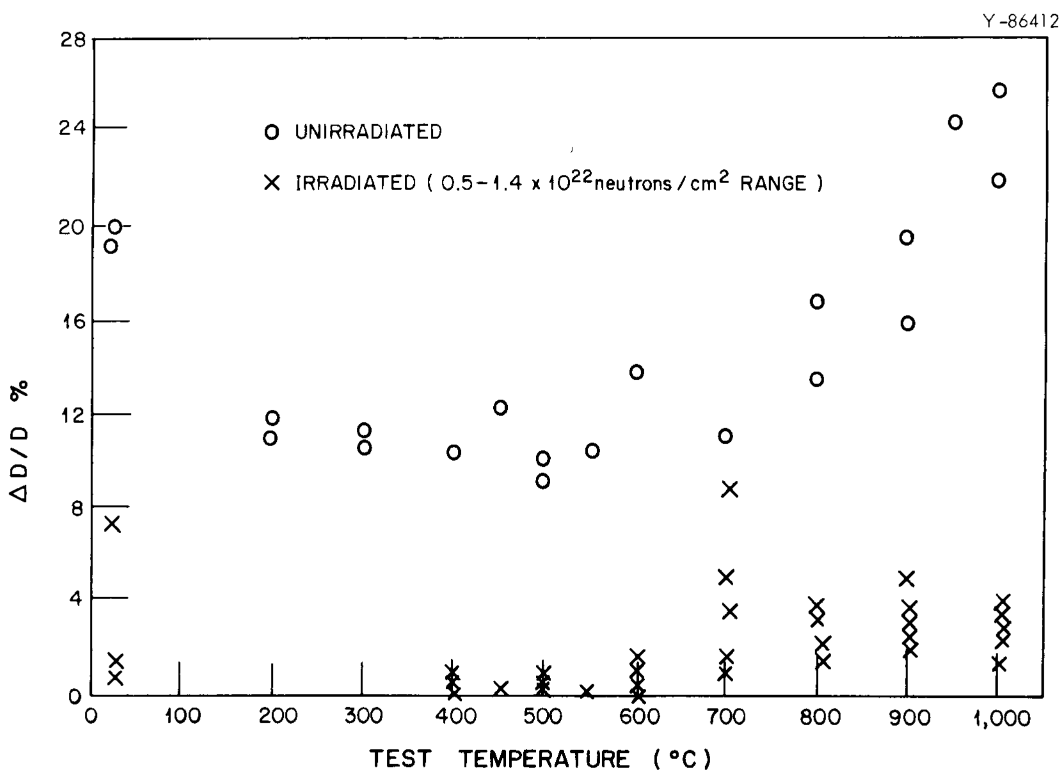


Figure 12

Table 2. Irradiation Conditions and Void Density Measurements for EBR-II Fuel Cladding

Section Number	Irradiation Temperature (°C)	Fast Neutron Fluence (neutrons/cm^2)	Voids per Cubic Centimeter
		$\times 10^{22}$	$\times 10^{15}$
1	370	0.8	1.4
2	398	1.2	1.3
3	438	1.4	1.3
4	465	1.3	0.9
5	472	0.9	0.4

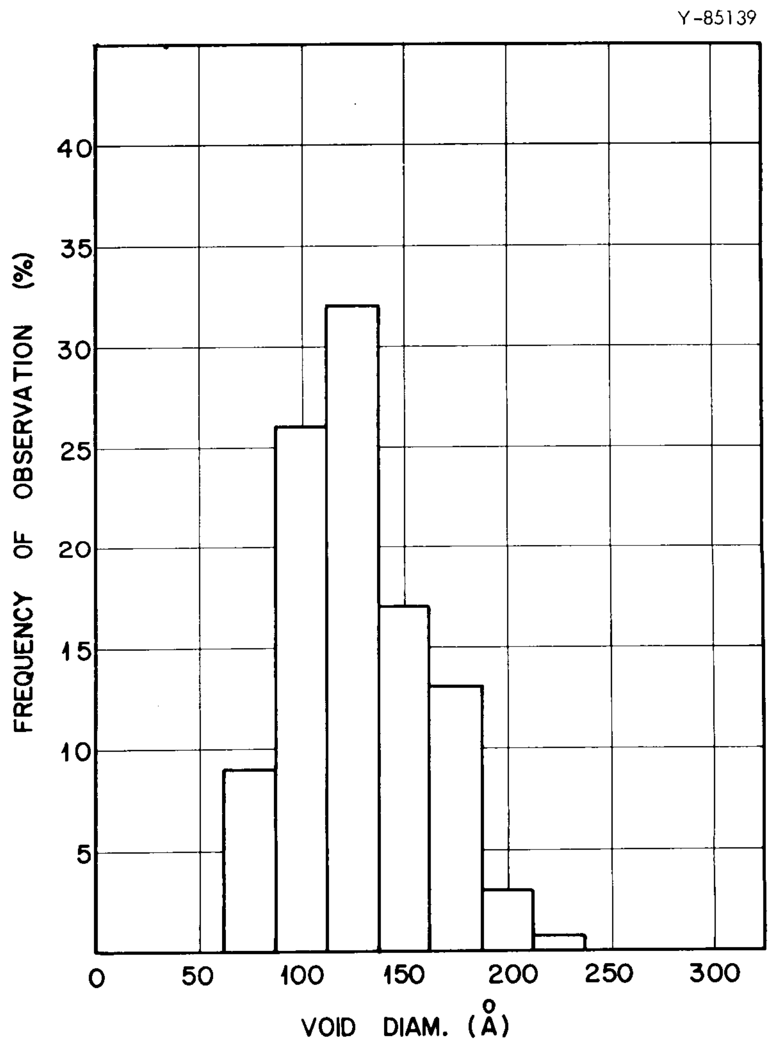

Y-85139
FREQUENCY OF OBSERVATION (%)
40
35
30
25
20
15
10
5
0
50
100
150
200
250
300
VOID DIAM. (Å)

Figure 13

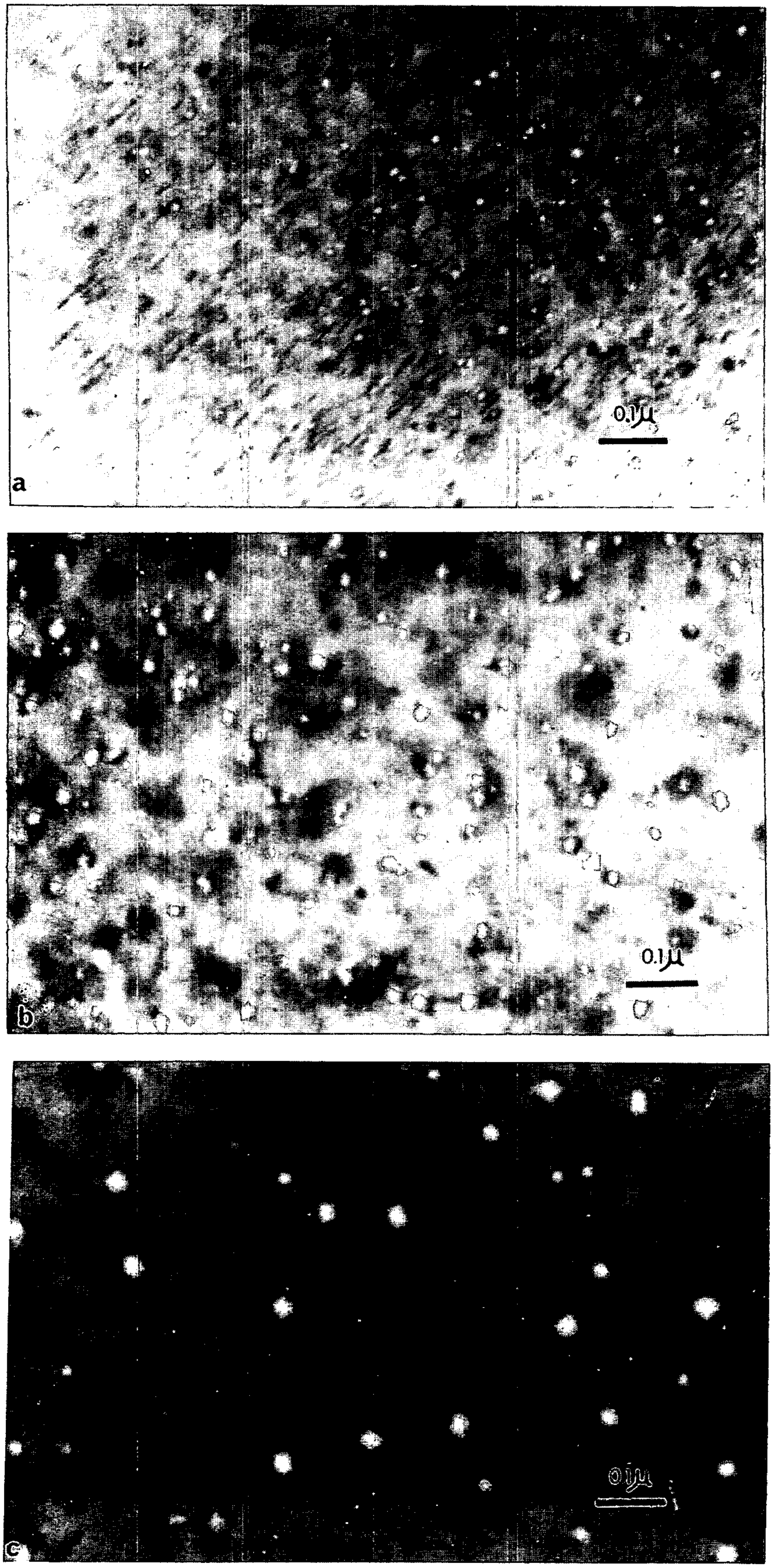


Figure 14

A very complex dislocation substructure was present in each of the five sections. At the lower irradiation temperatures the structure was so complicated that individual loops could not be observed. At 472°C, however, well-defined loops were resolved as shown in Figure 15. These loops lie on {111} and appear faulted, suggesting that they are Frank sessile loops formed by the precipitation of interstitial atoms. The loops ranged in diameter from 200 to 900 A and were present to a density of about $2 \times 10^{15}/cm^3$.

Changes in microstructure as a result of post-irradiation annealing were examined for specimen 3. After 1 hr at 600°C the dislocation loops disappeared and were replaced by a dislocation network. At progressively higher annealing temperatures, the dislocation density decreased. After 1 hr at 900°C the dislocation density was comparable to that of an unirradiated annealed specimen. Concurrent with changes in loop and dislocation structure, the void density decreased. Measurements of void size distribution after annealing indicated that the smaller voids annealed more rapidly. All voids were removed after annealing for 1 hr at 900°C.

These observations allow a qualitative interpretation of the data of Murphy and Strohm.(26) The as-irradiated tubing contained voids and dislocation loops which cause an increase in strength, possibly through the mechanism as discussed by Holmes et al.(24) The recovery of properties at 500°C as a result of post-irradiation annealing at 900°C is a result of the complete recovery of the damage. At 700°C and above the as-irradiated structure recovers very rapidly; thus a partial return of strength and ductility to unirradiated values is observed. It is important to note that ductility is not completely recovered at test temperatures in the range of 700 to 1000°C. The reasons for this will be discussed in the next section.

The formation of voids and dislocation loops as a result of irradiation to high fast neutron fluences not only causes large effects on mechanical properties but also leads to swelling or a decrease in the density of the material. Figure 16 shows the correlation between the density decrease and the fast neutron fluence for austenitic stainless steels irradiated at temperatures between 370 and 560°C. It should be noted that some of these data were obtained by direct density measurements and some by calculations from void density and size measurements. Several of the results were obtained from specimens removed from actual fuel cladding and thus the material was subjected to stress during irradiation and there can be little doubt that this will influence void growth. Interpretation of the data in terms of mechanisms is thus difficult. Figure 16, which is a summary of the available swelling data,(22,25,27-28) does illustrate, however, that for some combination of temperature, stress, and fluences in excess of 10^{23} neutrons/cm^2 volume increases greater than 10% may occur.

High Temperatures

At temperatures above 0.55 to 0.60 T_m, the irradiation-produced vacancies and interstitials are sufficiently mobile to allow continuous recovery of defects during irradiation. It is still observed, however, than when the iron- and nickel-base alloys are irradiated and then tested at these high temperatures, there are severe changes in mechanical properties. These changes are characteristically different from those observed at lower temperatures. In tensile

Figure 15

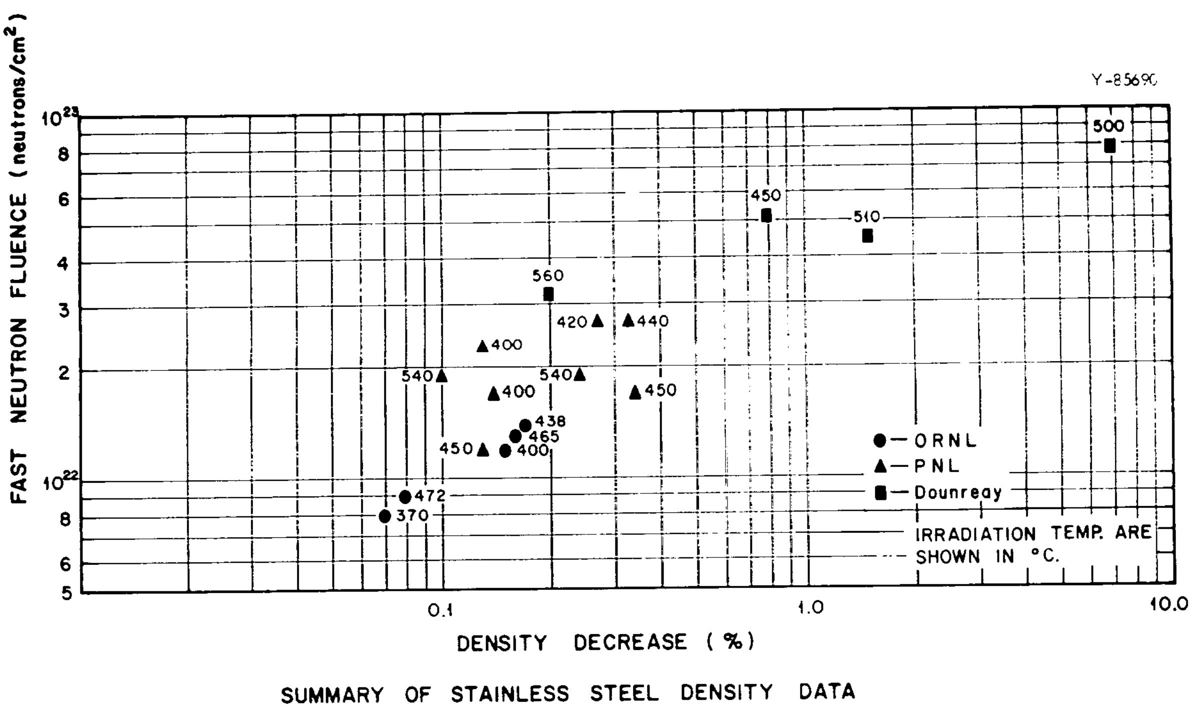


Figure 16

tests the stress necessary to produce a given amount of strain is unchanged; but irradiated specimens fail at a strain much smaller than that at which an unirradiated specimen fails. In creep tests the strain-time relationship is approximately the same for irradiated and unirradiated specimens. Because of the reduced ductility, however, the rupture life is significantly reduced. Examples of the reduction in ductility and rupture life[29] in type 304 stainless steel are shown in Figures 17 and 18, respectively.

There are several important experimental observations which indicate the nature and cause of the damage. Since neither the yield nor ultimate tensile strengths are affected[5,30] and the ductility cannot be recovered by high-temperature post-irradiation annealing,[31] it can be concluded that neither displacement damage nor precipitation reactions are the primary cause. Secondly, the loss of ductility is associated with the grain-boundary fracture process and becomes more severe as the test temperatures is increased and the strain rate decreased. For thermal reactor irradiations the post-irradiation ductility is related to the initial ^{10}B content of the alloy and the thermal neutron fluence.[32,33] Boron-10 has a large cross section (3800 barns) for the $^{10}B(n,\alpha)^{7}Li$ reaction with thermal neutrons - each reaction producing a helium and lithium atom. By cyclotron injection of helium and lithium ions into an austenitic alloy Higgins and Roberts[34] demonstrated that of these two transmutation-produced isotopes, only helium had a large deleterious effect on elevated-temperature ductility.

The most widely accepted model for the loss of elevated-temperature ductility stems primarily from the work of Hyam and Sumner,[35] Rimmer and Cottrell,[36] Cottrell,[37] and Barnes[38] and is summarized as follows. The helium, which is produced from $^{10}B(n,\alpha)^{7}Li$ reactions with thermal neutrons and (n,α) reactions between fast neutrons and most alloy constituents, has a very low solubility in the matrix and precipitates to form bubbles. When a normal stress (σ) is applied to a bubble having an initial radius (γ) larger than a critical radius (r_c) given by

$$r_c = 0.76\ \gamma/\sigma \tag{8}$$

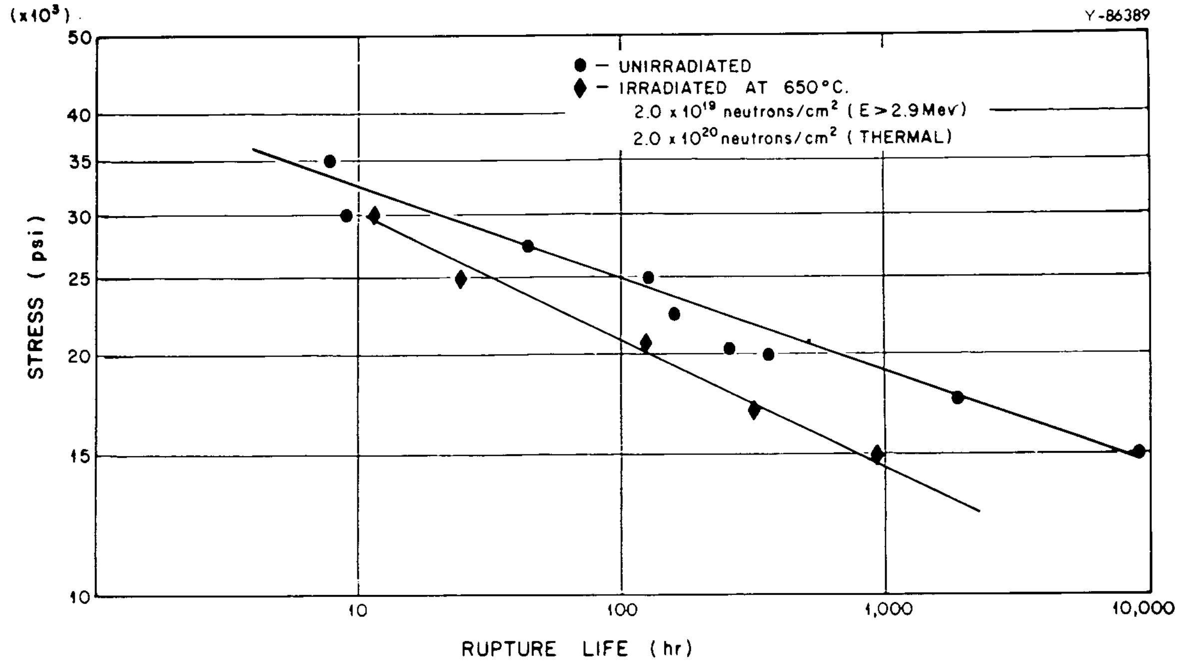
where γ is the surface energy, the bubble will become unstable and expand indefinitely. Those bubbles which are located at grain boundaries can lead to fracture initiation for several reasons:

1. Due to higher grain boundary diffusivities helium is supplied to these bubbles and they can grow much faster than bubbles located in the matrix.

2. As a result of grain boundary sliding, stresses may be concentrated at grain boundary jogs and triple grain junctions; thus, a bubble located in such a region will be subjected to a normal stress several times the applied stress.

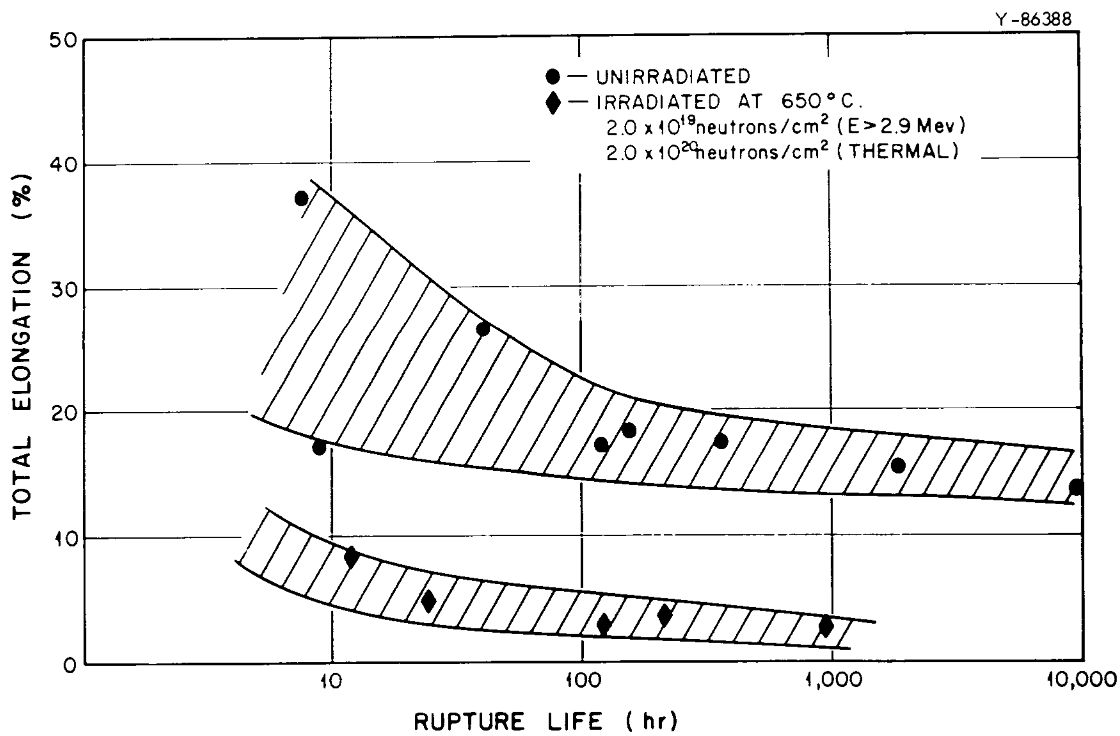
3. Once a grain boundary crack is formed, its rate of propagation may be increased by the presence of grain boundary bubbles.

Figure 19 shows that the elevated-temperature tensile ductility of type 304 stainless steel is a sensitive function of the total helium concentration.[32] These data were obtained from alloys containing various amounts of boron and irradiated to various neutron fluences.



EFFECT OF NEUTRON IRRADIATION ON THE RUPTURE LIFE OF TYPE 304 STAINLESS STEEL AT 650°C

Figure 17



EFFECT OF NEUTRON IRRADIATION ON THE TOTAL ELONGATION AT FRACTURE OF TYPE 304 STAINLESS STEEL AT 650°C

Figure 18

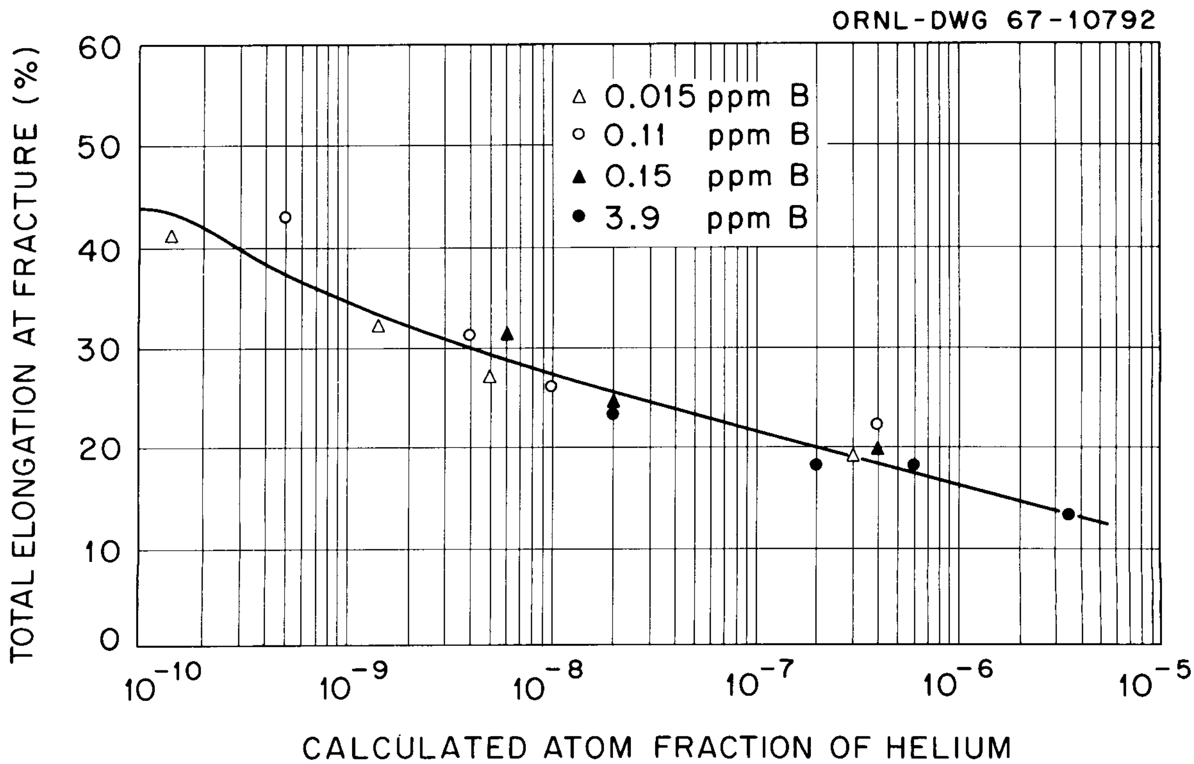

ORNL-DWG 67-10792
TOTAL ELONGATION AT FRACTURE (%)
60
50
40
30
20
10
0
△ 0.015 ppm B
○ 0.11 ppm B
▲ 0.15 ppm B
● 3.9 ppm B
10^{-10}
10^{-9}
10^{-8}
10^{-7}
10^{-6}
10^{-5}
CALCULATED ATOM FRACTION OF HELIUM

Figure 19

Helium bubbles have been observed[39,40] in both the matrix and the grain boundaries after high-temperature irradiation. Figure 20 shows helium bubbles in type 304L stainless steel irradiated at 700°C and containing approximately 35 x 10^{-6} atom fraction helium. Rowcliffe et al[38] have observed the growth of helium bubbles under stress at 750°C as would be predicted by Eq. (8).

For material irradiated in thermal reactors, the distribution of helium bubbles is controlled primarily by the initial boron distribution. Woodford, et al[41] have observed halos of bubbles around precipitate particles in a precipitation-hardening austenitic stainless steel, indicating that boron is contained within these precipitates. In this case there was a reduction in both ductility and creep rate. The reduced creep rate resulted from the pinning of dislocations by bubbles. The same effect could also be responsible for the reduced ductility.

It is important to note that for irradiations conducted in fast reactors in which the thermal flux is essentially zero most of the helium will be produced as a result of (n,α) reactions between fast neutrons and nearly all alloy constituents. Under these conditions the initial helium distribution will be nearly homogeneous. It has been shown by King and Weir[42] and Kramer et al[43] that homogeneous helium distributions produced by injecting α particles into type 304 stainless steel cause reductions in elevated-temperature ductility similar to those observed after irradiation in thermal reactors.

The observations by Murphy and Strohm[26] that even at high test temperatures the ductility of irradiated EBR-II fuel cladding is not recovered suggests that helium is responsible. This is consistent with the fact that strength properties are essentially the same as those of unirradiated tubing.

The elevated-temperature embrittlement problem has been found to be a function of structural and compositional variations. Decreasing the grain size or producing grain boundary precipitates by preirradiation aging treatments give significant improvements in the post-irradiation tensile and creep-rupture ductility of type 304 stainless steel.[30] These effects are believed to be due to the decreased tendency for intergranular fracture as a result of the increased stress necessary to nucleate and propagate grain boundary cracks.

Roberts and Harries[44] found that the post-irradiation tensile ductility of a 20% Cr-20% Ni niobium-stabilized austenitic stainless steel was significantly improved by aging 100 hr at 750°C before irradiation. Again, the results were interpreted in terms of the effects of grain boundary precipitates on the formation of "wedge" type cracks during testing. It was also shown in this investigation that the magnitudes of the post-irradiation ductility in a 18% Cr-10% Ni niobium-stabilized alloy decreased with increasing boron content up to 50 to 70 ppm (weight) and are then partially recovered in alloys containing higher boron contents.

Titanium additions of approximately 0.2 wt % give significant imporvement in the post-irradiation tensile and creep-rupture ductility of types 304 and 304L stainless steel.

Figure 21 compares the post-irradiation creep-rupture ductility of types 304, 304L, and 304L + 0.2% Ti stainless steels at test temperatures of 650 and 700°C. This effect is believed to be a result of (1) the decreased tendency of

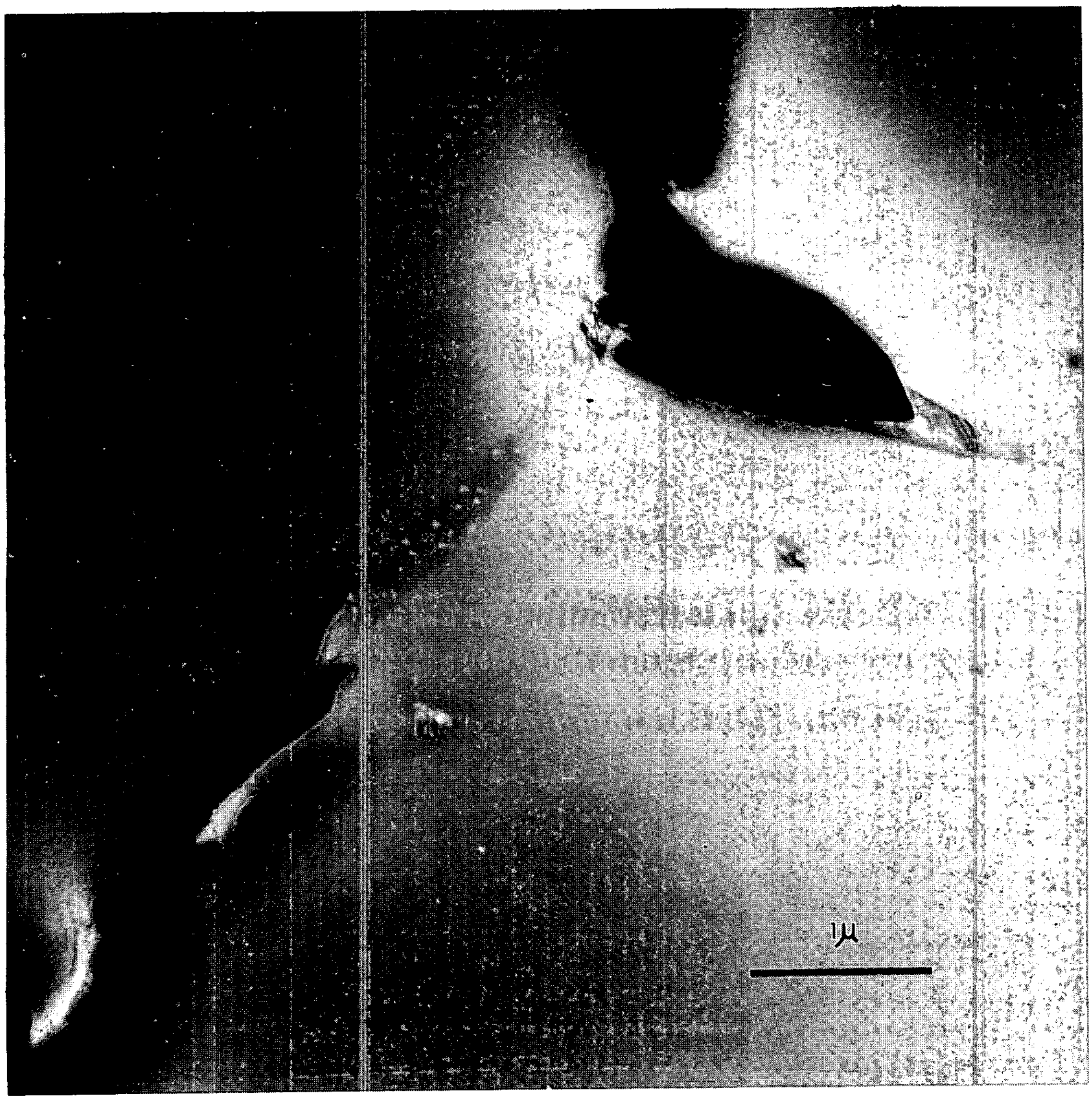
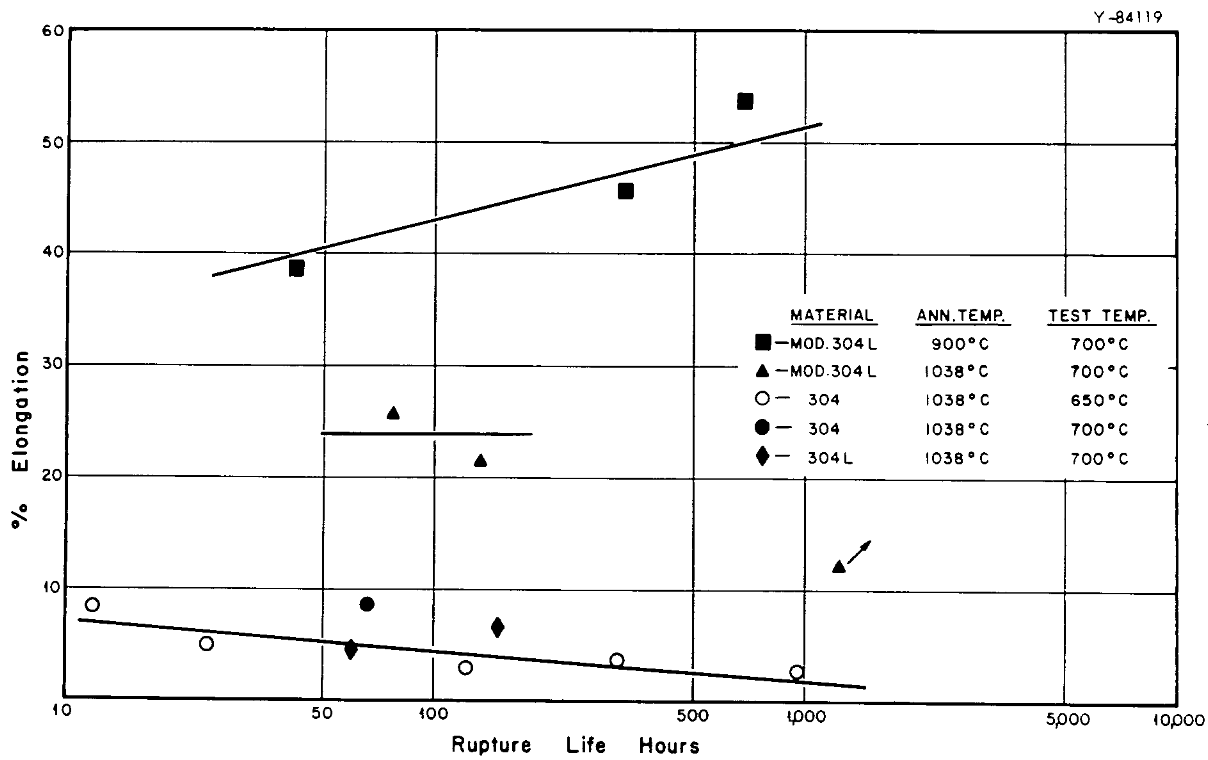


Figure 20



SUMMARY OF POSTIRRADIATION CREEP-RUPTURE PROPERTIES OF TYPE 304, 304L AND MODIFIED 304L STAINLESS STEEL IRRADIATED AT 650°C

Figure 21

the titanium-modified alloy to fracture intergranularly, possibly as a result of the redistribution of elements such as nitrogen, oxygen, etc., (2) the segregation of boron into precipitates, thus reducing the amount of helium produced in the grain boundaries, and (3) a refinement in grain size.

SUMMARY

Materials selected for use as cladding and structural components in a fast reactor system will operate over a wide range of temperature, neutron flux, and stress conditions. The changes in mechanical and physical properties which occur as a result of neutron irradiation are a function of many variables, the most important of which appear to be irradiation temperature and neutron fluence. With regard to the austenitic stainless steels it appears that all known forms of damage may occur. In components which operate at the lower end of the temperature range (below approximately 380°C) the work-hardening coefficients and uniform elongations will be reduced. How severe these effects will be at fast neutron fluences in excess of 10^{22} neutrons/cm^2 is unknown.

Damage may take on several forms in the temperature range 380 to approximately 600°C. Changes in the precipitate distribution and morphology have been observed. The ways in which these changes affect mechanical properties are not entirely understood. Very recently the formation of voids and dislocation loops as a result of irradiation in this temperature range to fast neutron fluences in excess of approximately 10^{22} neutrons/cm^2 has been observed. This damage causes drastic reductions in ductility parameters and a density decrease or swelling of the material. Available data suggest that this form of damage is most severe at temperatures near 550°C and that for fast neutron fluences of 1×10^{23} neutrons/cm^2 density decreases as large as 10% may occur.

At temperatures above 600°C one would expect displacement damage to be unstable and to recover in short times after it is created. Under these conditions changes in strength properties are small. Reductions in ductility which become more severe at higher temperature and lower strain rates are, however, observed. These effects are a result of helium which is produced by various (n,α) transmutation reactions during irradiation.

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LIST OF FIGURES

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Figure 13- (Y-95139) Void Size Distributions in EBR-II Cladding Irradiated at 438°C to 1.4×10^{22} neutrons/cm^2.

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(b) 1.4×10^{22} neutrons/cm^2 at 438°C, 1.3×10^{15} voids/cm^3.
(c) 0.9×10^{22} neutrons/cm^2 at 472°C, 0.4×10^{15} voids/cm^3.

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Figure 18- (Y-96388) Effect of Neutron Irradiation on the Total Elongation at Fracture of Type 304 Stainless Steel at 650°C. (Ref. E.E. Bloom, "In-Reactor and Post-irradiation Creep-Rupture Properties of Type 304 Stainless Steel at 650°C," Oak Ridge National Laboratory, ORNL-TM-2130 (March, 1968).)

Figure 19- (ORNL DWG 67-10792) The Elongation at Fracture of Type 304 Stainless Steel Containing Various Amounts of Boron and Exposed to Four Radiation Doses, Ranging from 1×10^{18} to 5×10^{20} neutrons/cm^2. The elongation is shown to be a function of total concentration of helium produced by both high-energy and thermal neutrons. The specimens were tested at 700°C after irradiation at 50°C in the Oak Ridge Research Reactor. Boron concentrations (in parts per million); open triangle, 0.015; open circle, 0.11; solid triangle, 0.15; solid circle, 3.9.

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DISCUSSION

J.F. Schumar - Are we going to be able to qualify stainless steels for large fast breeder reactors?

J.R. Weir - According to the Liquid Metal Program Office, that is about a $10 million question.

E.C. Kovacic (APDA) - My questions are with regard to the swelling of the stainless steel. Could you comment on what you think the effect of the flux level might be as opposed to the fluence and at what flux level were the data obtained that you have listed there?

J.R. Weir - All those data were obtained in either EBR-II or Dounreay and the fast flux of those reactors was between 2 and 3×10^{15}. The part of your question concerning whether it is flux sensitive, we can't really answer. My guess is that it is not. That is a guess. The basis for the guess is a theory by Joe Beeler. His computer theory, according to Joe, actually predicted the voids before they were observed experimentally. And according to this model, it is probably not very flux sensitive; i.e., all that it requires is a high energy neutron and given enough of them, you will end up with voids that are large enough to see in the electron microscope.

E.C. Kovacic - My second question is in regard to the implication of this swelling on the use of stainless steel as a fast reactor clad. If I remember the data correctly, it indicated about 8 v/o increase at a fluence of $\sim 8 \times 10^{22}$. This seems to be more than the maximum volume increase that you could accommodate in a fast reactor core design and yet those fluence levels seem quite low to me in terms of the kind of burnup we have to obtain for economic fuel performance. Would you comment on that? What is the implication of this on the use of clad in fast reactor fuels?

J.R. Weir - Well, I think that the first real assessment of this problem is occurring right now in the FFTF project, and I haven't talked to the design people there. It is amazing to metallurgists how much trouble we can give a designer and he can design his way out of it, sometimes. This might be the case with the volume increase in the fast reactors, although you are limited by the properties of the c oolant and the geometry of the core from the safety standpoint. We can't really say what affect this will have unless a large group of people take a serious look at it in a reactor design. As far as I know, that is going on right now only in the FFTF. Perhaps there is some work like this in England where they are looking at this effect.

D.H. Gurinsky (BNL) - Jim, I would appreciate your commenting on the results you reported in a trip report on the single crystal work on nickel which I think is a fair substantiation of this helium embrittlement phenomenon.

J.R. Weir - Well, I have written lots of trip reports and I am not sure just which one you are referring to. The helium embrittlement occurs in a wide range of materials. It occurs in pure nickel if it is polycrystalline. It does not occur in single crystal nickel (that is French work). It occurs in pure iron, it occurs in pure copper, and it occurs only in the temperature range in which the material likes to fracture intergranularly. So, anytime the material tends to fracture in the grain boundaries anyway, if you introduce the void nuclei or bubbles in the grain boundaries, it induces fracture at much, much lower strain and, therefore, producing the ductility effect. There is lots of evidence for the helium effect.

E

RADIATION EFFECTS TO REFRACTORY METALS AND ALLOYS*

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ABSTRACT

A general review is made of the effects of neutron irradiation on the elevated temperature mechanical properties of refractory metals and alloys. Particular attention is paid to the importance of the irradiation and test temperature and to the resulting sub-structure on the observed changes in the mechanical properties.

INTRODUCTION

This presentation will be centered around one important simple equation which is as follows:

$$\dot{\varepsilon} = bv\rho \qquad (1)$$

This states that the creep rate, $\dot{\varepsilon}$, is equal to the Burgers vector b, mobile dislocation density ρ, and average dislocation velocity v. In many experiments on mechanical property determinations, the creep rate is determined as a function of applied stress. Or for a constant stress, the creep rate is determined as a function of temperature. It is very difficult, therefore, to properly interpret the experimental data in greater detail when the creep rate is known to be a function of two variables, the mobile dislocation density (ρ) and the average dislocation velocity (v), without some knowledge of the dependence of these two variables on stress and temperature.

The irradiation induced defects which will affect dislocation mobility (v) and dislocation multiplication (i.e. dislocation density ρ) are not very different than the lattice disorder or second phases that physical metallurgists are quite familiar with in precipitation hardening alloys, in cold worked materials, and in those materials which are rapidly quenched and aged. For instance, it is found that a quenched metal results in a super-saturation of vacancies which, at the proper aging temperature, tend to form cavities and voids. As a general

*This paper originated from work sponsored by the Fuels and Materials Branch, U.S. Atomic Energy Commission, under Contract AT(40-1)-2847.

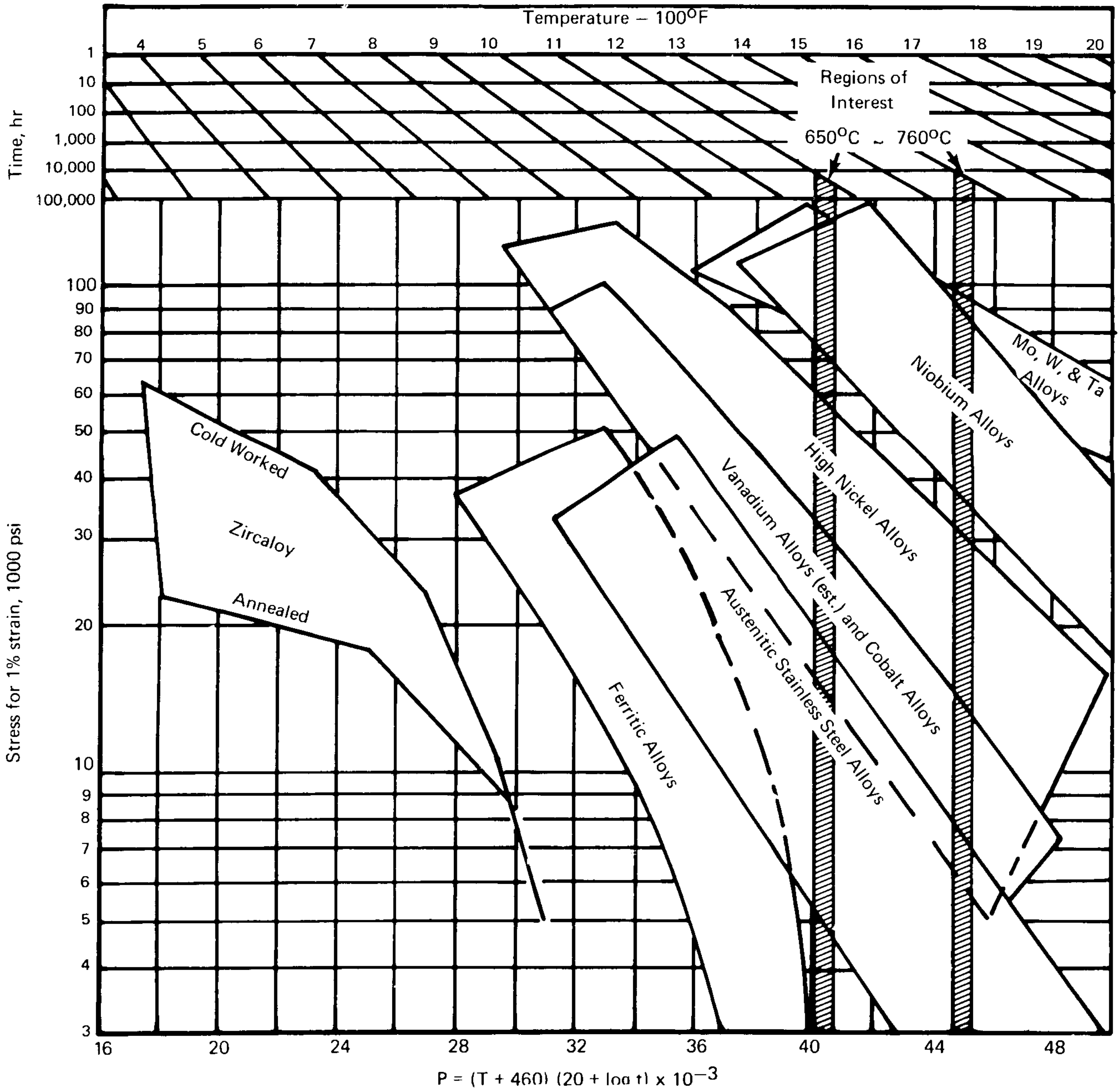

Figure 1 - Larson-Miller comparison of alloy systems based on approximate stress to produce 1% strain. Shaded regions bracket the cladding temperature range which may be of interest in advanced LMFBR designs.

TABLE I

EFFECTIVE ABSORPTION CROSS SECTIONS FOR LMFBR FUEL-CLADDING MATERIALS

Soft Spectrum Mean Fission Energy ∿ 120 kev General Electric Oxide-Fueled Reactor*		Hard Spectrum Mean Fission Energy ∿ 305 kev Combustion Engineering Carbide-Fueled Reactor**	
$10^3 \Sigma a$, cm^{-1}	Material	$10^3 \Sigma a$, cm^{-1}	Material
1.16	V	0.82	V
1.45	Zr	0.97	Zr
1.60	V-20Ti	1.03	V-20Ti
1.78	V-15Ti-7.5Cr	1.14	V-15Ti-7.5Cr
5.30	Fe-15Cr-4Al-1Y	2.66	UMCO-50
6.40	304 SS	3.21	Fe-15Cr-4Al-1Y
6.54	347 & 348 SS	3.74	Haynes #25
6.56	19-9DL	3.86	304 SS
6.61	316 SS	3.97	347 & 348 SS
6.74	Incoloy 800	3.97	19-9DL
7.64	Inco X-750	3.98	316 SS
7.64	Hastelloy R-235	4.18	Incoloy 800
7.91	Hastelloy X	4.75	Hastelloy-X
8.15	UMCO-50	4.78	Hastelloy R-235
8.52	Inconel 625	4.86	Inco X-750
10.6	Haynes #25	5.30	Inconel 625
13.6	Nb-1Zr	8.66	Mo-0.5Ti
14.0	Cb-752	8.68	Mo
16.5	Mo-0.5Ti	9.46	Nb-1Zr
16.6	Mo	9.56	Cb-752
31.1	W	17.4	W
61.6	W-25Re-30Mo	29.6	W-25Re-30Mo
65.4	W-25Re	31.8	Mo-50Re
69.3	Mo-50Re	32.0	W-25Re
91.3	Ta	41.5	Ta
174.0	Re	77.8	Re

*"Liquid Metal Fast Breeder Reactor Design Study," GEAP-4418, General Electric Company, January 1964.

**"Liquid Metal Fast Breeder Reactor Design Study," CEND-200 Combustion Engineering Company, January 1964.

rule, most of these processes lead to an increase in the strength of the material and a corresponding decrease in ductility. The radiation induced defects, resembling combinations of quenched, worked and precipitation hardened material, will affect both dislocation velocity and also dislocation multiplication leading to changes in the creep rate. In some cases, and especially at certain temperatures, it is found that the creep rate is accelerated when compared to an unirradiated specimen; in most instances, however, it is found that the creep rate is reduced.

To establish a proper perspective regarding the strength of the refractory metals and alloys compared to other structural metals, Figure 1 shows the stress for 1% strain as a function of a temperature-time parameter.(1) In this case the Larson-Miller parameter was selected. Zircaloy, which reactor designers are quite familiar with, is quite low in strength. The austenitic stainless steels, niobium base alloys, molybdenum, tungsten and tantalum alloys are indicated as well as some of the vanadium alloys and cobalt base alloys. The shaded region represents a temperature region of 650°C and 760°C which is presently of primary interest to LMFBR designers.

The calculated effective absorption cross section(2) for LMFBR fuel clad materials in two different spectra are listed in Table 1. The vanadium base alloys seem to be the most attractive from the reactor physics standpoint. Tungsten is a very poor material for an economic breeder, but it is used in other fast reactor concepts. This ordering of material, of course, depends on the assumptions made in the calculations. In any event, vanadium base alloys appear to be the most attractive, but unfortunately, the least amount of information is known about these alloys in the areas of radiation effects.

In this presentation, it is hoped to develop an understanding of the general effects of radiation on the BCC metals as a class of alloys and much of this knowledge will be based on tungsten of which, it is believed, most of the useful information is known. Next is to show that this understanding also applies to the molybdenum alloys and finally suggest that the vanadium alloys should - to a first approximation, behave in a similar manner.

POINT DEFECTS

Resistivity measurements(3,4) are generally used to obtain information on point defect migration behavior. The resistivity is measured at some fixed temperature following elevated temperature anneals for some constant time; i.e., isochronal annealing. The defects are introduced through working, quenching, aging, irradiating and/or combinations of these processes. As an example, for the case of recrystallized tungsten irradiated at about 70°C to neutron fluences ranging up to 1.5×10^{21} n cm^{-2} (E > 1 Mev), it is shown in Figure 2a that the point defects are removed according to a fixed pattern regardless of the degree of irradiation. Another important observation is that for anneal temperatures above 1000°C and up to 1900°C, a residual resistivity remains and this is attributed to transmutations of tungsten into rhenium and osmium atoms.

By determining the slope of the isochronal recovery curves $[d(\Delta\rho/\Delta\rho_o)/dT]$ information on the rate of defect recovery may be obtained. Figure 2b shows this defect recovery spectra which clearly suggests that unique temperature

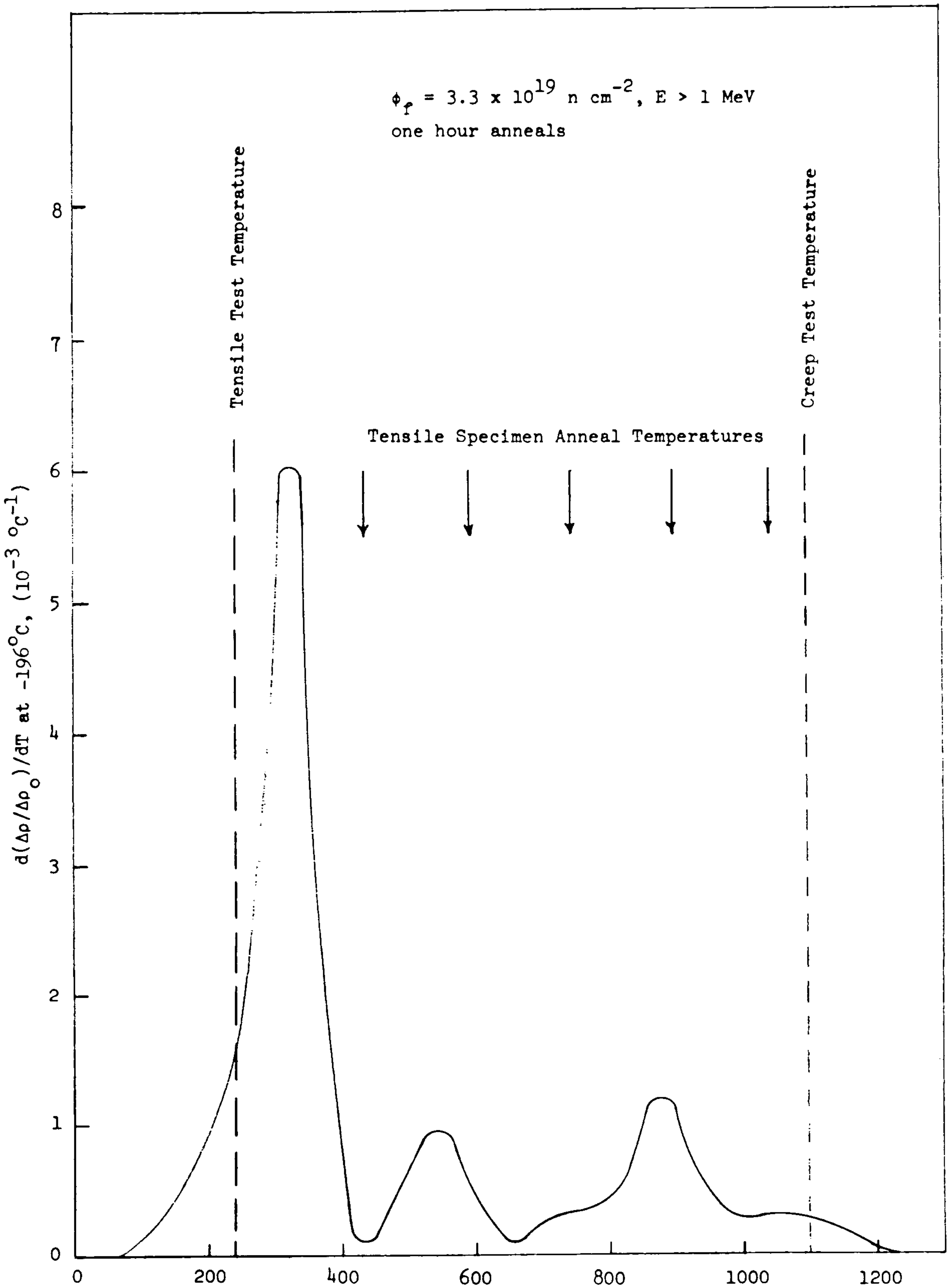

Figure 2 - Derivative of the normalized isochronal resistivity recovery of neutron irradiated recrystallized tungsten as a function of annealing temperature. The three prominent recovery peaks represent the 0.15, 0.22 and 0.31 T_m point defect recovery stages.

regions exist and these are attributed to the migration of different species of point defects. In particular, prominent temperature regions occur at about 0.15 T_m, 0.22 T_m, and 0.31 T_m where the first and last values are generally referred to as Stage III and Stage IV recovery regions, respectively.

Although the nature of these defect stages are important in the evaluation of experimental data and especially to the application of this information to reactor design, the point being made in this paper is that unique recovery temperatures do exist in these irradiated materials. Similar stages have been observed to occur at the same homologous temperatures in molybdenum and other bcc metals. For the present time, assume that free interstitials and/or impurity interstitials migrate in Stage III and that free vacancies migrate in Stage IV. The region at 0.22 T_m may be due to the migration of divacancies or to the release of impurity atoms or free interstitials from traps.

CLUSTER DEFECTS

As a result of the point defect migration processes, there is a tendency for some of these to accumulate into planar clusters which may collapse into vacancy or interstitial dislocation loops. Some of the point defects may migrate to other free surfaces such as grain boundaries or to the existing dislocation structures already in the material as well as to those cluster defects due to the relaxation of the displacement spikes. Under proper temperature and flux density conditions it is also possible for the vacancies to form polyhedral and/or spherical voids. Figure 3 shows the general features of the vacancy or interstitial loops (two dimensional) as well as a typical damage region (three dimensional) due to the displacement spike.[5]

Cluster defects affect the mechanical properties in a manner similar to a precipitation hardening alloy. Glissile dislocations interact with the clusters and must overcome these obstacles by bowing out between them, by cutting through or by climbing over. The climb mechanism becomes possible, as a general rule, at temperatures above one-half the absolute melting temperature due to the role which vacancies play in the dislocation climb processes. The vacancy concentration in thermal equilibrium become appreciable at the higher temperatures. There is, however, a possible influence of the radiation induced vacancies, which tend to give a local super-saturation of vacancies at temperatures below 0.5 T_m, in promoting dislocation climb at the lower temperatures; i.e., in the region of 0.31 T_m when they become mobile and therefore may interact with dislocations over a longer range in the lattice.

Figure 4 shows typical defect clusters observed in tungsten with the transmission electron microscope. Dislocation loops which, depending on irradiation and testing temperatures, are interstitial or vacancy in nature have been identified.[6,7] The density of these defect clusters will vary with irradiation fluence[8] and also with annealing (and/or testing) temperature.[9] The temperature dependence suggests an aging phenomena and these densities have actually been determined for the case of tungsten as shown in Figure 5. These loops tend to harden the material and the degree of hardening will depend on their size (d) and density (N) which, in turn, determines their dispersion (L) according to $L = (Nd)^{-1/2}$. Dislocation theory[10,11] suggest that the increase in shear strength (Δt) may be related by

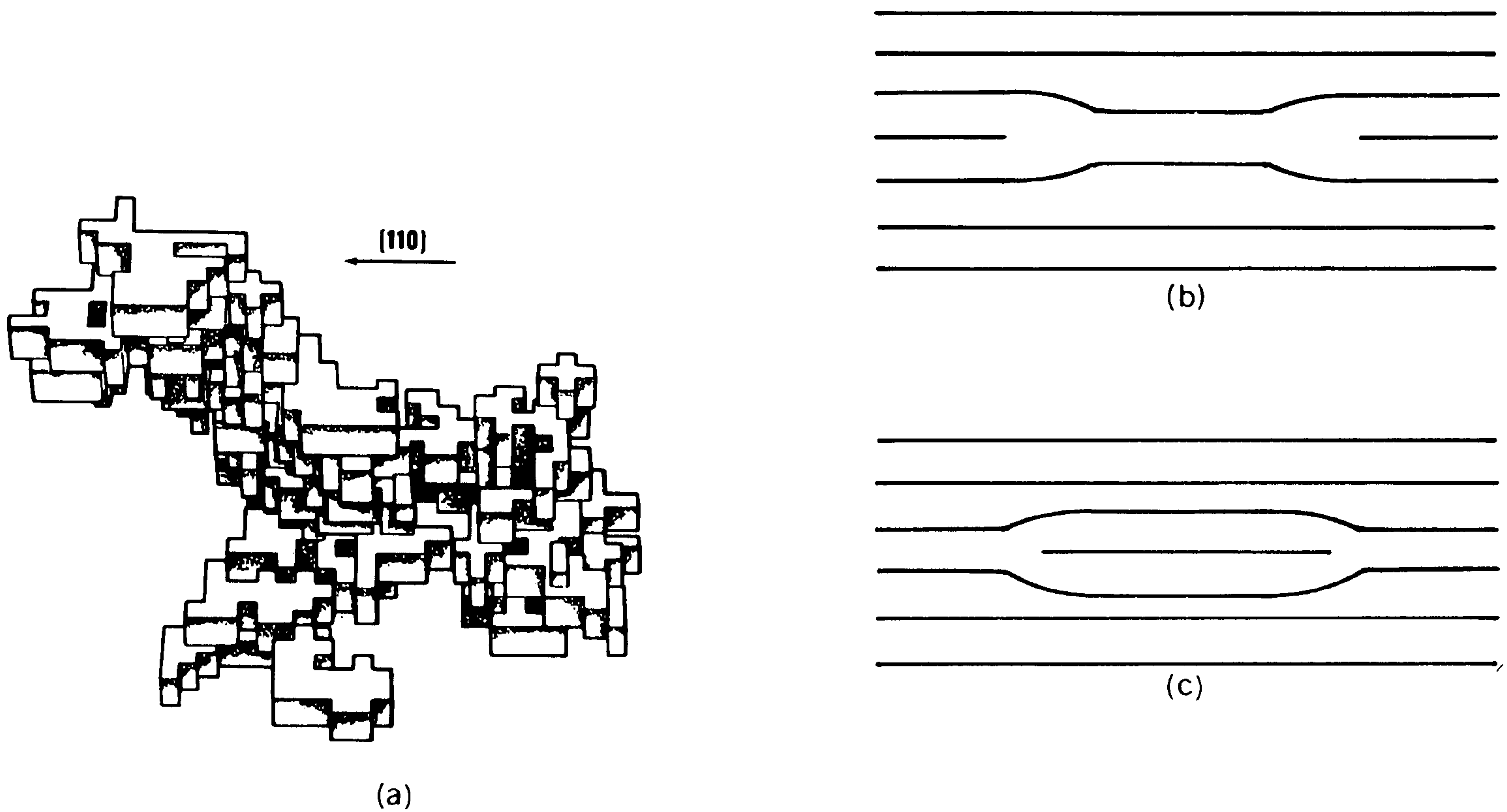

Figure 3 - Schematic drawing showing (a) a multiply connected collided-atom volume for a 5 keV spike in α-iron. The swiss cheese structure and <110> orientation are clearly illustrated in this figure (5). On high temperature anneals this configuration will progressively relax into a more compact three-dimensional vacancy cluster and/or into planar clusters such as (b) vacancy or (c) interstitial loops.

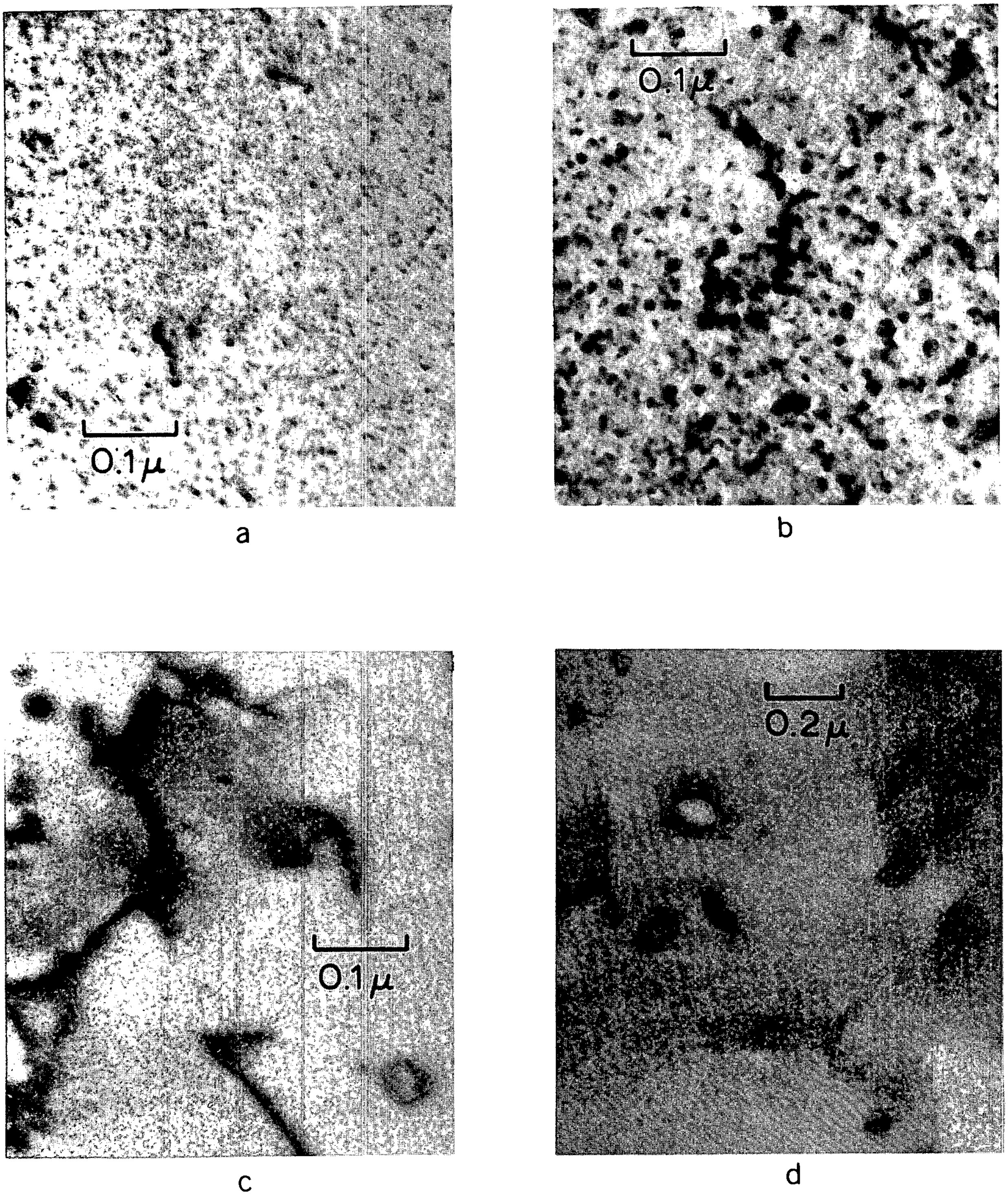

Figure 4 - Transmission electron micrographs of neutron irradiated polycrystalline tungsten. Irradiated to 4.2 x 10^{19} n cm^{-2}, E > 1 MeV and annealed for one hour at (a) 240°C, (b) 743 °C and (c) 1043 °C. Irradiated to 1.5 x 10^{21} n cm^{-2}, E > 1 MeV and annealed for about 300 hours at (d) approximately 1100°C.

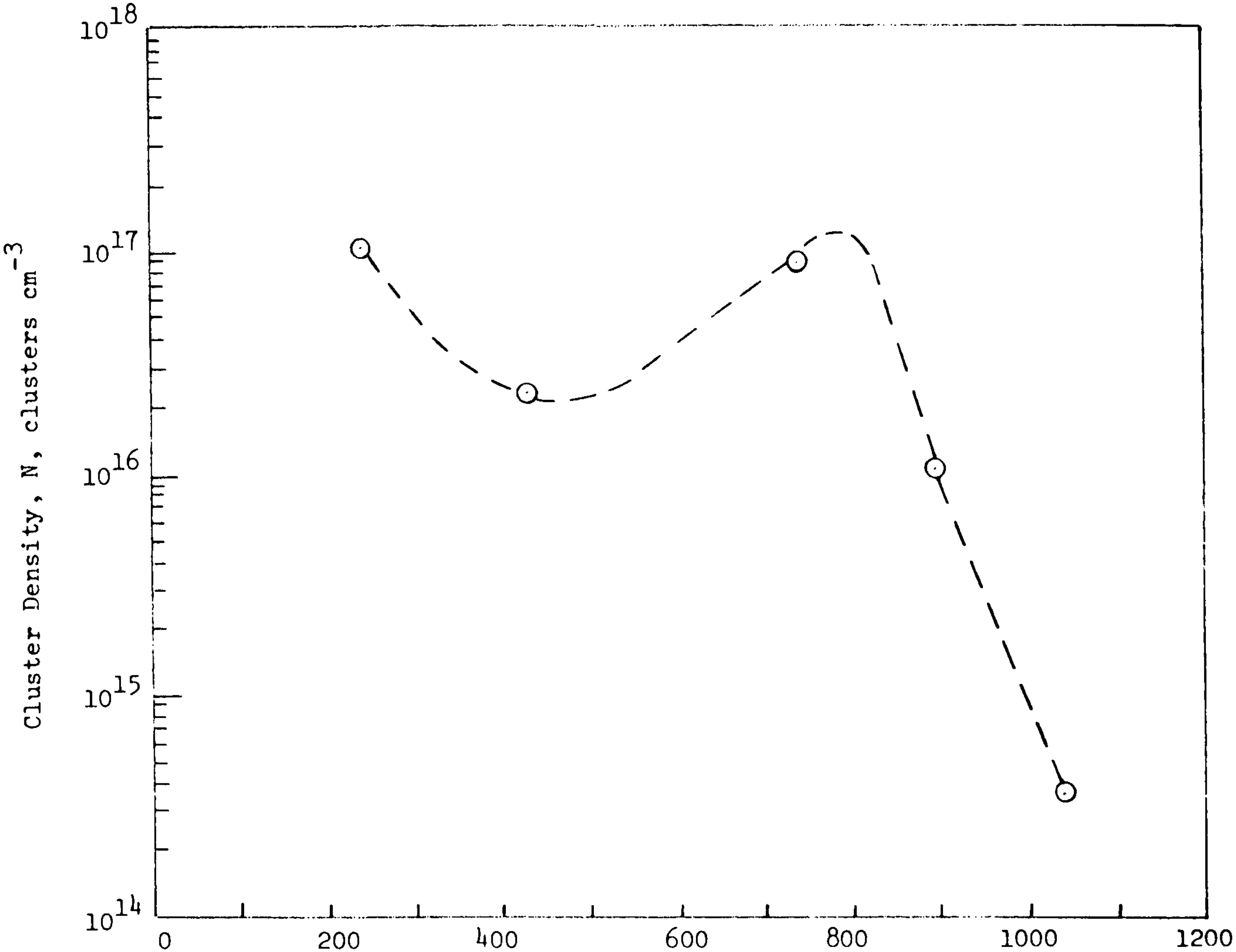

Figure 5 - Defect cluster density of neutron irradiated (4.2 x 10^{19} n cm^{-2}, E > 1 MeV) polycrystalline tungsten as a function of annealing temperature. The radiation induced hardening is almost completely eliminated following the 1043°C anneal where the cluster density was reduced by about a factor of 400 from that observed at the peak.

$$\Delta t = \alpha \frac{\mu b}{L} \qquad (2)$$

where μ and b are the shear modules and Burger's vector, respectively. The constant α represents an interaction parameter of the dislocation and obstacle and was found to be approximately 1/5 in reasonable agreement with theoretical computer calculations (1/4) for the fcc metals.[11] The point being made that, to a first approximation, a correlation may be made between the microscopic observation (micro-metallurgy) of defects in the metal to the mechanical properties (macro-metallurgy) measure with tensile test equipment.

TRANSMUTATION PRODUCTS

At higher temperatures, the point defects are essentially completely removed as are also most of the defect clusters which have been formed directly or through the aggregation of point defects. Although these irradiation induced defects have been removed, it is observed (Figure 6) that the creep rate of tungsten, when tested at a constant applied stress and temperature, is reduced by a factor of 600 with a correlated increase in the rupture life. This large change in strength is attributed to the presence of transmutation products according to solid solution strengthening theories.

As shown in Figure 4, a few defect clusters may still remain at the 1100°C (0.37 T_m) test temperature. The cluster defect densities, however, are several orders of magnitude lower than those observed for the tensile tests and do not account for the observed increase in creep strength. The loops are completely removed at temperatures approaching 0.5 T_m unless they are stabilized by precipitates formed from the presence of impurity atoms in the material. These precipi- have been observed to decorate the loops as bends in a necklace.

The smooth curve drawn in Figure 6 shows the excellent agreement between theory and experiment. By assuming the creep relationship

$$\dot{\varepsilon} = A' f(\sigma) e^{-\Delta H_c/RT} \qquad (3)$$

with A' being a constant which includes the usual pre-exponential factors, ΔH_c the effective activation energy for creep, R and T, the gas constant and absolute test temperature, respectively. The stress function has many forms which may be proportional to the Hyperbolic Sine, a power law (σ^n) or an exponential law ($e^{B\sigma}$). For the range of plastic flow stresses used in this experiment the power law may be used. The flow stress σ is defined as

$$\sigma = \sigma_a - \sigma_i. \qquad (4)$$

Where σ_a is the initial applied stress, and σ_i the lattice frictional stress due to the presence of long range internal stress fields. Therefore, for the case of a fixed applied stress and temperature, Equation 3 may be written as

$$\dot{\varepsilon} = A(\sigma_a - \sigma_i)^n \qquad (5)$$

In the case of the unirradiated recrystallized specimens, and for these test temperatures, the value of $\sigma_i << \sigma_a$ and the creep rate may be expressed as a function of σ_a^n where both constants of Equation 5 (A and n) may be determined. For the case of the irradiated specimens, it is shown that rhenium atoms are formed which in turn increases the value of σ_i according (for dilute alloys) to the relationship

$$\sigma_i \equiv \sigma_{Re} = k'(C_{Re})^{1/2} \tag{6}$$

where C_{Re} is the atom fraction of rhenium atoms and k' a constant converting this relationship into the appropriate stress units. The expression [Equation (6)] may also be written as

$$\sigma_{Re} = k\ (\phi_{th})^{1/2} \tag{7}$$

since the rhenium atom concentration is essentially directly proportional to the thermal neutron fluence.

Combining Equations (5) and (7) [or Equation (6)], a general expression

$$\dot{\varepsilon} = A[\sigma_a - k\ (\phi_{th})^{1/2}]^n \tag{8}$$

may be obtained. Conversely the applied stress required to maintain a constant creep-rate at a fixed temperature may be related to the thermal neutron fluence (ϕ_{th}) as follows:

$$\sigma_a = (\dot{\varepsilon}/A)^{1/n} + k(\phi_{th})^{1/2} \tag{9}$$

or

$$\sigma_a = C + k(\phi_{th})^{1/2} \tag{10}$$

which suggests that the increase in strength is proportional to the square root of the thermal neutron fluence, provided the data is evaluated within those limits where A and n are constant. The constant k will vary slightly with temperature for values $0.35 < T < 0.5\ T_m$. Typical of most solution strengthened materials, the influence of rhenium at temperatures above $0.5\ T_m$ will not be very significant.

For the case of tungsten, it is now shown that strengthening mechanisms due to observable defect clusters and due to transmutation products can be adequately explained by existing dislocation theories and that these mathematical relationships may be used as guide lines, for a first approximation analysis, in the design of components for conditions at which no experimental engineering mechanical property data exists.

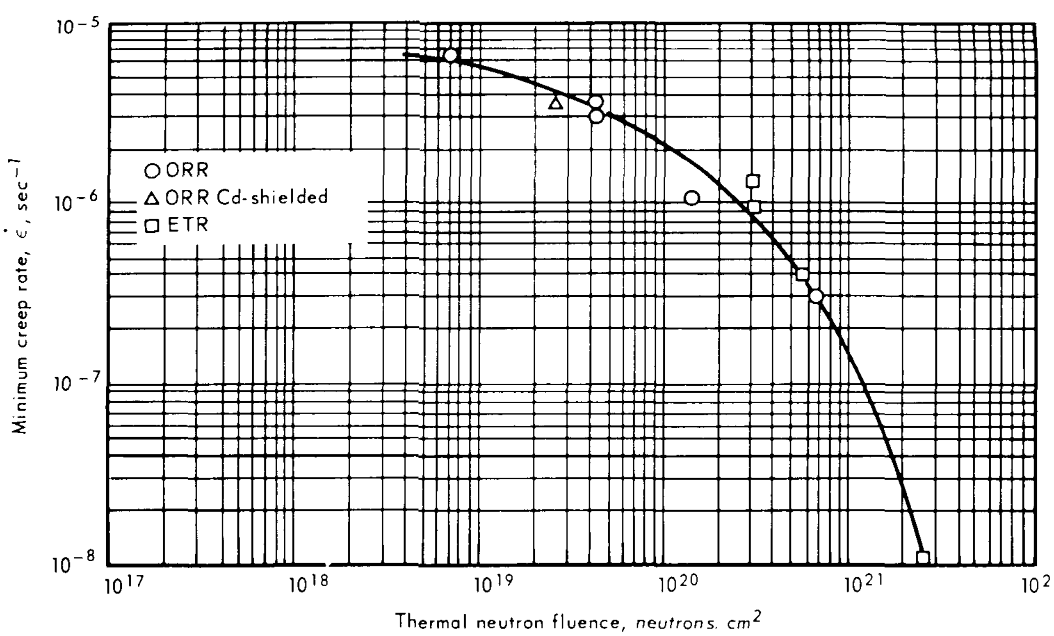

Figure 6 - Minimum creep-rate of irradiated polycrystalline tungsten tested at a fixed temperature of 1100°C and an applied stress of 18.28 kg mm^{-2} as a function of thermal neutron fluence. The maximum fluence yielded about 2.7 atom percent rhenium atoms.

RADIATION ENHANCED PRECIPITATION

Specimens irradiated at elevated temperatures, and in particular for $0.31 < T < 0.50\ T_m$ which is above and below the critical temperature $0.35\ T_m$, show that some radiation induced defects are still present and these tend to strengthen the material above and beyond that observed for specimens irradiated to the same fast (and thermal) neutron fluence at reactor ambient temperatures. This phenomenon cannot be correlated with observable cluster defects and/or transmutation products in the manner discussed previously (i.e. tensile properties at 240°C or creep-properties at 1100°C).

As an example, Figure 7 shows a typical family of creep curves for molybdenum irradiated to a fast neutron fluence of 1.4 to 1.8×10^{20} n cm^{-2} (E > 1 Mev). The increase in the rupture life compared to that of the unirradiated specimen for the constant load and fixed temperature tests are factors of 2.7, 12 and 17 for specimens irradiated at 70, 700, and 1000°C, respectively. These temperatures represent the 0.12, 0.34, and $0.44\ T_m$ values in the same order. The two higher temperatures were selected to bracket the $0.41\ T_m$ temperature value where most of the observable cluster (loop) defects in molybdenum were reduced by about three orders of magnitude.[12]

The large increase in creep strength of molybdenum and to a lesser extent on tungsten* suggest that radiation induced cluster defects (collision cascade region of the displacement spike) are nucleating sites for the formation of carbide (or nitride) precipitates from the impurity atoms which are in solution at the elevated irradiation temperatures. It is also reasonable to assume that the carbon in solution at the elevated temperature may precipitate on some of the cluster defects during reactor shutdown where the carbon solubility decreases rapidly with temperature. The number of carbon atom jumps required to reach a relaxed displacement spike (compact cluster or loop defect) will be much smaller than that required to reach other lattice imperfections such as the grain boundaries, incoherent portions of twins or existing dislocation networks within the matrix.

Although not directly observed, it is also possible that the increase in the rupture strength (i.e., rupture life) in the case of the molybdenum and tungsten may be attributed to the presence of sub-microscopic voids (vacancy clusters and/or voids) which may harden the material according to the relationship shown in Equation (2). Voids have been observed, for instance, in the case of unalloyed vanadium[13] irradiated in EBR-II.

RADIATION EFFECTS ON ALLOYS

The influence of neutron irradiation on several alloys of the bcc crystal lattice structure suggests that many of those defects associated with the Stage III recovery are still present in the alloy provided that the liquidus temperature

*Irradiated to 1.4 to 1.8×10^{20} n cm^{-2} (E > 1 Mev) at 70, 1000, and 1300°C; i.e., 0.09, 0.34 and $0.43\ T_m$.

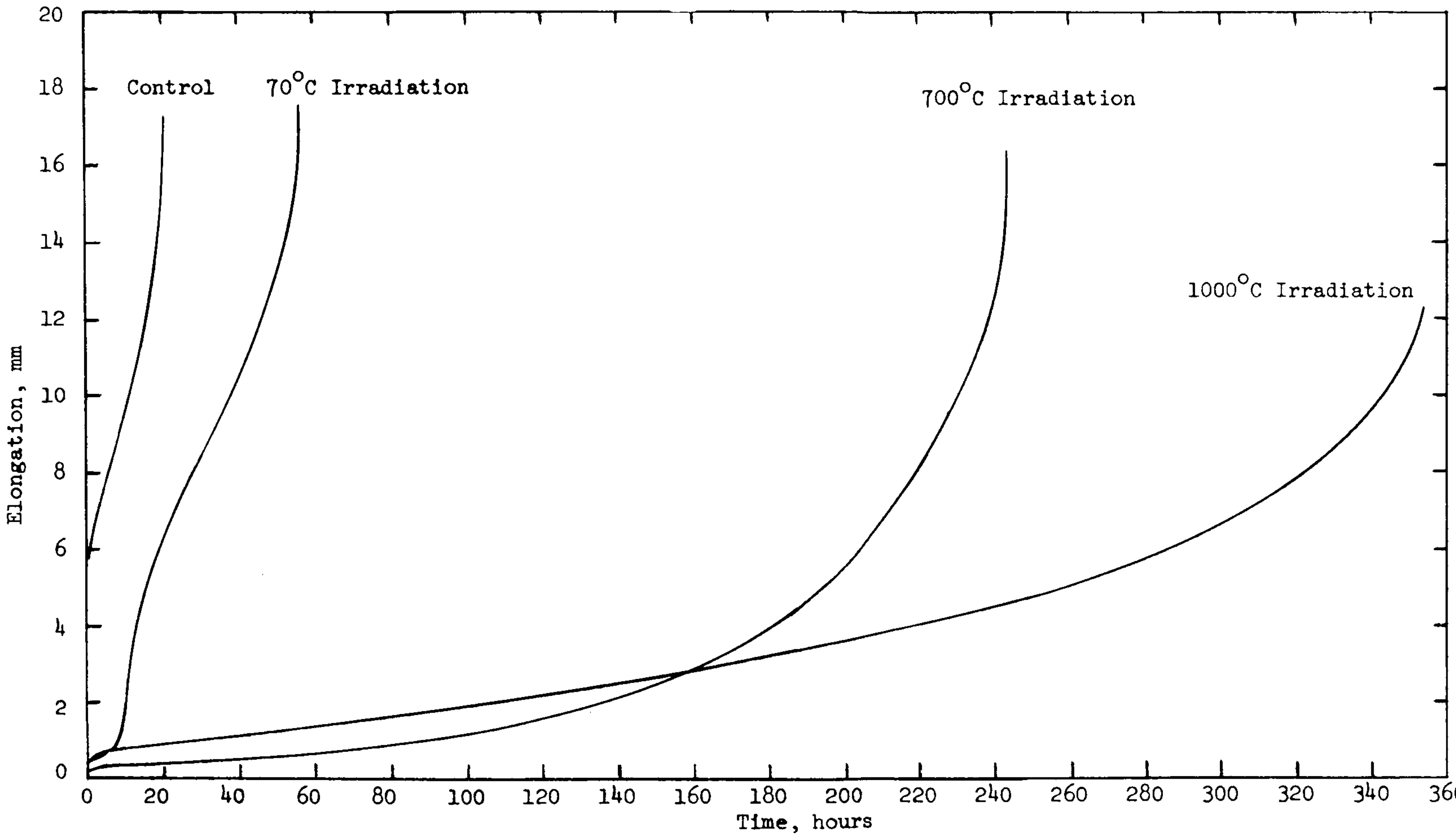

Figure 7 - Creep-rupture properties of irradiated (1.4 to 1.8 x 10^{20} n cm^{-2}, E > 1 MeV) polycrystalline molybdenum specimens tested at a fixed temperature of 750°C and applied stress of 18.00 kg mm^{-2} following irradiation in a thermal reactor at temperatures of 70,700 and 1000°C.

is used to determine the corresponding homologous temperature. A remarkable difference between the unalloyed metal and the alloy of the same metal is the lack of pronounced 0.22 and 0.31 T_m (Stage IV) recovery regions for the case of the alloys.

Resistivity studies show that Stage IV defect is not significantly present in W-25Re alloy.[14] In addition, tensile tests and hot-hardness results show that the thermal hardening component, which is believed to be due to the migration and clustering of those point defects in the 0.22 and 0.31 T_m recovery region, is absent in alloys such as W-25Re, W-25Re-30Mo and Mo-50Re but is very pronounced in the base metals W and Mo.[15]

Transmission electron microscopy studies[16] on Cu and a dilute Al alloy of Cu reveal that "black spot" cluster defects believed to be very small vacancy clusters as well as the larger planar defects (vacancy and/or interstitial platelets or loops) are present in the pure Cu but only loops are present in the dilute alloy for equivalent irradiations. This suggests that two distinct types of radiation induced defects are present in the unalloyed metals and they should also have different degrees of thermal stability as well as different interactions (hardening effects) with glissile dislocations.

This apparent difference between the alloy and the base metal should tend to suppress the cluster induced changes in mechanical properties, of the bcc alloys (i.e. V-20Ti) irradiated in the EBR-II since the sodium ambient temperature will be above the 0.22 T_m temperature level.

SUMMARY

Although very little effort has been applied to the investigations of the effects of radiation on the mechanical and physical properties of the refractory metals and alloys, compared to the work reported on the austenitic stainless steels and other fcc alloys, it is believed that some important preliminary observations may be recorded. They are:

1. The transmission microscope has contributed valuable information on the effects of radiation to the bcc metals. It has been shown that mechanical properties may be correlated with observable cluster defects by the use of presently accepted dislocation theories.

2. Transmutation products are significant contributors to the strength of the irradiated metals. This is shown to be the case of tungsten and it should also be true for tantalum and any other high thermal neutron absorbing metal and alloy.

3. Elevated temperature ($T > 0.5\ T_m$) ductility is not seriously affected by neutron irradiation. It is possible that the crystal structure plays an important role in the resulting ductility in that the fcc metals appear to be much more sensitive to this embrittlement.

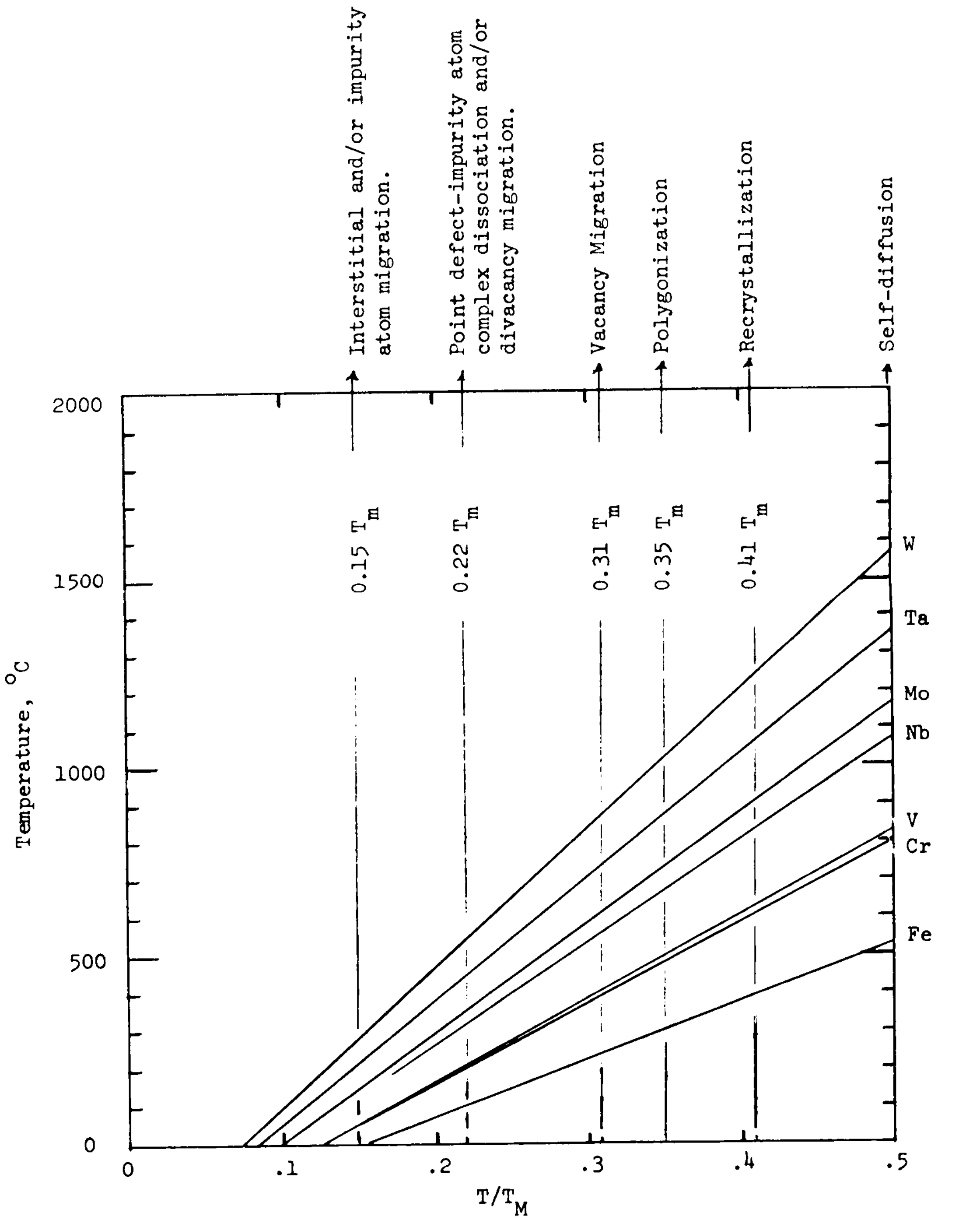

Figure 8 - Temperature in degrees Centigrade corresponding to the homologous temperature for the respective bcc metals. Some possible defect migration mechanisms are also presented.

4. Alloys of the bcc metals are not affected as much as the respective base metal for equivalent irradiation fluences and temperatures. The alloy may show better ductility at temperatures above 0.22 T_m than the base metal with both showing possible complete recovery at temperatures above 0.35 T_m.

5. Due to the low solubility of interstitial impurities in Mo and W, elevated temperature irradiations of these two metals tend to show irradiation induced nucleation of precipitates (or voids) which result in a further hardening of these metals. There is no reason why the other metals Ta, Nb, V and Cr should not exhibit a similar phenomena.

6. It is only recently that preliminary test data are reported on the effects of irradiation on vanadium and its alloys. Indications are that, based on homologous temperatures, these metals behave similar to the other bcc metals and alloys.

7. Irradiation, anneal and test temperature plays a very important role in the influence of neutron irradiation on the physical and mechanical properties of refractory metals and alloys. For metals of reasonable purity the unique temperature regions may be 0.15, 0.22, 0.31, 0.35, 0.41 and 0.50 T_m with possibly another region at a temperature of about 0.71 T_m. The temperatures and possible contributing mechanisms for some of the refractory metals are summarized in Figure 8.

ACKNOWLEDGMENTS

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DISCUSSION

J.F. Schumar - I am sure that after John Moteff's talk, we know more of the mechanics of irradiation damage. John, my question would be - do you think that we can synthesize some alloys that will go to 10^{30} nvt and still have 50% elongation and no creep?

J. Moteff - Yes, and I think that if we can get Congress to pass a rule that the mechanical properties should go accordingly, we will be in.

K

LIQUID SODIUM AS A REACTOR COOLANT

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United Nuclear Corporation
Elmsford, New York

ABSTRACT

The attainment of high breeding ratios, leading to low fuel cycle costs and conservation of our natural uranium resources, is shown to be the primary reason for selecting sodium as a reactor coolant. Reliability of sodium systems is discussed. Failures in existing systems, which operate at sodium temperatures below 1000°F, are attributed generally to poor engineering practices rather than to a lack in sodium materials technology. If further reductions in power costs are to be realized by raising sodium temperatures to 1200°F to 1400°F to generate 1050°F steam, the state-of-the-art of sodium technology must be extended in the areas of fuel cladding and steam generator materials and sodium purity control.

INTRODUCTION

When asked to present a paper on "Liquid Sodium as a Reactor Coolant," I was immediately confronted with the problem of not being able to cover all that might be said on this broad subject within the allocated time. Instead of touching briefly on a large number of items, I decided to limit the paper to a few topics which I considered particularly timely. In selecting these topics, I was also guided by the Program Committee's desire to convey to the audience an idea of the potential usefulness and limitations of sodium as a reactor coolant and its present status of development. I purposely omitted much detail and oversimplified, where appropriate, to place emphasis on basic advantages and problems.

To keep the scope of the paper within manageable limits, it addresses itself only to nuclear plants for central station power. In the text, the word "liquid" is dropped from the title, i.e., "sodium" stands for "liquid sodium" – not sodium vapor.

WHY SODIUM?

Table 1 shows the effects of sodium's favorable properties on plant characteristics.

Sodium has excellent high-temperature heat transfer and transport properties.* These properties permit high heat release rates or, in reactor terminology, high power densities. Low plant heat rates or high thermal efficiencies can be attained because (1) high reactor outlet temperatures obtainable with sodium lead to highly superheated steam, which is one requisite for high Rankine cycle efficiencies, and

*In this paper, "transport properties" refer to all thermophysical properties which affect coolant system and pumping cost, i.e., coolant density, specific heat, thermal conductivity, viscosity and vapor pressure.

TABLE 1 — FAVORABLE PROPERTIES OF SODIUM AND THEIR EFFECTS ON PLANT CHARACTERISTICS

Heat Transfer and Transport Properties

- High Heat Release Rates (High Power Densities)
- Low Plant Heat Rates (High Thermal Efficiencies)
- Thin-Walled Containment
- Emergency Cooling

Cost of Sodium

- ~20 cents/lb

Nuclear Properties

- High Breeding Ratios

(2) pumping power requirements for coolant circulation are relatively low. Because of sodium's low vapor pressure, sodium containment vessels and pipes can be thin-walled. Its properties are ideal for providing emergency cooling, although there are still some uncertainties in regard to sodium superheat and two-phase flow phenomena which must be resolved.

Sodium is readily available and can be purchased at a reasonable cost – approximately 20 ¢/lb.

High breeding ratios can be attained with sodium as a reactor coolant. The two basic characteristics which contribute to this are:

1. Scattering of neutrons by sodium does not greatly reduce their energies;

2. With plutonium, the average number of neutrons liberated for each neutron absorbed in the fuel, (η), increases with increased neutron energy.

Therefore, more neutrons are liberated per fission if the reactor coolant is sodium than would be liberated if the coolant contains hydrogen or other low-weight atoms which slow down neutrons during scattering. A larger number of neutrons is then available to convert more fertile U^{238} into fissile plutonium. High breeding ratios are further assured because a hard neutron energy spectrum leads to relatively low parasitic absorption of neutrons in sodium and structures.

The major reasons for selecting sodium over other coolants are depicted in Table 2.

Relatively low cost is the principal factor for selecting sodium over other alkali metal coolants. One of these, lithium, appears attractive because of its high specific heat, 1 Btu/lb-°F vs 0.3 Btu/lb-°F for sodium. However, in addition to its higher cost, the corrosiveness of lithium makes its containment with conventional materials most difficult. Lithium also has a relatively low atomic weight; therefore, neutrons are slowed down and breeding ratios are reduced. Potassium, as an alloying element, can suppress the melting point of sodium. Thus, NaK was selected as a reactor coolant for several plants because it was thought to be advantageous for the coolant to be liquid at room temperature. However, NaK reacts more vigorous-

TABLE 2 — REASONS FOR SELECTING SODIUM OVER OTHER COOLANTS

Coolants	Primary Reason	Secondary Reason
Other Alkali Metals	Coolant cost	
Water	Nuclear	Heat transfer and transport
Gas	Heat transfer and transport	

ly in air, and experience has shown that maintenance and repair are actually facilitated by the fact that sodium can be frozen in the adjoining piping.

In comparing heat transfer and transport properties of sodium with those of water, it is immediately apparent that sodium's high thermal conductivity and low vapor pressure give it a distinct advantage. In the early days of reactor inventing, it was thought that these favorable properties were sufficient to warrant the selection of sodium over water as a reactor coolant. However, in view of the tremendous progress which has been made with water-cooled reactors and the utilities' traditional use and familiarity with water, it appears doubtful that these advantages are sufficient in themselves for the selection of sodium over water at this time.

Nevertheless, the near and distant future hold strong economic incentives for the use of sodium, rather than water or steam, as a reactor coolant. These incentives arise because the hydrogen in water or steam slows down neutrons, and, as described above, this leads to lower breeding ratios. Significantly higher breeding ratios can be attained with sodium-cooled reactors, leading to lower fuel cycle costs and to the conservation of our natural uranium resources.

It has been estimated that the plutonium, which will be produced in water-cooled plants in the foreseeable future, might be worth twice as much to an operator of a sodium-cooled breeder than to one with a water-cooled plant. Serious studies are under way to determine how much plutonium should be held for future use in sodium-cooled, fast breeders in lieu of recycling it in water plants. The stakes are large. To demonstrate their order of magnitude, one may take an estimate that 300 tons of fissile plutonium will have been produced in water plants by 1985. If the plutonium will be worth twice as much, e.g., 20 $/g vs 10 $/g, the potential savings are 3 billion dollars. With an estimated production of 40 tons of fissile plutonium during 1985, the potential annual savings are 400 million dollars at that time. For the initial demonstration and prototype plants, these savings in fuel cycle costs may be offset by higher plant capital costs. Nevertheless, it is expected that the net power costs for subsequent sodium-cooled, fast breeders will be less than those for water or steam-cooled plants.

Even if trade-off studies show that only a fraction of the potential savings can be realized, it is obvious from the magnitude of the numbers that the economic incentives are substantial. Similarly, it can be demonstrated that the higher breeding ratios of sodium-cooled reactors will decrease the doubling time of plutonium production, and thereby, substantially reduce uranium mining requirements. The goal of conserving uranium resources is further aided by the fact that high specific powers can be attained in sodium-cooled breeders, so that inventory requirements for fissile isotopes are relatively low.

Those who favor gas as a reactor coolant claim the same nuclear advantages with even higher breeding ratios. However, gases have very poor heat transfer and transport properties in comparison to those of sodium. Even under the proposed pressures, gases do not have the potential for attainment of the high heat release rates and low plant heat rates predicted for sodium. Of special concern with gas-cooled reactors are the possible damage and safety problems resulting from a loss-of-coolant flow and/or gas pressure. In contrast, sodium has particularly favorable properties to cope with an accidental loss of pumping power and/or coolant pressure, although further efforts are required to establish what features must be incorporated into a reactor design to assure that sodium voiding during an extremely large excursion can be handled in a safe manner.

In summary, the principal reasons for selecting sodium as a reactor coolant are its potential for high breeding ratios, high heat release rates, high thermal efficiencies, and its inherently favorable emergency cooling characteristics. Since the high breeding ratios represent an overriding economic incentive, thermal performance parameters such as maximum sodium temperatures, heat fluxes, sodium velocities, etc., may be held at modest levels in early plants. The advantage of better performance with higher values remains as a potential improvement for later plants. These last considerations are important to the assessment of the state-of-the art of sodium technology, which will be discussed later.

Regardless of how high its thermal efficiency or how low its initial cost, no plant produces power economically unless it demonstrates a high availability factor. This means that plant components and systems must have a high degree of reliability.

RELIABILITY

One frequently hears remarks such as, "Well, sodium as a reactor coolant looks attractive on paper, but will sodium systems ever be reliable?"

In spite of the continued difficulties encountered with existing sodium systems, I would like to state at the outset that there should be no question as to whether they can ever be made reliable, that in fact we already have proof that it can be done.

Figure 1 is a symbol of that proof. It is a picture which appeared on the front page of the New York Times on September 27, 1957, more than 10 years ago. It shows President Eisenhower looking through a periscope of the submarine Seawolf while it was submerged 60 ft beneath the surface of the ocean. Powered by the sodium-cooled S2G plant, the Seawolf operated as a unit of the Atlantic Fleet for nearly two years. Its excellent performance record during this period and the implications from this picture should leave very little doubt that sodium systems can be reliable.

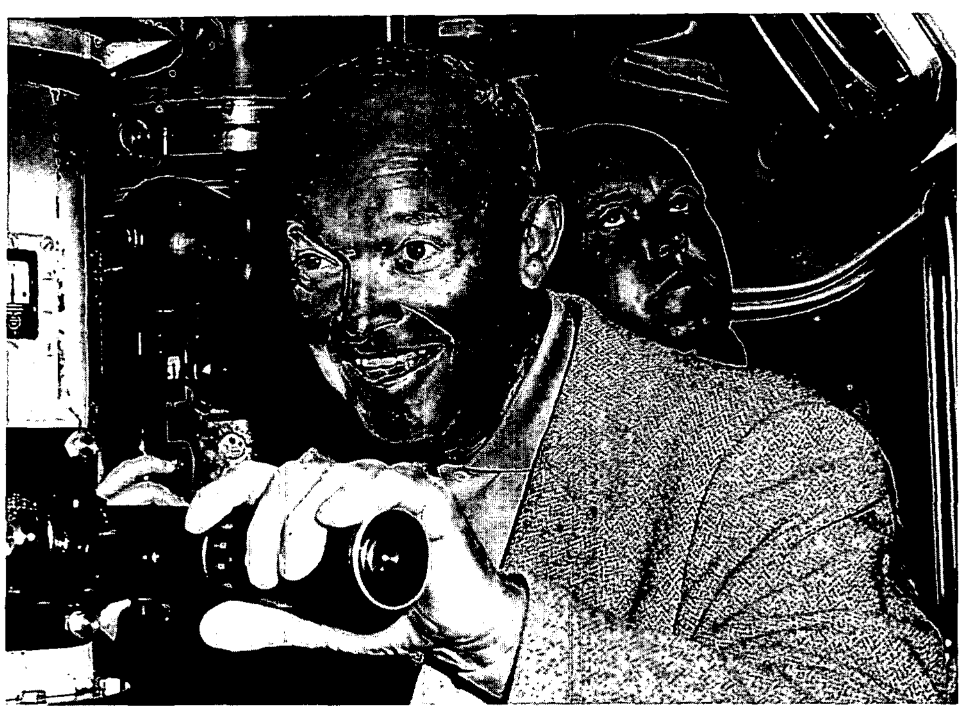

Fig. 1 — President Eisenhower Viewing the Sea Above the Seawolf Through a Periscope

The Seawolf is usually remembered for the difficulties encountered during the development of its sodium-cooled power plant, rather than for its successes. There were many difficulties during the development phases, including some with the sodium-heated steam generator equipment, about which more will be said later. However, once commissioned, the sodium system performed reliably.

In spite of its reliability, the S2G plant was replaced with a pressurized water plant, because it was not economic for the Navy to continue with two completely different systems. It might also be noted that requirements for compactness would not allow breeding. Thus, the primary reason for selecting sodium for central station power plants was not a controlling factor in the Navy's decision.

Why was the S2G plant so reliable and why today, 10 years later, do we still encounter crippling failures in almost every operating sodium system? There is no single answer to this question, but I would like to advance a few thoughts which hopefully can be applied toward the prevention of similar difficulties in the future.

1. Many sodium systems, including full-size reactor plants, have been built for "experimental" purposes. As with many experimental projects, reliability considerations were not given the high priority which they deserved.

2. Most failures have not been due to a lack in technology, but, rather, due to a lack of applying existing technology. For example, sodium circulation and gas entrainment problems have been encountered repeatedly because of insufficient attention to well-established rules regarding venting, draining, and liquid level requirements. The use of improper cleaning and storage procedures has led to many failures. Such difficulties can be prevented in the future if greater insistence is placed on requiring engineers and supervising personnel to be fully familiar with the existing sodium technology before assigning them the responsibility for designing, installing, or operating sodium components and systems. (This may require

overcoming a new Parkinson's law which seems to have become firmly established. It says that any organization which works on sodium equipment for the first time will make again the same mistakes that all the others have made before.)

3. Don't design and build your own sodium components because you think you can do it at a lower cost than buying them from a commercial supplier. In many instances, in the past, this kind of reasoning was faulty. Sodium heaters are a typical example, but represent only a small fraction of failures caused by such decisions. Many sodium leaks have resulted from a short-circuiting arc between a home-made electric resistance heater and a sodium pipe or vessel. It would have been less expensive to buy a proven heater, even though initial costs might have been higher. Buying components from commercial suppliers has the additional advantage of providing direct support to an infant sodium component industry which must become viable to supply improved, reliable components in the future.

4. Complete component and system testing during the development phases contributed, perhaps more than any other cause, to the reliability of operation of the Seawolf plant. No other past sodium project had the benefit of such rigorous testing.

5. Another major contribution to reliability was the emphasis on detailed engineering plans and specifications, and stringent quality control procedures during all stages of the Seawolf program. Recognition of this has undoubtedly led to the disciplined engineering approach which is now being taken by the U. S. Atomic Energy Commission in its Liquid Metal Fast Breeder Reactor (LMFBR) Program.

It should be noted that, in this tally, failures are attributed mostly to engineering practices rather than to materials technology. The present status of sodium materials technology and some future needs are discussed in the following section.

MATERIALS TECHNOLOGY

We reviewed the state-of-the-art of sodium technology three years ago,[1] and concluded that:

1. "The existing plants have an inherent capability of producing 900°F steam with sodium temperatures up to 1000°F. Sodium technology for these systems is well in hand. They can operate under cold-trapped conditions without exceeding acceptable corrosion rates of available materials. Sodium-sampling techniques, chemical analysis methods, and on-line indicators are available to monitor sodium purity in cold-trapped systems.

2. "The fast breeder reactors of the future are projected to produce 1050°F steam with maximum sodium temperatures in the 1200 to 1400°F range. If these temperatures are to be achieved with economic plants, the state-of-the-art of sodium technology must be extended in the areas of fuel cladding and steam generator materials and related sodium purity control."

We also indicated that some development would be required to cope with sodium frost accumulation in cover gas spaces and to select adequate hard-facing materials for higher temperature systems.

These statements are still valid today, although in retrospect it appears that we could have been more conservative in regard to steam temperatures for existing plants. Accordingly, I have lowered this value to 800°F in Table 3.

Much new information has become available during the last three years. Sodium technology is being advanced around the world in considerable breadth and depth, as illustrated by the results which were reported at three international conferences in 1966. These conferen-

TABLE 3 — MATERIALS TECHNOLOGY

	Existing Plants	Target Plants
Steam Temperature, °F	800	1050
Max. Sodium Temperature, °F	1000	1200 to 1400
Required Technology	In hand	• Fuel cladding materials • Steam generator materials • Sodium purity control

ces were: "An International Symposium on the Alkali Metals," held at Nottingham; an IAEA sponsored "Symposium on Alkali Metal Coolants" in Vienna; and an "International Conference on the Properties of Liquid Metals" held at Brookhaven National Laboratory. The proceedings of these conferences were published last year in References 2, 3, and 4, respectively. They include 148 papers, of which 112 are directly concerned with sodium. In content, they range from fundamental investigations, such as "Interatomic Distances in Liquid Metals and Alloys," to applied topics, such as "Effects of Sodium on the Metallurgical and Mechanical Properties of Candidate LMFBR Alloys." A list of these papers, under subject headings, is appended for convenient reference.

No attempt is made here to review each paper or to list hundreds of other pertinent articles and reports which have been published in recent years. A Liquid Metal Information Center has been established at Santa Susana, California, under the auspices of the U. S. Atomic Energy Commission. It is available for literature searches and other source information in this field.

In general, the recent literature indicates that the empirical approach of the past is being supplemented by new fundamental investigations. These should lead to the better understanding of basic mechanisms which will be required to extend the existing sodium technology to the higher temperature reactors. The previously mentioned major areas of concern have been selected for further comment.

Fuel Cladding Materials

Fuel cladding and core structures for fast breeder reactors must not only be compatible with the sodium, but they must also withstand high radiation levels, particularly high-energy neutron fluxes, for long times. Irradiation resistance of applicable materials was the subject of two preceding papers at this Session, and will, therefore, not be discussed further here. Compatibility with sodium in the 1200 to 1400°F temperature range is intimately tied to sodium purity control, about which more will be said later in this paper.

Steam Generator Materials

A serious materials problem exists with sodium-heated steam generators. It is one of stress corrosion on the water side, not on the sodium side.

Type 300 series stainless steels are preferred for sodium containment, but they are highly susceptible to stress corrosion attack in aqueous media, especially under the sensitized conditions which pertain after long-term operation at elevated temperatures. The sodium-heated steam generators, which were developed for the Seawolf, were the first to experience this type of attack. Others followed with the latest incident being a steam pipe failure in the Sodium Component Test Installation (SCTI) at the Liquid Metal Engineering Center. This pipe and associated fittings, which were fabricated from type 347 stainless steel, failed, while no stress corrosion attack was noted in the steam generator which was under test at the same time. The steam generator did not fail because all water-exposed surfaces were clad with Inconel to make this unit, which is otherwise fabricated with type 316 stainless steel, less susceptible to stress corrosion. Thus, this incident demonstrates that Inconel cladding of water-exposed surfaces is a possible solution to the stress-corrosion problem.

The use of Inconel cladding is relatively expensive. A less expensive solution is the use of low-chromium alloy steels throughout a steam generator. These ferritic steels are less susceptible to stress corrosion than the austenitic steels, although caustic embrittlement is of concern. Their strength and resistance to decarburization in a sodium environment decreases rapidly as temperatures approach 1000°F. Their maximum useful temperatures in this regard are not known, but it appears certain that target temperatures of 1050°F steam cannot be attained with the presently available ferritic steels. As mentioned before, maximum thermal performance is not essential for early plants to be economically attractive, and therefore, low-chromium alloy steels are likely candidates.

The sodium-heated steam generators at the Hallam Nuclear Power Facility (HNPF), Experimental Breeder Reactor II (EBR-II), and Enrico Fermi Atomic Power Plant (EFAPP) use such steels throughout.

It should be noted that these solutions utilize commercially available materials. Another long-term approach, which perhaps should receive more attention than it has been getting in the past, would be to develop new materials. Such materials should have a low susceptibility to stress corrosion, high resistance to carbon transfer, and adequate mechanical properties. Some success in this direction has been achieved by additions of carbide formers, e.g., niobium and titanium to the low-chromium alloy steels.

It is obvious, from the experience to date, that there is no guarantee that stress-corrosion failures will never occur in sodium-heated steam generators. Steps which may be taken to prevent their occurrence include:

1. Select materials which have a low susceptibility to stress corrosion;
2. Prevent contact of these materials with undesirable substances during fabrication and storage;
3. Utilize proven cleaning fluids and procedures;
4. Maintain complete control over water chemistry at all times; and
5. Operate the steam generator only under approved conditions.

These material considerations may be augmented further by special design features, such as "no crevices," drainability, etc. With these approaches, it should be possible to achieve a negligibly small incident rate.

If a failure does occur, it is possible to cope with sodium-water reactions. As has been demonstrated repeatedly during tests and accidental failures, no serious damage should result if certain precautions are taken. These include special venting arrangements, means for early detection of a leak, and prompt dumping of water and steam to reduce pressure. Venting is required to prevent buildup of excessive pressures on the sodium side, and to prevent hydrogen, which is released in sodium-water reactions, from forming explosive mixtures with oxygen. Leaks are detected by increases in pressure or increases in hydrogen concentration in the cover gas of the sodium system and/or by in-line, sodium purity monitors which will be described further.

Sodium Purity Control

During early experience with sodium systems, plugging of small lines was frequently encountered. It was soon established that the plugs were caused by precipitating oxides, resulting from the fact that the oxygen solubility in sodium decreases with decreasing temperatures. The remedy for this problem was a cold trap in which the oxides were removed from the sodium by precipitation under controlled conditions. Greater precautions were taken to prevent the contamination of sodium with oxygen, and the plugging meter was invented to maintain a check on oxide precipitation temperatures for the circulating sodium. Plugging temperatures of 300°F or oxygen concentrations of 20 ppm* were considered acceptable.

As sodium temperatures rose above 1000°F, it was recognized that oxygen also played an important role in corrosion and mass transfer processes. To limit such effects, it became desirable to hold oxygen concentrations at 10 ppm or less.

A number of problems arose with the advent of 10 ppm maximum oxygen. First, there was no proven method for oxygen analysis in the 1 to 10 ppm range; secondly, the solubility of oxygen in this range was not well established; and thirdly, other impurities appeared to be present at comparable levels. This last problem manifests itself in many ways and is still of concern today, especially in regard to other interstitial impurities: carbon, hydrogen, and nitrogen.

Two methods, amalgamation and vacuum distillation, are commonly used for chemical analysis for oxygen. With both methods, sodium is separated from a residue, which, in the past, has been assumed to be sodium oxide. The presence of other compounds in the residue, e.g., carbonates, hydroxides, or hydrides, leads to errors in the oxygen determination. Attempts are being made now to identify these compounds, so that the analysis may yield total oxygen and the form in which the oxygen is present.

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*In this paper, parts per million (ppm) signifies the impurity contained in sodium on a weight basis.

The presence of other impurities also affects the interpretation of plugging meter readings. If sodium oxide is the only impurity present, a plugging meter reading can be related to oxygen concentration, provided that the oxygen solubility and the meter's characteristics in regard to velocity and cooling rate effects are known. With other impurities present, the plugging meter is only a "something precipitating meter," it is not an oxygen meter. All too frequently, plugging meter readings are still being reported as ppm oxygen. This should not be done, unless there is assurance that the dominant impurity is oxygen, which in today's systems is generally not the case.

This does not mean that the plugging meter has lost its usefulness. On the contrary, it should continue to prove to be a most valuable instrument for detecting precipitating impurities, especially following accidental contamination of the sodium.

Another "something" monitor is the Rhometer, which detects changes in the electrical resistivity of sodium. Most impurities cause changes in resistivity but they are frequently comparable in magnitude to those resulting from normal sodium temperature changes. Attempts at developing temperature compensating devices for the Rhometer continue to meet with difficulties.

Plugging meter and Rhometer readings for sodium systems are analogous to total dissolved solids and electrical resistivity measurements for water systems, respectively. Another analogy can be drawn between an oxygen activity meter for sodium systems and a pH meter for water systems.

Most everyone knows that pH control is essential to the prevention of excessive corrosion in water systems, but few remember the full significance of pH readings. For present purposes, I would like to recall that a pH meter measures the hydrogen ion concentration in water, or, more strictly speaking, the hydrogen ion activity. It is this hydrogen ion activity, rather than total hydrogen content, which plays a major role in water-corrosion processes. Many pH meters have millivolt scales to indicate oxidation-reduction potentials.

An analogous instrument, a Liquid Metal Oxygen Meter, measures the oxidation-reduction potential of oxygen in sodium directly, and, like a pH meter, operates on the principle of an electrochemical cell. It was developed under the auspices of the U. S. Atomic Energy Commission and is now commercially available. As with water systems, the oxygen potential or oxygen activity is expected to be a more significant parameter than total oxygen for assessing corrosion potentials in sodium systems. It should be remembered, though, that activity is a thermodynamic property, and, as such, is not a measure of reaction rates.

Similar considerations lead to the conclusion that carbon transfer in a sodium system, e.g., carbon transfer from ferritic to austenitic steels, or from steels to other fuel cladding materials, is controlled by differences in carbon activities. Therefore, carbon activity or carburizing potential measurements for sodium would be of greater interest than total carbon determinations. An on-line carbon meter which measures these properties is also being developed under the auspices of the U. S. Atomic Energy Commission. Prototype models have been built and are ready for field tests at Commission-designated sites. Chemical analysis for carbon and carbon solubility studies have generated controversial results, and it is hoped that with the use of the carbon meter a better understanding of carbon transfer processes will be gained. Carbon transfer is of concern because a loss of carbon usually reduces strength, and pickup of carbon can lead to extreme embrittlement of containment and structural materials.

The monitoring of hydrogen in sodium is of special interest as a means for detecting a water-to-sodium leak in a steam generator. An instrument based on the diffusion of hydrogen through a hydrogen permeable membrane and cover gas chromatography are the principal, existing hydrogen monitors for leak detection. Hydrogen is a common, low-level impurity in all sodium systems. It can affect the mechanical properties of fuel clads and other structures if they are made of hydrogen-sensitive materials such as niobium or zirconium. The development of a hydrogen activity meter has been initiated recently.

Nitrogen transfer has been observed in sodium systems, but little is known about the basic mechanisms involved. No serious difficulties have been traced to nitrogen transfer, although changes in mechanical properties might be expected.

The greatest uncertainties lie in the interactions between the interstitial impurities: oxygen-carbon-hydrogen-nitrogen. They affect not only impurity detection and corrosion processes, but also the design and performance of purification equipment.

Much work remains to be done in regard to sodium purification. The general conclusions which we reached three years ago are still valid today:[1]

"At the anticipated higher temperatures of future plants, cold trapping may still produce acceptable sodium purities for all-steel systems. If refractory metals are used as cladding materials in systems that operate above 1000°F, oxygen levels may have to be reduced further by gettering. Presently available criteria for the design of hot traps are empirical, and more fundamental data are needed to achieve economic designs. The effects of impurity interactions are not well understood but may play an important role in gettering performance and in the magnitudes of corrosion rates. Refinements of existing techniques or new approaches will be required to determine sodium purity at hot-gettered levels."

CONCLUSIONS

1. Sodium is of interest primarily because high breeding ratios may be attained with sodium as a reactor coolant. High breeding ratios lead to low fuel cycle costs and to the conservation of our natural uranium resources.
2. Sodium has excellent heat transfer and transport properties which hold a potential for further improvements in heat release rates and plant heat rates beyond initial demonstration plants.
3. Sodium systems can be made reliable.
4. Most failures which have been experienced in sodium systems, which operate at temperatures below 1000°F, can be attributed to poor engineering practices rather than to a lack in sodium materials technology.
5. If the projected target temperatures of 1050°F steam with 1200 to 1400°F sodium temperatures are to be attained, the state-of-the-art of sodium technology must be extended in the areas of fuel cladding and steam generator materials and sodium purity control.

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DISCUSSION

E.E. Hoffman (General Electric) - Kurt, one problem for which I realize you can't give me much detail, but there seems to be quite a bit of activity going on by people interested in fast reactors on liquid sodium superheat, and I wonder how serious do you consider this problem, and if it is a problem, what are some of the solutions that are being considered?

K. Goldmann - I only mentioned liquid superheat in passing, pointing out that this is a potential problem and in case you do have very large excursions in the reactor, you can get to rather high temperatures, temperatures that are pretty far above the saturation temperature of the sodium, and subsequently you can get a sudden expulsion of the sodium. Now, I believe that answers as to what one does about this have to come from design studies and also from a great deal more experimental and development work to establish to what extent superheat as such is a problem. As long as the sodium does not suddenly form large volumes of vapor, superheat is no problem. It only becomes, or could become, a problem if you suddenly generate a large amount of vapor. I think the answers to what is required to do this, what you could do to prevent large amounts of superheat from occurring, and how you could prevent large reactivity excursion are design problems.

FFTF FUEL DEVELOPMENT PROGRAM

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The FFTF Driver Fuel Development program is directed toward high functional reliability and safety of the initial core through the use of conventional fuel element materials and fuel component design. This paper describes the two primary phases of the development program; process development and irradiation testing. These activities are integrated with the LMFBR program.

The principal irradiation tests consist of encapsulated full length pins in GETR, prototypic bare pin assemblies in EBR-II, and transient tests in TREAT.

The candidate and alternate fuel fabrication routes are discussed. The reference mixed oxide pellet fabrication method is mechanical blending, cold pressing, and sintering solid cylindrical pellets. Large scale fabrication demonstration rounds by industry are planned.

The results from this program will provide extensive basic knowledge and fuel performance data for the rapidly developing fast reactor era.

The rapidly growing power requirements in the United States together with the need for total energy conservation have resulted in USAEC sponsored development programs for fast breeder reactors.

In this program, the Fast Flux Test Facility, also known as the FFTF, is one of the major facilities for the U. S. Liquid Metal Fast Breeder Reactor Program. This facility consists of two primary plants: (1) the Fast Test Reactor or FTR and (2) the Nuclear Proof Test Facility or NPTF, to be used for physics. These plants will be constructed at the Pacific Northwest Laboratories in Richland, Washington with a critical date in November, 1971 for the NPTF and November, 1973 for the FTR.

The main purpose of the FTR is to provide a representative and controlled sodium cooled fast flux test environment in which to conduct large scale tests of materials and reactor components. The results will lead to both demonstration and commercial breeder plants. Only limited data now exist on materials and components operating in severe fast reactor environments and on methods of manufacturing mixed oxide fuels for fast reactors. The successful completion of the development programs described herein will provide the means to assure the high reliability, safety, and functional efficiency for the FFTF.

The materials and fuel processing problems for the FFTF are significantly more difficult than those of the thermal reactors. The high coolant temperatures, ~ 1000-1200 ^{o}F, and high fast neutron flux, upward of 6 x 10^{15} n/cm^2 sec, point to technical uncertainties that will not yield to extrapolation from thermal reactor test data. Similarly, the handling of large quantities of plutonium from future power reactor discharges that contain a high percentage of isotope Pu 241 requires special handling techniques. In recognition of these and other problems, the fuel development program for the FFTF is concentrating on two specific areas: (1) process development and (2) irradiation testing. This integrated program begins on the existing base of currently available materials and processing technology and irradiation data. It is highly important that the FFTF operate with a high availability factor, thus particular emphasis is placed on functional reliability. This position will be achieved by thoroughly assuring the quality of all materials and components used in the plant, and by having thoroughly tested prototypic materials and components in appropriate environments. Special emphasis is also placed throughout the program on instrumented large scale statistical tests and thorough pre- and post-test characterization.

The current reference FTR driver fuel pin design consists of a 32-inch fuel column enclosed in 0.250 inch diameter 316 SS tube 16 mils thick. The overall length of the pin is six feet. The solid mixed oxide fuel pellets are 93% theoretical density. The fuel-cladding diametral gap of 6 mils results in a fuel smeared density of 88% theoretical density.

A. PROCESS DEVELOPMENT PROGRAM

The objectives of the process development program are: (1) to develop processes, specifications, and quality standards which will assure high FTR fuel performance reliability; and (2) to assure a technological base which is adaptable to industrial use.

The proposed reference process for fabricating NPTF and FTR fuel pins basically consists of mechanically blending mixed oxide powders, pressing, sintering, and loading the pellets into thin walled tubing. A brief description of each major process operation follows. The purpose of the process development phase is to obtain a thorough knowledge of the main process variables affecting the product and to demonstrate the ability to consistently produce the product to tight specifications. A pilot plant for this process is being installed.

1. Raw Materials

The reference route for raw materials for the mechanical blending process includes: (a) the preparation of PuO_2 (average particle diameter 2 to 4 microns) by calcining Pu(IV) oxalate precipitated from plutonium nitrate solutions and (b) the use of commercially available, ceramic grade UO_2. The natural or depleted UO_2 for the end pellets will be sinterable ceramic grade produced by the conventional ammonium diuranate (ADU) process.

The mixed oxide fuel preparation phase includes receiving UO_2 and PuO_2 powders that are fully characterized. The preweighed UO_2-PuO_2 is then mixed, screened, and blended in a "V" type blender adapted with an intensifier unit.

The process development work includes an intensive analytical program with numerous industrial fabrication sites participating. Analytical methods for plutonium assay have been established, but the accuracy is uncertain and further development is required.

2. Ball Mill

The blended powders are then dry ballmilled for a predetermined time to produce optimum surface area and a uniform plutonia-urania micro dispersion. The specified limits for final sintered density and Pu homogeneity are 93-95% theoretical density and 50 micron maximum PuO_2 particles.

Each batch of powder has specific characteristics related to time of milling and surface area. These factors generally run 20-40 hours and 5-8 square meters per gram, respectively.

The milled powders are placed in the "V" blender with intensifier for an additional five minutes to break any agglomerates and are then passed through a 100 mesh screen.

Alternate techniques being examined include:

a. Short Milling Time Only - Purchase powders slightly coarser than required for sintering and mill for a short time to intermix and activate the powders.

b. Mixing Only - Purchase powders to size for sintering and just intermix by "V" blender, high speed mixer or air mixer.

Mechanical blending is the reference process primarily due to the experience with mixed oxide fuel fabrication for thermal reactors. Coprecipitation will also be evaluated. Development efforts include controlled screening for particle size assurance and a complete process variables study covering all operations through pelletizing and sintering.

3. Binder Addition

Carbowax 20,000 binder is added to 1 kg of mixed oxide (this material is nominally 2% dry weight of a 20% aqueous solution). Criticality considerations now limit the batch operations to a total of 1 kg of fuel. This quantity can be significantly increased by use of geometrically critically safe equipment. Following binder addition, the powders are dried at $\sim 68\ ^{\circ}C$ for two hours in a circulating air oven. The dried fuel is granulated and screened to -20 +100 mesh to provide free flowing press feed. The use of dry binders or pressing without binders are being considered as alternate techniques.

4. Pellet Pressing and Sintering

Prior to pelletizing a powder lot, ten test pellets are sintered to determine the sintering characteristics and to make minor press and/or die adjustments. Dependent upon the characteristics of the feed material either of two pressing methods are used to fabricate the pellet:

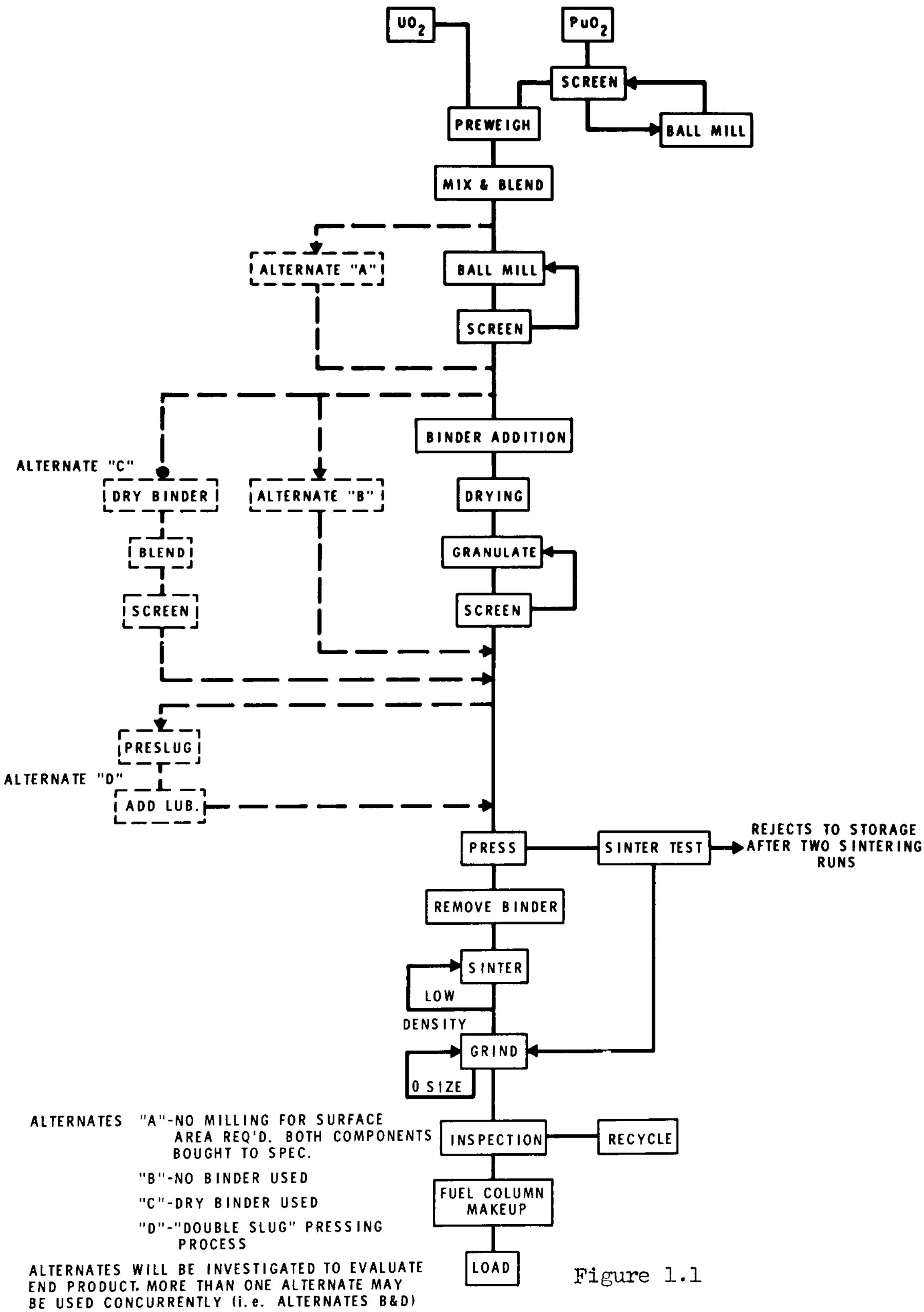

Figure 1.1

Reference Fuel Fabrication Process

a. A direct press and sinter cycle, or

b. A "slugging" process in which the powder is pressed to a relatively low green density, granulated, and repressed to a high density and then sintered.

Samples are selected for relative oxygen to metal content analysis and geometrical density calculations. Pellets low in density will be resintered. If the specified density is not achieved after resintering, either (a) a waiver will be required for use of the fuel in the process, (b) the material will be recycled, or (c) the material will be held for recovery.

5. Recycle Material

The material being considered for clean recycle includes pellets (a) which did not meet density specifications, or (b) which show excessive cracking or chipping. The recycle fuel material will either be (a) crushed, milled and recycled through the process commencing with the binder additive; (b) crushed to -10 +100 mesh size and added to the fuel to assist in controlling the sintered density; or (c) sent through an oxidation and reduction cycle to produce press feed material. Method (b) is preferred since it is the simplest and, as a minor diluent, does not grossly affect the sintering characteristics of the fuel to which it is added. The effects of recycle material additions will be evaluated in the development program.

6. Grinding

It may be necessary to grind some or all of the pellets to final size ($\pm$.0005 inch). This operation may be done either wet or dry depending on feed rate and total station throughput. The grinder scrap generated by this operation will require chemical recovery. Elimination of this step is one of the major goals of the process development program.

7. Cleaning

All pellets are cleaned of loose dust by vacuum or by jet techniques.

8. Pin Assembly

The pin assembly phase of the process consists of the fuel loading, decontamination, plenum hardware insertion, and final end cap closure operation. Figure 1.2 is a flow diagram of the individual process steps.

9. Fuel Loading

Pellet columns are assembled to meet length and weight requirements. Then (a) two insulator pellets (depleted UO_2) are weighed and added to each end of the fuel column, (b) the entire column is loaded into the 316 SS cladding tube, (c) a neutron reflector (Ni rod) is placed on top of the insulator pellet and an Inconel spring is placed on top of the

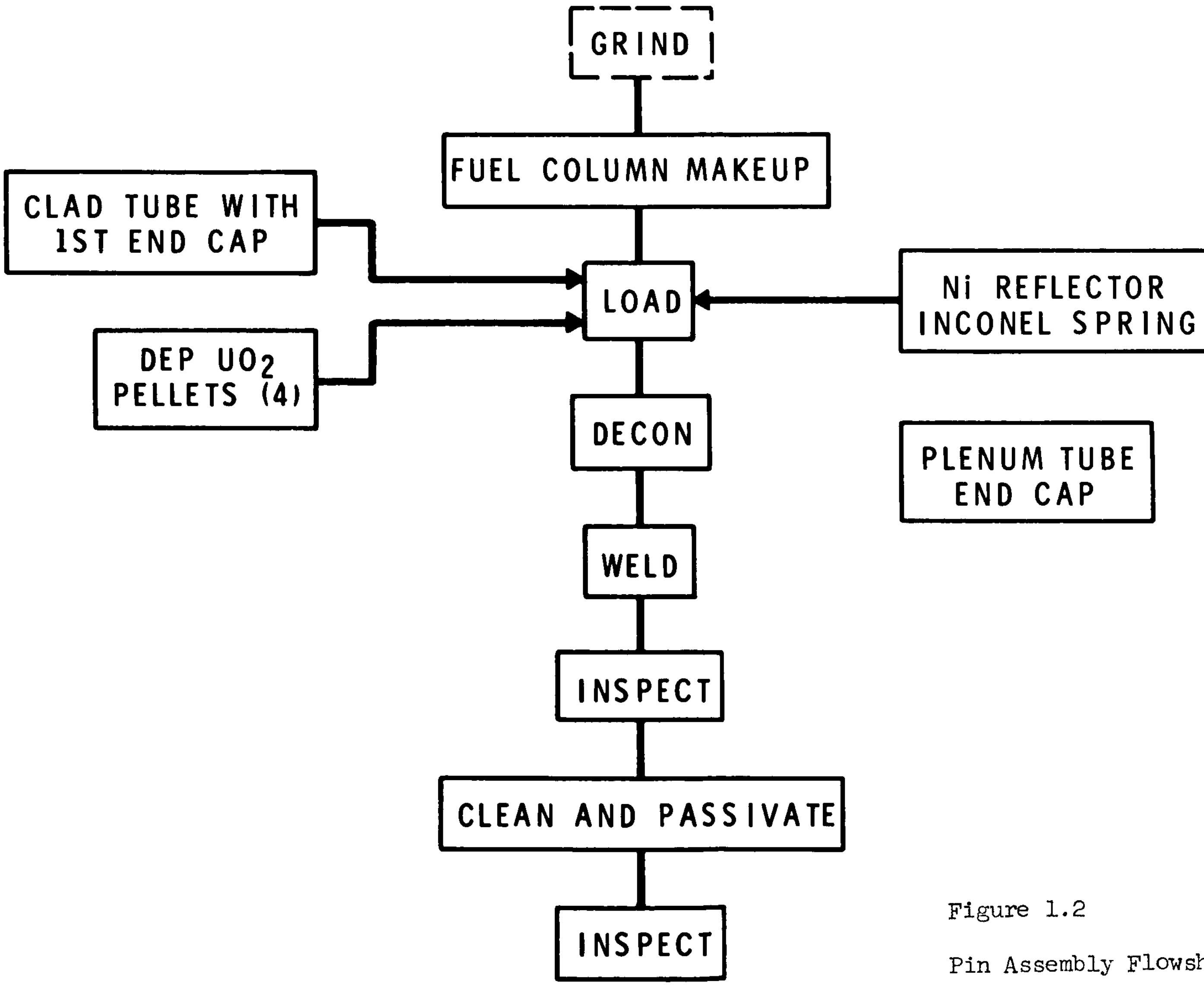

Figure 1.2

Pin Assembly Flowsheet

reflector. The reference design FFTF fuel pin is shown in Figure 1.3. The pin is then removed from the loading station for decontamination. Automation techniques for powder and pellet handling operations will be developed.

10. Decontamination

The outer surface and top 1/4 - 3/8 inch of the inner surface of the loaded fuel tube are decontaminated to standard acceptable levels of $<$ 500 d/m smearable.

Outgassing plugs are placed in the end of each pin and the pins are transferred to the welding box.

11. Welding

The fuel pins are TIG welded in one atmosphere of helium. After pumping down and backfilling the welding chember, the offgas plug is removed and a plenum tube (extending above the fuel tube) is inserted. The end cap is pressed and held down against the plenum tube, which in turn compresses the Inconel spring. This prevents subsequent movement of the fuel column. The end closure is then welded. A welding development program is in progress to assure the consistency and inspection methods for high quality welds.

12. Inspection

Following alpha monitoring of the weld and a visual check, the pins are inspected by the following tests: (a) weld dimensions and radiography, (b) pin radiography, (c) leak test, (d) gamma scan, (e) pin dimensional test.

The pins are cleaned and passivated in nitric acid.

13. Storage

Pins are again checked for surface contamination, individually sealed in plastic tubing, and stored in fuel transport boxes for wire wrapping and assembly of the subassembly.

14. Clad

The current reference cladding material is 316 SS. However, initial development work and irradiation tests are being conducted with 304 SS. In addition to fuel development, an extensive cladding development program is also in progress. The cladding development activities are being emphasized in both the cladding process development and irradiation testing phases. The main process activities are concerned with the sources of and detection of defects in the tubing and the sensitivity of the nondestructive testing methods to be used. The detailed methods of producing cold worked cladding, its effects on performance, and the mechanisms of clad swelling are being investigated.

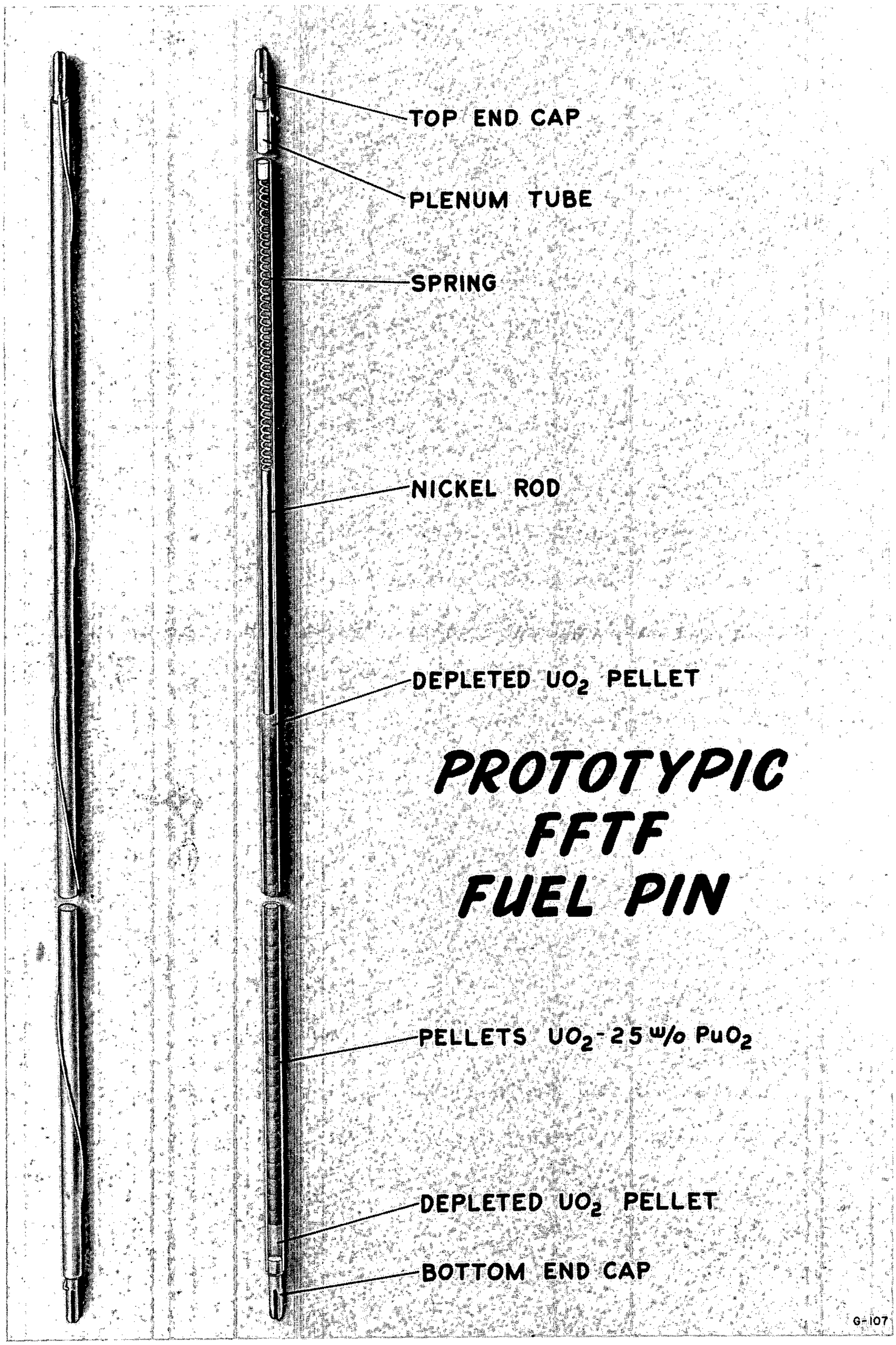
TOP END CAP
PLENUM TUBE
SPRING
NICKEL ROD
DEPLETED UO_2 PELLET
PROTOTYPIC
FFTF
FUEL PIN
PELLETS UO_2-25 w/o PuO_2
DEPLETED UO_2 PELLET
BOTTOM END CAP
G-107

Figure 1.3

15. Subassembly

The wire wrap concept is the reference pin design. The subassembly assembly operation starts with the wire wrap and is completed with the attachment of the flow duct. The wire wrap is welded to each end of the pin. The pins are assembled in strips which are then welded to a grid spacer to form the 217-pin subassembly. The active development program in this area includes wire wrap versus grid spacers, fretting and wear, personnel exposure to gamma and neutron hazards, thermal hydraulic characteristics, weld quality verification, and dimensional tolerance buildup of individual components. The Pu^{241} handling hazard is relatively new and will become increasingly significant as the Pu^{241} content increases. The conceptual subassembly is shown in Figure 1.4. The assembly of the first test subassembly is shown in Figures 1.5, 1.6, and 1.7.

16. Fuel Suppliers Program

The anticipated schedule for the fuel supplier qualification program and the NPTF-FTR core fabrications are illustrated in Figure 1.8. This schedule reflects the proposed AEC guidelines for procurement of test and research reactor fuels.

The overall vendor qualification program is divided into three phases.

a. Development Phase - To set up equipment and develop process capabilities to enter the latter two phases. (About April, 1968 to a date yet to be decided.) Emphasis is placed on analytical chemistry. Industry must recognize the importance of this analytical effort and should increase staff and facilities accordingly.

b. Prequalification Phase - Fabrication of prototypic pins October, 1968 to July, 1969.

c. Qualification Phase - Fabrication of ~ 1000 prototype FTR fuel pins for the NPTF, September, 1969 - April, 1970.

Fabrication of the NPTF core is to begin about September, 1969 with the qualification phase leading into the full NPTF core fabrication ~ April, 1970. This initial FTR core fabrication will begin about July, 1970 and be completed in November, 1972.

B. IRRADIATION TESTING PROGRAM

The principal objective of the irradiation testing program is to demonstrate reliable performance of the reference design FTR driver fuel. Secondary objectives are (1) to provide a broad base of statistically significant irradiation experience within a range of fuel parameters that will permit interpolation of the data obtained to optimize the reference FTR fuel design, (2) to provide the irradiation experience with mixed oxide fuel necessary for the development of analytical performance models to be useful for the design of LMFBR fuel, (3) to provide data which will permit assessment of the nonstandard operating condition (transient)

PROTOTYPIC DRIVER FUEL SUB-ASSEMBLY

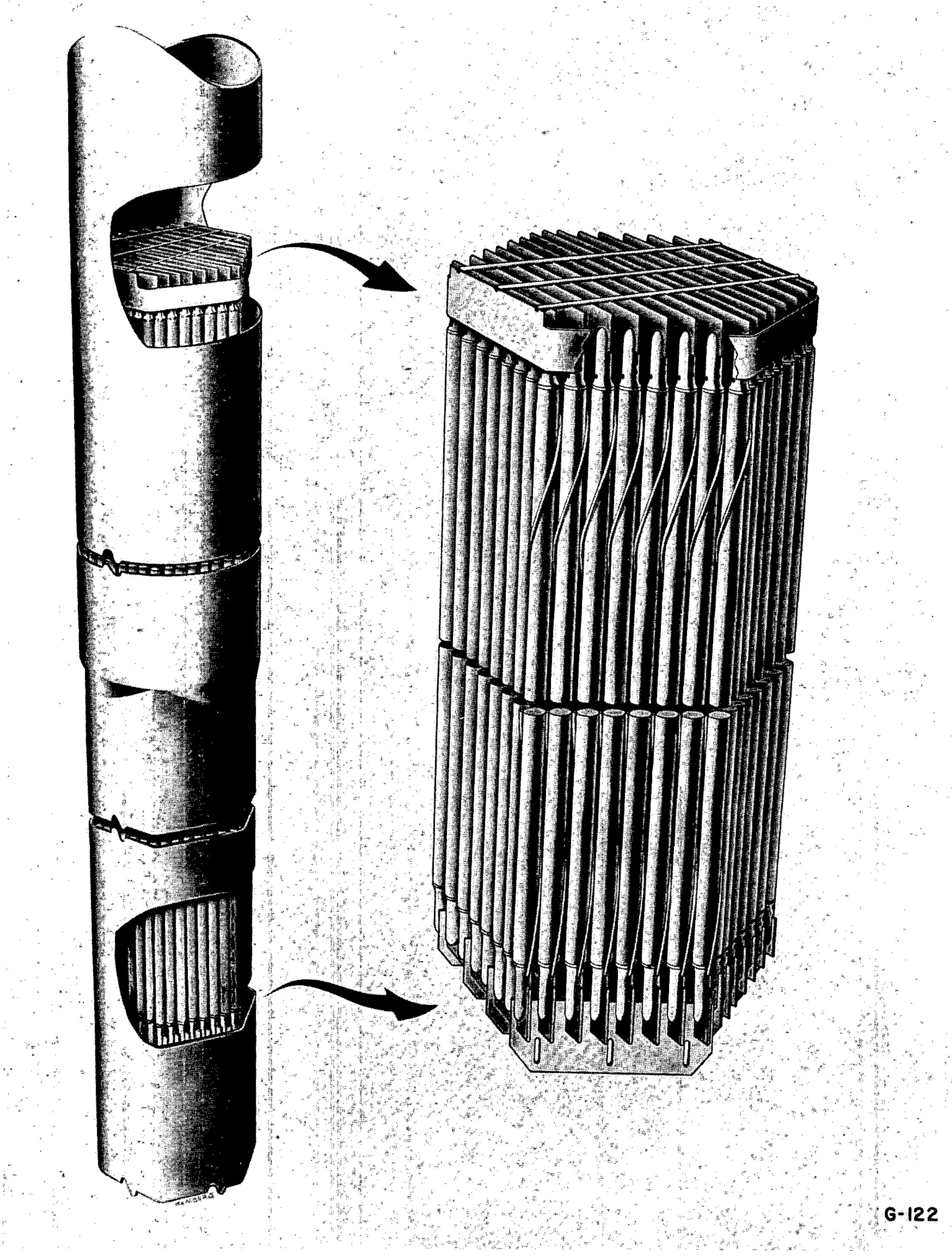

Figure 1.4

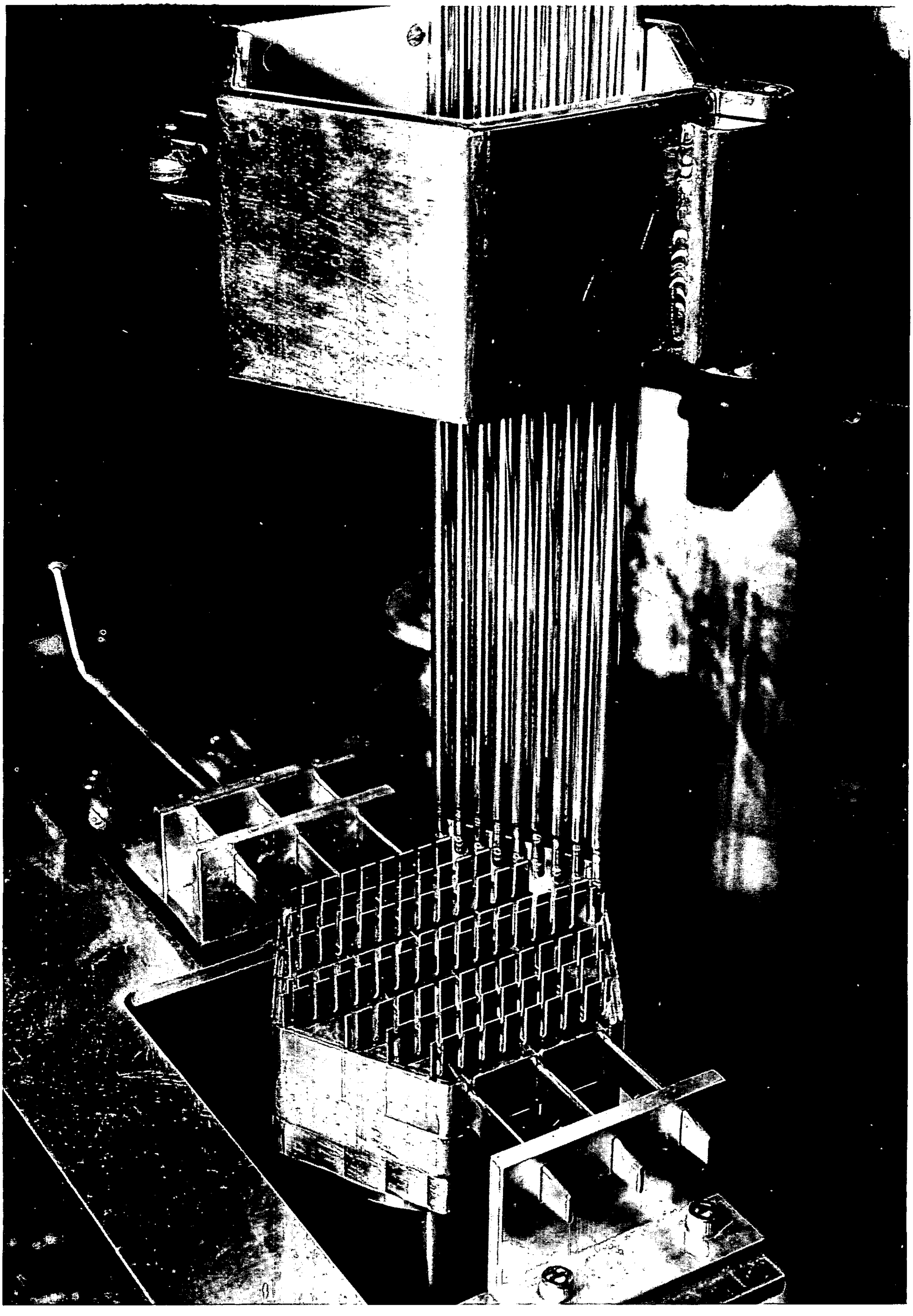

Figure 1.5 First Row of Pins in Subassembly

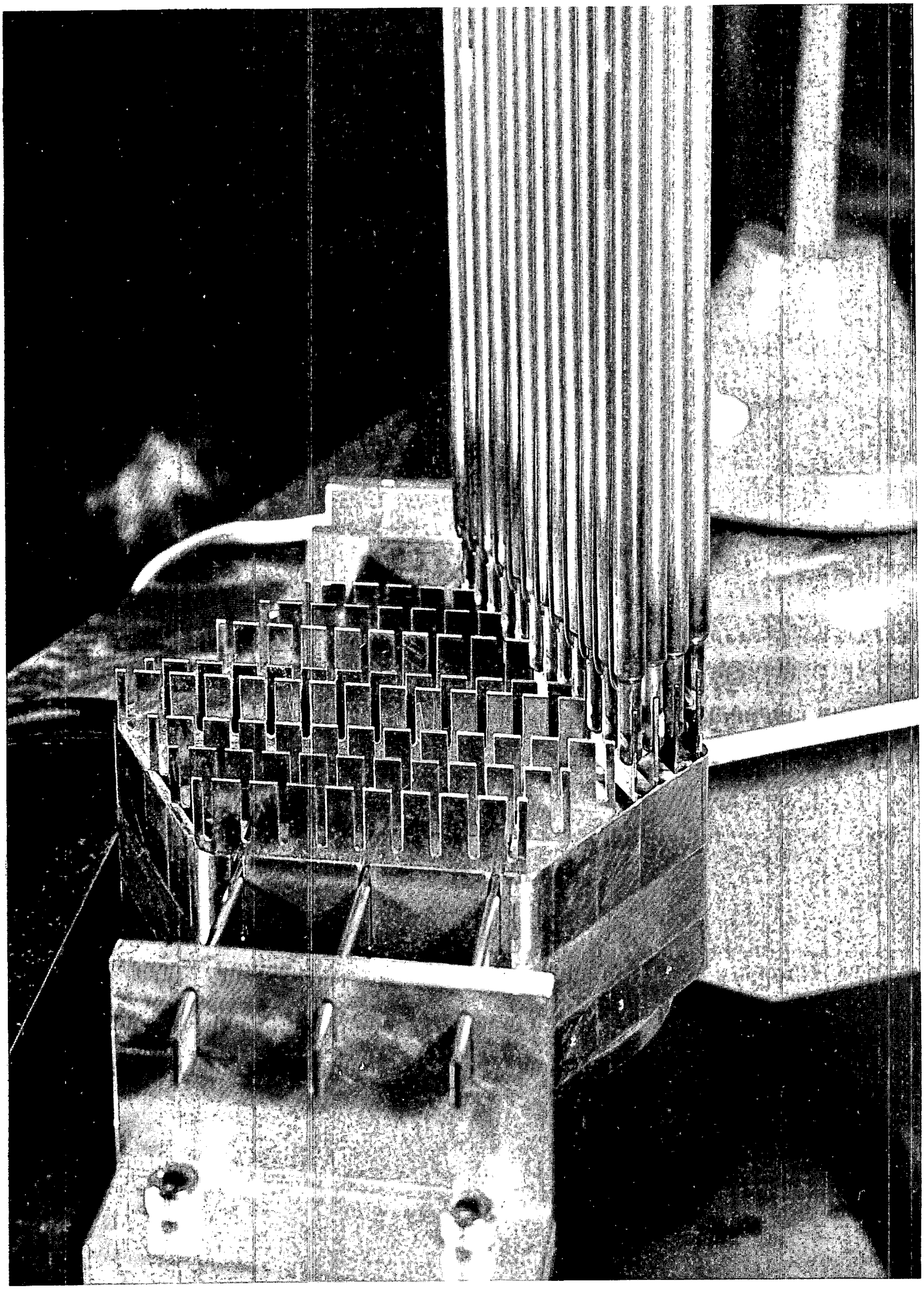

Figure 1.6 Fourth Row of Pins in Subassembly

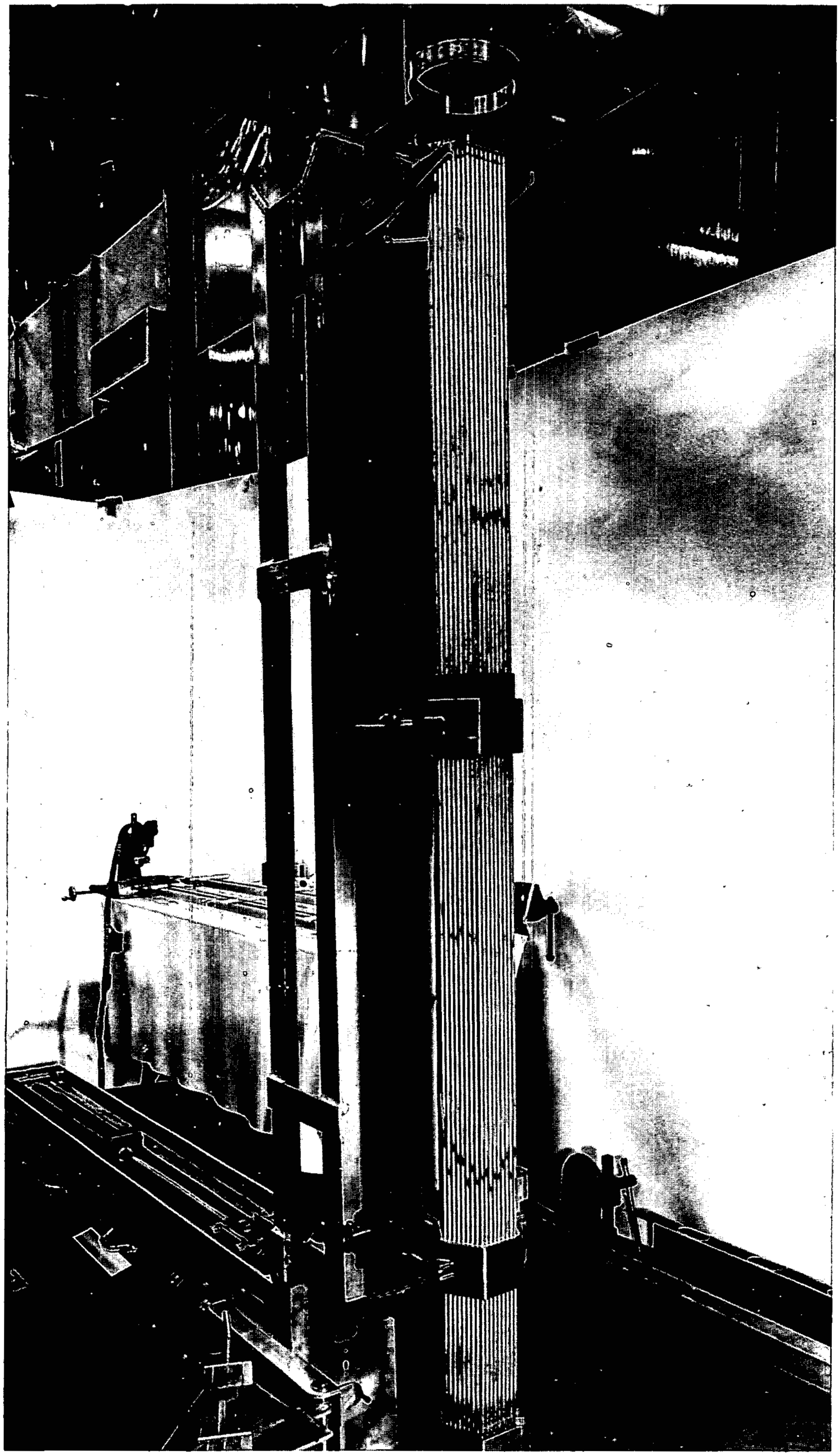

Figure 1.7 Completed Subassembly

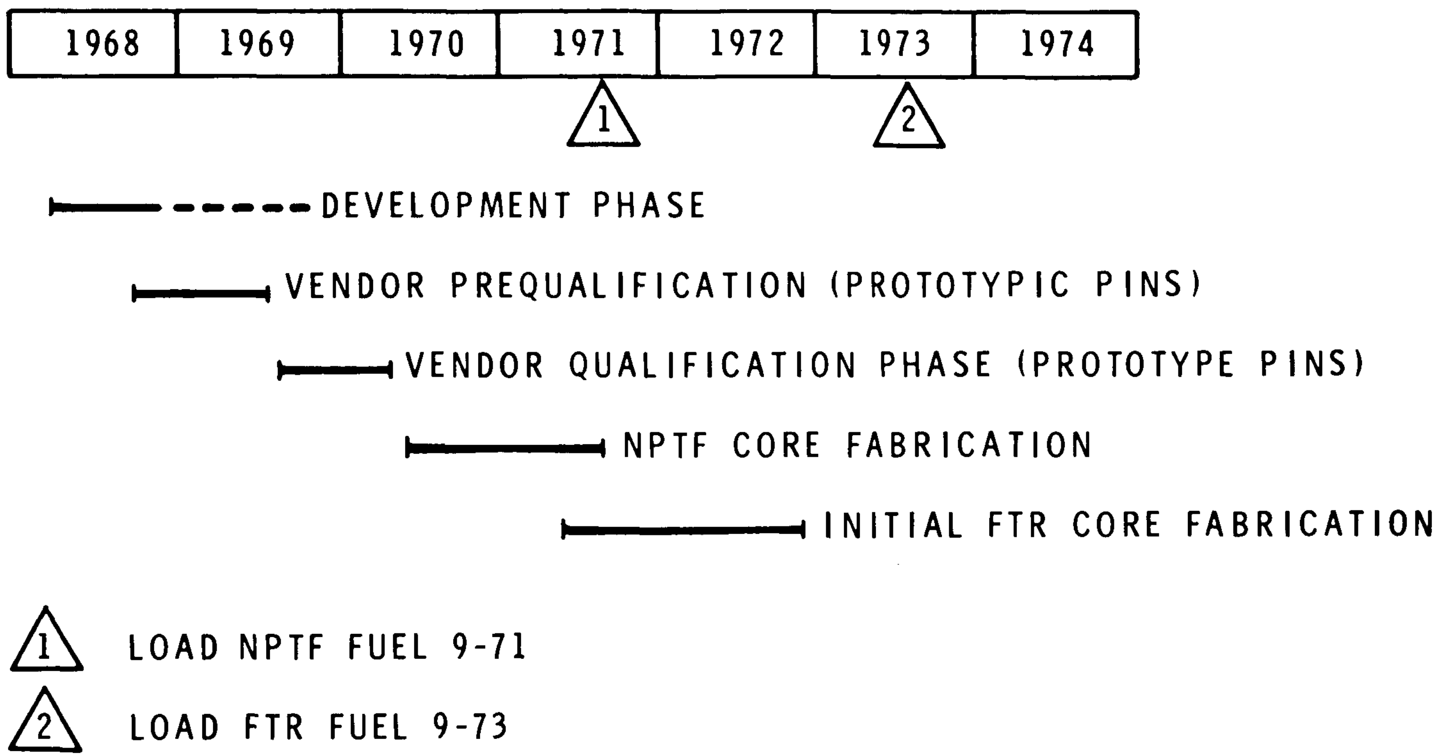

Figure 1.8

FFTF Fuel Fabrication Schedule

behavior of the fuel and cladding. Specifically, the transient failure thresholds and mechanisms are to be established for the reference design FTR fuel in support of core design safety analysis, (4) to provide data on the behavior of fuel typical of that anticipated for prototype fast ceramic reactors.

The irradiation testing program for the development of FTR driver fuel is an integrated program which both complements irradiation test results from the U.S., U.K. and French fast reactor development programs and the process development program. These programs have provided the preliminary information from which the reference FTR driver fuel has been designed. (Figure 1.3)

As a consequence of a lack of sufficient information concerning the behavior of fuel as a function of smeared density over the range of interest to the FTR driver fuel, alternate reference fuel pin parameters have been established as shown in Figure 1.9. These alternate parameters are incorporated in the FTR driver fuel irradiation testing program. The statistical matrix of these tests emphasizes the reference design but the alternate tests will also provide statistically significant data for evaluation. In this way, it will be possible to develop a reference fuel which will meet FTR requirements and provide a broader base for the interpretation of the irradiation test results that will be obtained.

1. Thermal Flux Irradiation Tests

Irradiations are being conducted in the MTR/ETR on mixed oxide fuel specimens with varying oxygen to metal ratios. Specifically, fuel specimens comprising 13-1/2 inches of UO_2-PuO_2 fuel, clad in 304 SS, and having oxygen to metal ratios of 1.94, 1.96 and 2.00 are being irradiated to high burnups (50,000 and 100,000 MWd/tonne) to assess the effect of this important fuel characteristic on the behavior of the fuel at these high exposures.

Irradiations being conducted in the GETR consist of prototypic FTR-length fuel specimens (approximately 32 inches fuel column length). These irradiations will provide data on the steady-state irradiation behavior of full length fuel columns. These tests are considered to be extremely important to the FTR fuel development program, as recent results from other programs have indicated that the length of the fuel column may significantly affect the swelling behavior of the fuel pin. In addition to providing steady-state irradiation data from long fuel columns, the test capsules were designed to be directly reencapsulated for transient irradiation in the TREAT facility. Transient testing of the irradiated fuel specimens will provide data on the transient failure thresholds and failure mechanisms of the reference FTR full length specimens. Here again, preliminary data indicate that the length of the fuel column may be a significant factor in determining the transient behavior of the fuel pin.

2. Fast Flux Irradiations

The bulk of the FTR fuel development irradiation testing program involves irradiations of both unencapsulated and encapsulated fuel specimens in the EBR-II fast reactor.

DESIGN	PELLET DENSITY, % T.D.	FUEL-CLADDING DIAMETRAL GAP, MILS	COLD FUEL SMEARED DENSITY, % T.D.
REFERENCE	93	6	88
ALTERNATE #1*	96	6	85
ALTERNATE #2	90	6	85
ALTERNATE #3	92	8	85

*ANNULAR PELLET; ID = 0.054 INCH

Figure 1.9

Reference FTR and Alternate
Fuel Design Parameters

Some encapsulated fuel pin irradiations currently in EBR-II include fuel specimens that contain fuel axial motion restrictors. Following completion of steady-state irradiation in EBR-II to burnups of 10,000 and 50,000 MWd/tonne selected pins will be reencapsulated and transient irradiated in TREAT. Additional encapsulated fuel pin irradiations are planned to evaluate the performance of mixed oxide fuel to burnups on the order of 100,000 MWd/tonne. Further, as deduced from a recent review of the available fast flux irradiation data for mixed oxide fuel clad in austenitic stainless steel, it is obvious that not enough attention has been paid to the effect of added cladding thickness on restraint of fuel swelling and growth. Accordingly, encapsulated fuel pins with different cladding thicknesses are planned for irradiation in EBR-II.

The heart of the FTR fuel development irradiation testing program is in planned irradiations of unencapsulated fuel specimens in the EBR-II. This plan requires twelve 37-pin subassemblies. The first three subassemblies, scheduled for insertion in mid-1968 are principally fuel development tests. That is, they incorporate fuel parameters based on the aforementioned reference fuel design and alternate fuel designs. Except for the fuel parameters, these tests will not be prototypic of the FTR pin design. Three subassemblies are required in this series of tests to cover the range of operating conditions anticipated for the FTR driver fuel; i.e. at linear heat generation rates of 6 kW/ft, 10 kW/ft, and 14 kW/ft. Uranium in these mixed oxide pins will be normal, 45% ^{235}U, and 93% ^{235}U, respectively. The normal U enrichment fuel irradiation tests are of particular importance to approach prototypic FTR cladding fluence-fuel burnup ratio and fuel chemistry.

A second set of three subassemblies will incorporate fuel pins which, except for fueled length, are prototypic of the FTR driver fuel pin design. Obviously, the 13-1/2 inch core length in EBR-II prevents irradiation of the reference FTR 32-inch fuel column. Consequently, the difference between 32 inches and 13-1/2 inches will be made up by depleted UO_2 pellets in the EBR-II specimens. All other aspects of the fuel pins will be representative of the FTR fuel pin reference design; internal hardware, plenum size, nickel reflector, etc.

A third group of three subassemblies to be proposed for irradiation in EBR-II will use a specially designed subassembly that incorporates as many of the design features of the reference FTR fuel subassembly as possible. For example, the pin support spacing system and the pitch-to-diameter ratio will duplicate that of the FTR reference assembly. Again, irradiations at three levels of linear heat generation rates are required. These tests are intended to constitute proof tests of the reference FTR subassembly design.

A fourth set of three subassemblies incorporating the reference FTR assembly design features will also be proposed for irradiation in EBR-II. The principal parameter in these tests, however, will be the amount of cold work in the 316 SS cladding. It is currently planned to evaluate three levels of cold work in these tests; 5%, 10% and 20%. The incentive for conducting thest tests is principally based on the observations from the U.K. data that cold worked cladding may resist fuel swelling and growth induced deformation of the cladding and also may be more resistant to swelling of the cladding. This latter phenomenom is a relatively new observation and has major implications to the fuel design criteria and the irradiation test program.

Provision has also been made in the fast flux irradiation program to irradiate fuel specimens in the Enrico Fermi reactor when that facility is returned to operation. These irradiation tests will also be of great significance to the FTR fuel development program because, in this reactor, it would be possible to irradiate prototypic FTR length pins.

The program has also made preliminary provisions for the irradiation testing of fuel pins obtained as part of the fuel supplier fabrication qualification program.

The FFTF fuel development program just described represents active programs and the present PNL/AEC planning. It is subject to modification on the basis of schedule, cost, concept, and available test facilities. This program is a key part of the integrated USAEC-LMFBR fuel development effort. The results obtained will substantially extend fast reactor mixed oxide fuel technology and provide a strong industrial fabrication base for the development of demonstration and commercial fast breeder reactors.

FUEL ELEMENT DESIGN AND CLADDING MATERIAL DEVELOPMENT FOR HIGH PRESSURE COOLANT FAST BREEDER REACTORS

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The Swedish fast reactor program has so far been limited to comparative studies of different coolants. Problems of critical nature have been investigated in some detail. Calculations of creep collapse of canning tubes have been performed with a method that permits relaxation and thermal stresses to be considered. Criteria such as creep due to internal overpressure at end-of-life conditions and short time failure due to core depressurization or shut-down have also been investigated. Fuel viscosity is believed to be very important in connection with swelling and an irradiation experiment to elucidate this is presented. A new series of canning materials for fast reactors is described.

INTRODUCTION

Sweden is of course aware of the fact that fast reactors will play an important role in the future but major efforts towards the development of such reactors have not yet started. Nevertheless, the need has been felt to increase the knowledge of the problems involved. Therefore work was started about two years ago in cooperation between AB Atomenergi and the ASEA Company with the purpose to compare different fast reactor systems. The first phase of this comparison has now been accomplished. It has of course not been possible to penetrate all areas in detail. The work has been concentrated on such problems that are believed to be critical. Fuel and cladding certainly belong to these critical areas.

The major efforts have been devoted to the steam-cooled reactors. This does not mean that Sweden has an a priori preference for this coolant. Further work must be done before a definite choice can be made.

Most of the work presented in this paper is based on the steam-cooled study but this is not thought to limit its applicability since the problems and methods are of a general nature. For this reason no direct reference will be made to the steam-cooled design study. It is mentioned only as a background for the application of the methods.

BASIC IDEAS

Swelling represents one of the most critical limitations for fuel performance in fast reactors. The problem also exists in thermal reactors, especially for the high peak burn-ups now being considered, but it is much less critical. It is well known that measures such as low-density fuel, annular or dished pellets and high external restraint to a certain extent can limit major geometric changes. It is, however, not possible to completely suppress such deformations except for cases where the fuel temperature throughout the pin is sufficiently high.

In the absence of adequate knowledge of fuel mechanical properties it is possible to visualize two extreme cases. In the first one the fuel is so rigid that it cannot really be plastically deformed.

The swelling, if any, will then be "irresistible" and, in the case of a fuel pin, cause diametral increase. This will strain the cladding and ultimately crack it, if it is brittle, or obstruct the coolant sub-channels. The second case represents a fuel that has a very low viscosity. The swelling will then be pushed towards the central hole which is formed during the irradiation or which may exist already from the beginning. Initial porosity will also be filled effectively. If, as is the case in the steam-cooled fast reactor, the coolant pressure is high, then there will even be a risk for ovalization and partial collapse of the pin if the cladding is weak. None of these two extreme conditions represents the true fuel behaviour but unfortunately enough is not known about fuel viscosity in-pile to permit good guesses. As the fuel temperature in fast oxide fuelled reactors tends to be quite high, even in the outer rim, there are reasons to believe that the fuel can flow to a great extent. A few data that can be interpreted in this way can be found in the literature[1,2].

Based on this discussion we can define two different fuel cladding design concepts. The first is free-standing where the cladding does not have to rely on support from the fuel and which, as will be seen, can be accomplished for the steam-cooled reactor by artificial means such as prepressurization of the pin. The second is the collapsible design which must rely on support from the fuel. The latter concept is completely impossible if the fuel is too viscuous. Even if the fuel is rigid, there are several serious difficulties with this concept.

FUEL ELEMENT DESIGN

In the case of steam-cooled reactors the coolant pressure will be very high. The mechanical problems for the cladding are thus very pronounced. For this reason it was considered necessary to devote some work to a detailed analysis of the creep of slightly oval tubes under external overpressure. Published work, for instance that of HOFF et al.[3], did not, according to our view, sufficiently well represent the true conditions since relaxation and thermal stresses are not generally considered.

In this paper only some characteristic features of the method will be presented. The work represents an extension and modification of that presented at last years Topical Meeting on Fast Reactors in San Francisco. A slightly oval tube is considered and its circumference is represented by a polygon of an arbitrary number of straight elements which generally are taken shorter than the wall thickness. An external pressure and a thermal gradient are imposed and this will set up a stress and strain pattern which can be calculated. This state is then introduced as the initial boundary condition in the creep equations (Norton's law with certain modifications) and the changes in stresses and strains with time are calculated. They can be followed at any point on the outer as well as on the inner surface. The calculation may be interrupted at any moment and a new set of boundary conditions can be introduced, for instance to represent a shut-down, a transient etc. It is thus possible also to estimate the degree of plastic fatigue or similar since all strains can be followed.

For the purpose of fuel element design we have applied this method simply to find the thicknesses necessary to withstand the external pressure at given rating and temperature at the outer surface. Figure 1 shows as an example curves for three materials and represents 450 W/cm linear pin power, 6.50 mm outer pin diameter and 700°C outer surface temperature. The criterion for creep collapse was that the slope in the ovality-time curve should be three times its lowest value which, for an initial ovality of 0.02 mm, happened to occur at an ovality of about 0,06 mm after 13000 hrs, which was the time selected for the calculations.

This calculation gives a conservative result since it does not take into account the fission gas pressure build-up that tends to relieve the cladding stress. It is nevertheless evident that a prepressure must be applied for all three materials. The lower limit for this prepressure is thus given.

The next criterion to consider is the internal overpressure that reigns during shut-down or depressurization of the core. This can only occur at relatively low temperatures where creep does not contribute. This criterion gives an upper limit to the internal pressure at end-of-life.

In steam-cooled as well as in sodium-cooled reactors the internal pressure may be higher than the coolant pressure at end-of-life conditions. It is then necessary, however, to consider creep of the cladding in the opposite direction. This constitutes our third criterion.

All three criteria can be summarized in one picture as shown in Figure 2 which refers to Sandvik 12R72HV and specifically applies to a steam-cooled pin with the fission gas plenum at core inlet. If the initial internal pressure is lower than the curve A-B collapse due to external overpressure can be expected. This limit is conservative since it implies that the overpressure is constant throughout the fuel life. If, on the other hand, the final internal pressure is higher than the curve C-D, unacceptable outward creep will result. (The straight horizontal line represents the external coolant pressure.) If the curve E-F is reached at end-of-life, the cladding may burst at shut-down or depres-

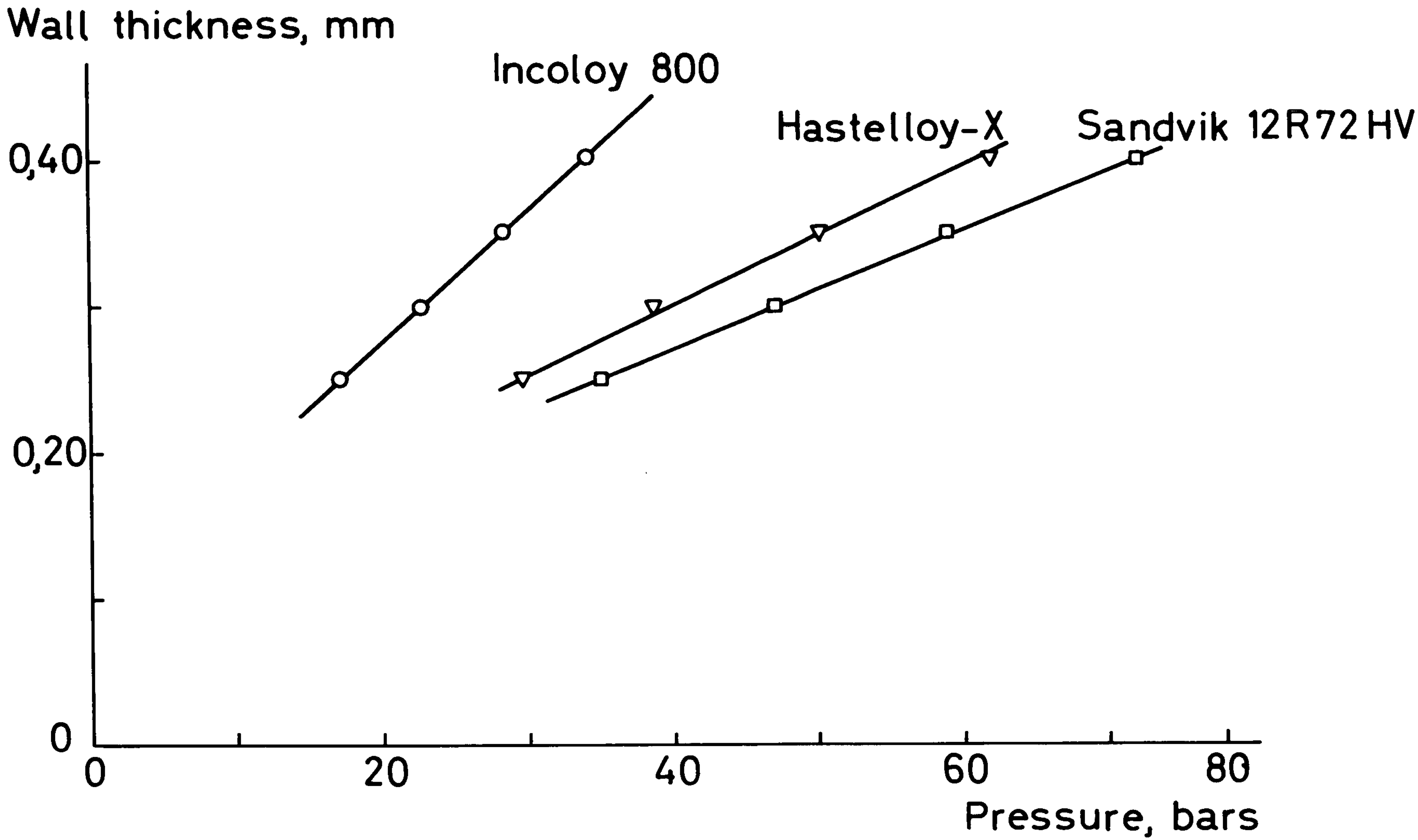

FIGURE 1. Wall thickness vs creep collapse pressure for three cladding materials. Linear pin power 450 W/cm, surface temperature 700° C, outer diameter 6,50 mm. The curves represent collapse in 13000 hrs as defined in text.

surization. It can be noted that the latter criterion only is applicable to very thin cladding at the selected coolant pressure of 130 bars.

These considerations permit the necessary plenum length to be calculated on the basis of an assumed fractional release of fission gases. In our study we found that it was economically feasible to select a plenum length such that the internal pressure never overrides the coolant pressure. This must be considered as an advantage over the sodium-cooled reactor with non-vented fuel since the capability of the cladding to restrain fuel swelling is fully retained and not partly spent in resisting an internal overpressure.

From Figure 2 it can also be seen that the coolant pressure can be increased from the selected value of 130 bars without changing the cladding thickness, provided the prepressurization of the pins is increased.

Thus our calculations indicate that it is possible, using the new class of alloys to be described below, to design the cladding for high pressure coolant fast reactors according to the free-standing concept, whereas for Incoloy 800, which is roughly equivalent from neutronics point of view, an excessive plenum length would be required. It should be remembered that most of the calculations are based on estimated in-pile mechanical properties for materials that are not well known. We are, however, confident that these estimates do not involve unreasonable assumptions, i.e. that the neutron irradiation will have little influence on the creep rates at the high temperatures under consideration.

Nevertheless, it is of interest also to study the collapsible concept. We have started theoretical work to elucidate this in some detail. The same methods for creep calculations as above will be used. Here the deformations must be calculated as well as their signs and magnitudes in different regions of the cladding. Since the cladding will follow the fuel much more closely than in the free-standing concept, the local deformations will be greater. The strain increments must be accumulated and correlated with some damage criterion which should combine the damage created by creep and fatigue respectively. A criterion has been proposed but we have not yet been able to verify it. It is simply a sum of two terms representing creep and fatigue damage as expressed by the life-fraction rule and Coffin-Manson's law respectively with correction factors to take account of the influence of irradiation.

FUEL

As can be understood from the introductory considerations, we regard fuel viscosity as extremely important. To study this property in some more detail we hope to be able to perform a series of irradiation experiments in the Swedish R2 reactor. A preliminary test was made in March 1968 but the post-irradiation investigations have not yet been completed.

The test pin had a diameter of 6 mm and a length of about 100 mm. It was loaded with enriched UO_2, sintered with a central hole. The cladding was very thin - about 0.16 mm ($\sim$ 7 mils) AISI 316 - and could not much protect the fuel from the external pressure which was about 80 bars. The

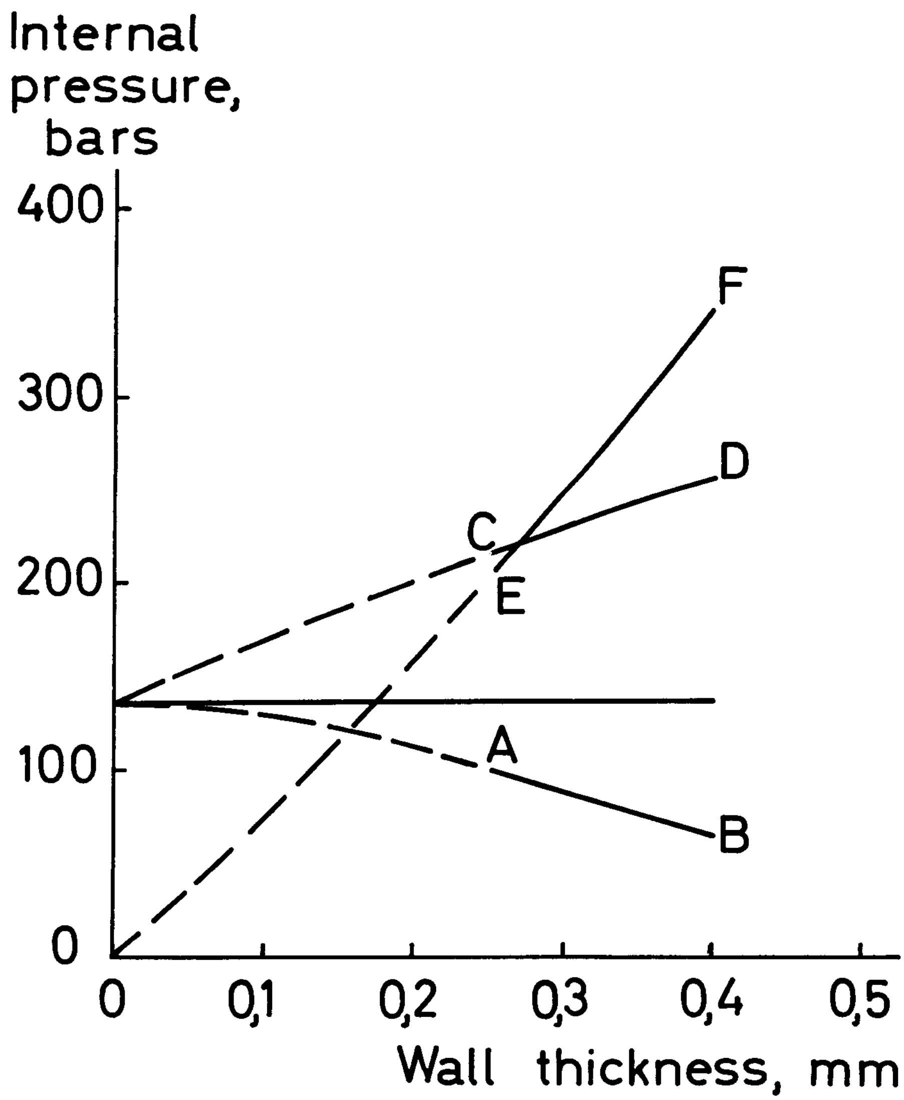

FIGURE 2. Summary of design criteria for cladding thickness. The curves represent calculations for 12R72HV

cladding surface temperature was planned to be 650°C and we hope to have attained temperatures on the fuel surface of the right order of magnitude. The rating should have been higher than 500 W/cm and the irradiation time was about 12 days. The test parameters are much more severe than we expect in a steam-cooled fast reactor. If the pin has retained its circular shape we have at least an indication that the chances are fairly favorable for the application of a collapsible design as far as fuel is concerned. However, the behaviour of mixed oxide under long time irradiations must be studied before any definite conclusions can be drawn.

CLADDING

Cladding for fast reactors must be strong and ductile at the same time and also compatible with the fuel and coolant environments. Most stainless steels and nickel base alloys that have been considered are more or less seriously embrittled by irradiation due to the formation of helium which, at high temperatures, coalesce to bubbles and partly concentrate in the grain boundaries. Under creep conditions most steels rupture in the grain boundaries and it is therefore clear that the helium bubbles seriously must reduce ductility under these circumstances.

The SANDVIK steel company has, some years ago, developed a steel for non-nuclear applications (superheat tubing for fossil fuelled steam generators) designated 12R72HV. Its composition is shown in Table 1. Creep testing of this alloy revealed ductility values as high as 50-60 per cent at 650°C and creep strengths much higher than in ordinary austenitic stainless steels, whereas the tensile strength is roughly equivalent to that of the latter. It has been shown that creep rupture in this alloy is predominantly transcrystalline which should reduce the embrittling effect of grain boundary helium bubbles. Since the high temperature irradiation embrittlement seems to be intercrystalline in nature, this fact was considered as very interesting. It was therefore decided to investigate the potential of this and similar alloys as cladding for fast reactors.

Boron has generally not been considered acceptable as an alloying element in core materials (except neutron absorbers) since, besides being a neutron poison, it causes helium formation. In the present case the situation is different because of the comparatively low (n,α) cross section for boron in high energy spectra. Moreover, recent measurements at AB Atomenergi[4] have shown that the (n, α) cross section for nickel is much higher than previously thought and on the basis of this it is found that the contribution to helium formation from boron can be neglected in the present case[5].

The fabrication properties of the 12R72HV alloy are similar to those of ordinary stainless steels and much more favourable than those of high creep strength alloys such as Inconel 625. It is expected that the same will be true for the modified alloys, discussed below.

We believe that the 12R72HV alloy has a great potential as cladding material for sodium cooled fast reactors. In steam environment the nickel content is, however, too low from the corrosion point of view. Therefore we have decided to investigate alloys with higher chromium and nickel contents. Table 1 shows five alloys which now are being studied.

As can be seen from the Table the steels are alloyed with titanium, molybdenum and boron. It is not yet clear why this type of steel behaves in such a ductile manner. It seems, however, probable that chromium carbides, which precipitate in grain boundaries, play an important role. One possible explanation of the good ductility may be that these carbides are effective in preventing grain boundary sliding. Titanium, on the other hand, contributes to the strength as it can be made to precipitate in the form of carbides or carbonitrides on dislocations. It can be expected that the relative contents of titanium and carbon (and also nitrogen) should be important. Therefore so-called over-stabilized as well as under-stabilized compositions are being investigated. Molybdenum is probably increasing the matrix strength.

The influence of structure and heat treatment on out-of-pile creep and other properties is now being investigated in some detail. Preliminary results indicate that the best creep properties are obtained if the alloys are finished in one of the following ways:

- After final cold reduction the steel is annealed at 1150^{o}C and rapidly cooled.

- The alloy is annealed at 1150^{o}C and rapidly cooled before the final cold reduction. It is thought that there exists a range of cold reductions that give optimum properties. Finally, the alloy is annealed at about 850^{o}C.

In the first case most titanium carbides are put into solution and they can reprecipitate during creep and thus contribute to strength. The second treatment results in a material that is recovered but not recrystallized. In this case the titanium carbides are already precipitated. Further investigations will show which treatment is to be preferred but there are at least two reasons to believe that the 850^{o}C anneal will be chosen. A technical reason is that it is easier to anneal cladding tubes at a lower temperature. From the irradiation embrittlement point of view it seems to be preferable to have a structure that is as stable as possible as far as precipitations are concerned and this is best fulfilled by the low temperature anneal.

Mechanical properties which are representative for the conditions the potential materials will have as cladding could of course not be presented at this time. To give an idea it can be mentioned that 12R72HV at 700^{o}C has a 10000 hours creep rupture strength of about 11 kg/mm^2 (16000 psi). This is much higher than for Incoloy 800 but lower than for Inconel 625. The creep ductility in the 1000 - 10000 hours range of rupture times is of the order of 30-60 %.

An extensive irradiation program is being started in the spring of 1968 and specimens will be irradiated in the Swedish thermal R2 reactor at 700^{o}C up to fast doses of about $3 \cdot 10^{21}$ n/cm^2 (> 1 MeV). Post-irradiation creep and tensile testing will be performed at various temperatures.

TABLE. Nominal chemical compositions of investigated cladding alloys.

Designation	Chemical composition, w/o					
	C	Cr	Ni	Mo	Ti	B
7XR72	0.05	19	34	0.9	0.45	0.008
12XR72	0.10	19	34	0.9	0.45	0.008
7X1R72	0.05	19	25	0.9	0.45	0.008
12X1R72	0.10	19	25	0.9	0.45	0.008
12R72HV	0.10	15	15	0.9	0.45	0.008

The corrosion properties have so far not been investigated in detail. We believe, however, that the compatibility with steam should be of the same character as for corresponding straight chromium-nickel alloys. We plan to investigate this further in high pressure steam and we also hope to test the low nickel alloys in a sodium environment.

The development and introduction of new cladding materials is no easy task and large testing efforts are needed before they can be safely used in reactors. Our program is in an early stage and many difficulties will probably appear before we can draw definite conclusions. We believe, however, that the road we have chosen will lead to improved cladding materials for fast reactors. As has already been pointed out, this belief does not only refer to high pressure steam- or gas-cooled reactors but also to liquid metal fast breeder reactors.

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SESSION IV

April 3, 1968

COMPONENTS FOR FAST REACTOR SYSTEMS

Chairman: G. Wensch*
U.S. Atomic Energy Commission

Local Co-ordinator: Eugene M. Simons
Battelle Memorial Institute

COMPONENTS FOR STEAM COOLED REACTORS - REQUIREMENTS & DEVELOPMENT STATUS

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INTRODUCTION

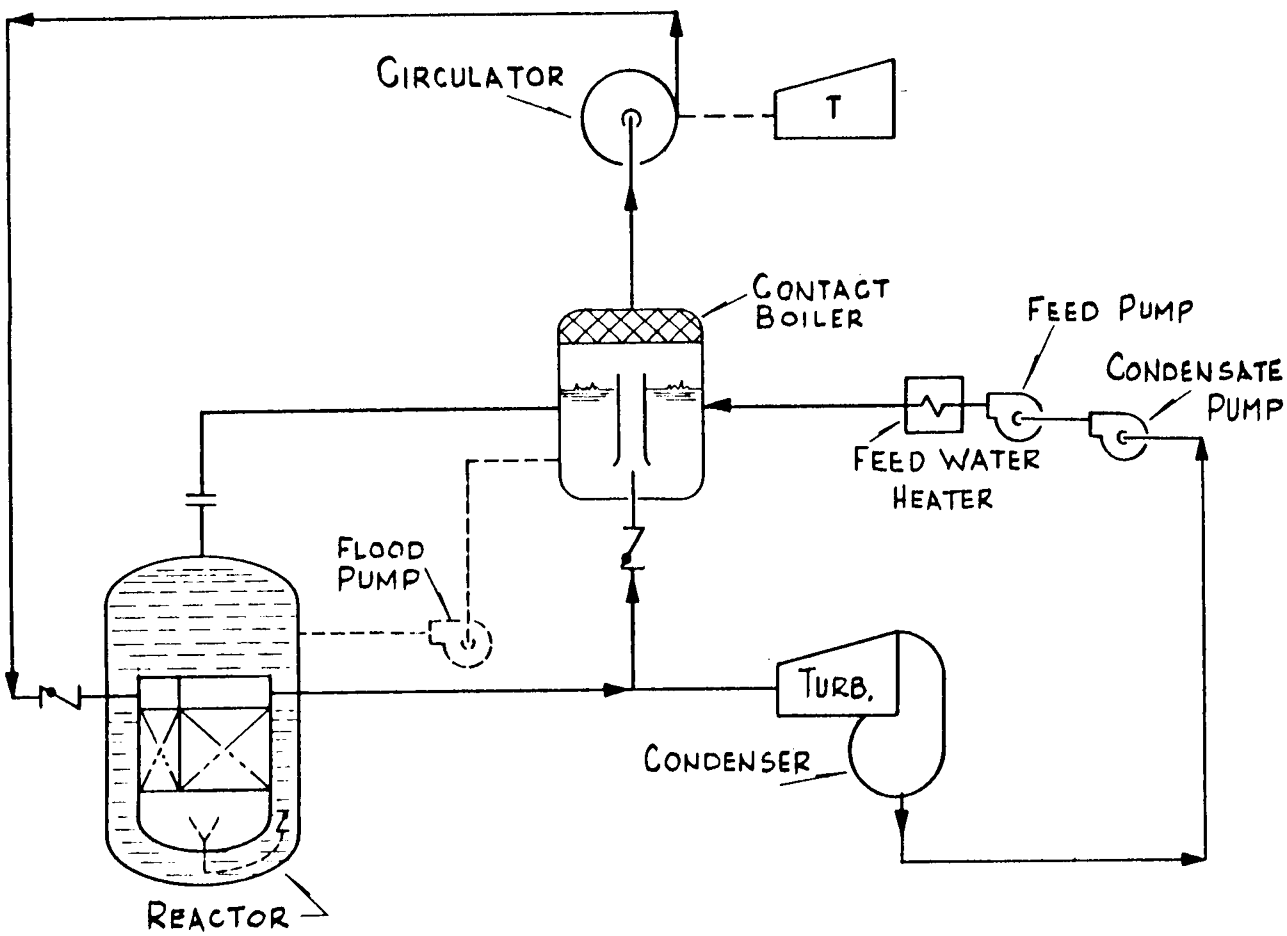
Development of a steam cooled fast reactor (SCFR) is a logical application of the extensive and well developed steam technology of fossil-fired and thermal nuclear power plants. A large industry exists for the manufacture of conventional steam cycle components, and the bulk of power utility operating experience has been obtained with steam as a process fluid. The steam cycle most ideally suited to the steam cooled fast reactor is, however, a relatively unfamiliar one requiring components such as steam circulators that are not found in today's product line of conventional nuclear and fossil-fired power plants. The success of any contemplated steam reactors such as a 50 MWe demonstration plant now under study by our company depends to a large degree on these components. For that reason a detailed consideration of the non-nuclear system components has been made and is summarized here.

Cycle Background

The coolant system of SCFR is generally based on a closed loop, direct cycle known as the Loeffler cycle. In this cycle, saturated steam is superheated in the reactor core and only a fraction of this superheated steam (approximately one-fourth) is used to drive the main turbine. The remaining fraction is used to generate dry, saturated steam in contact boilers by mixing the superheated steam in direct contact with the condensate returning from the main turbine condenser. The resulting saturated steam is then pumped back to the reactor core to become superheated and complete another cycle. A small portion of the steam generated is generally used to drive turbine-driven circulators which pump the saturated steam back to the reactor. A simplified diagram of such a cycle for SCFR is shown in Figure 1.

Initial research and development on this cycle was performed by Dr. Stephan Loeffler during the years 1923-1926, using a test plant erected at the Vienna Locomotive Works Company. This work led to the construction of the first large scale Loeffler system at the Vitkovice Mines, Steel and Iron Works in Czechoslovakia. During the 1930's and 1940's, a number of industrial applications of the Loeffler cycle were made, including central station plants, ship propulsion units, and steam locomotives, with most of the installations being in Central and Eastern Europe. These are listed in part in Table 1.

Aside from some of the components of the Loeffler cycle and the reactor core, the remainder of the SCFR equipment can be essentially of conventional power plant design.



SIMPLIFIED MAIN COOLANT FUNCTIONAL DIAGRAM - 50 MWe S.C.F.R.

FIG. 1

TABLE 1

SELECTED LOEFFLER CYCLE INSTALLATIONS

Installed At	Startup Date	Boilers: Number Installed	Feed Flow lb/hr	Pressure psi	Temp °F
Kraftwerk Karolinenschaft	1930	2	90,000	1760	935
Vitkovice, Czechoslovakia	1931	2	150,000	1850	935
Kraftwerk Trebovice	1933	3	200,000	--	-
Czechoslovakia	1938	1	-	--	-
	1939	1	-	--	-
	1949	1	420,000	--	-
Moscow, USSR	1934	2	300,000	1850	935
Brimsdown Power Station*	1937	2	210,000	2000	935
England	1942	2	250,000	1920	950
Leverkusen, Germany	1936	2	120,000	1850	935
Höchst, Germany	1936	3	90,000	1650	935
	1940	1	120,000	1650	935
Grand Couronne, France†	1938	1	140,000	1850	-
Grozavesti, Romania‡	1940	1	360,000	1850	-
Nihon Kasei, Japan	1939	3	-	1640	-
S.S. "Conte Rosso," Italy	1937	1	44,000	1850	890
Schwarzkopf-Loeffler Locomotive, Germany	1930	1	-	1700	890

*Running as of March, 1966

†Last spare parts in 1964

‡Running as of 1965

Steam Circulators and Drives

Large steam circulators have been built and successfully operated in the industrial installations mentioned previously. The first circulators were of the reciprocating type, later being succeeded by the rotating centrifugal type. Development of these circulators was guided by Dr. Loeffler in co-operation with the manufacturers, Escher-Wyss of Zurich, Switzerland.

As seen in Tables 1 and 2, the flow capacities of the circulators ranged up to 1,360,000 lb/hr of 2000 psi saturated steam with heads up to 100 psi. Single-stage overhung impellers were used, driven by steam turbines at speeds up to 9000 RMP. Labyrinth-type seals were used, with superheated steam at a pressure slightly higher than process steam pressure being injected into the seals to prevent leakage of the process steam through the seals. The bearings were oil-lubricated.

In recent years, small circulators have also been built for use in steam test loops with flow capacities ranging up to 70,000 lb/hr. These circulators have used both oil-lubricated and water-lubricated bearings with speeds ranging to 25,000 RPM. Both steam and high pressure water have been used as the sealing medium.

For use in SCFR's the circulators are required to pump saturated steam which is at pressures of 1500-2500 psi. Typical quantities would be 800,000 lb/hr for a 50 MWe plant or 6,000,000 lb/hr for a 1000 MWe plant, assuming three loops for the 50 MWe plant and six loops for the 1000 MWe plant.

Since the head that must be developed by the circulator is only the frictional flow loss in the system, the required differential pressures across the circulator are relatively modest, being in the order of 150 to 250 psi.

Designs currently being proposed for the circulators to be used in a 50 MWe demonstration plant utilize the technology that has been developed for BWR and PWR recirculating pumps. For example, one proposed design uses a single stage overhung centrifugal impeller supported inboard by a steam lubricated bearing and outboard by a conventional oil lubricated journal and thrust bearing.

The components of this proposed design that are common to a recirculating pump are the casing, water-cooled seals, impeller and inboard bearing. This is depicted in Figure 2. The differences are in the method of supporting the bearing by steam rather than by water, and the horizontal arrangement rather than vertical. A horizontal arrangement is required to direct couple to the driving turbine which conventionally is horizontal.

Another proposed design (Figure 3) is similar to the above, except that the single stage impeller would be a double flow type (back-to-back impeller) to reduce the load on the thrust bearing. Both journal and thrust bearings would be water lubricated in this proposed design.

A third alternate utilizes gas turbine compressor technology in that it uses a two-stage axial flow impeller mounted in an overhung arrangement. This impeller would basically be a modification of an existing aircraft gas turbine compressor stage. The bearings are proposed to be water lubricated.

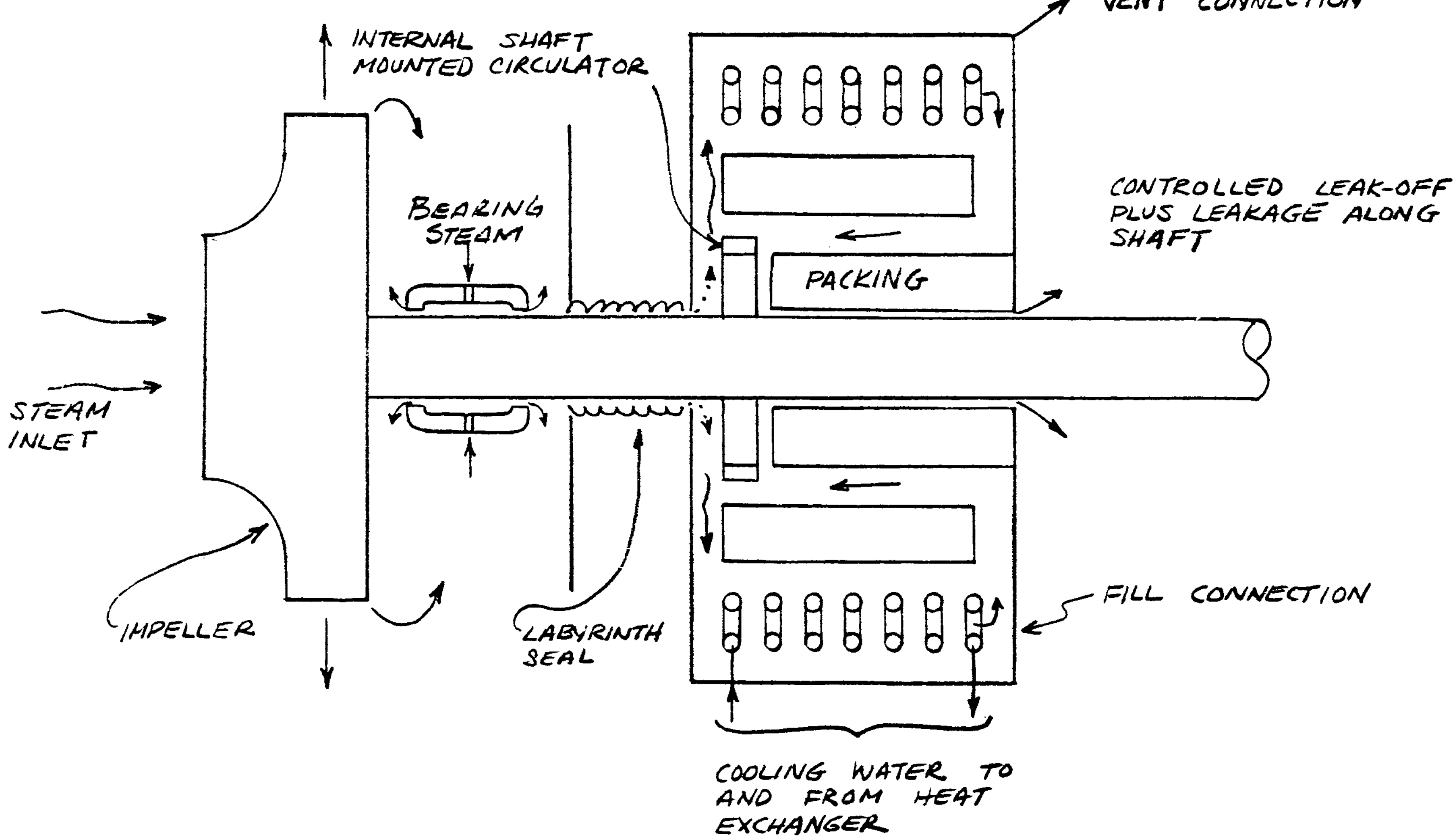
TABLE 2

RANGE OF STEAM CIRCULATOR EXPERIENCE

Manufacturer	Capacity lb/hr	Pressure psig	Temp °F	Pressure Differential psi	Impeller Diameter and Type	Impeller Speed rpm	Bearing Lube	Seal Type
Escher Wyss	160,000 to 1,360,000	1600 to 2000	900 to 950	40 to 105	9-1/2" to 18" centrifugal	2,000 to 9,000	oil	Various types used. Latest is superheated steam buffered.
Orenda Ltd., Canada	72,000	1600	650	350 (@ 800 psi inlet)	Regenerative	3,600	oil	Face type. Mechanical.
Orenda Ltd., Canada	70,000	1500	1050	100	5" centrifugal	25,000	water	Buffer water type. Backed by ring seals.

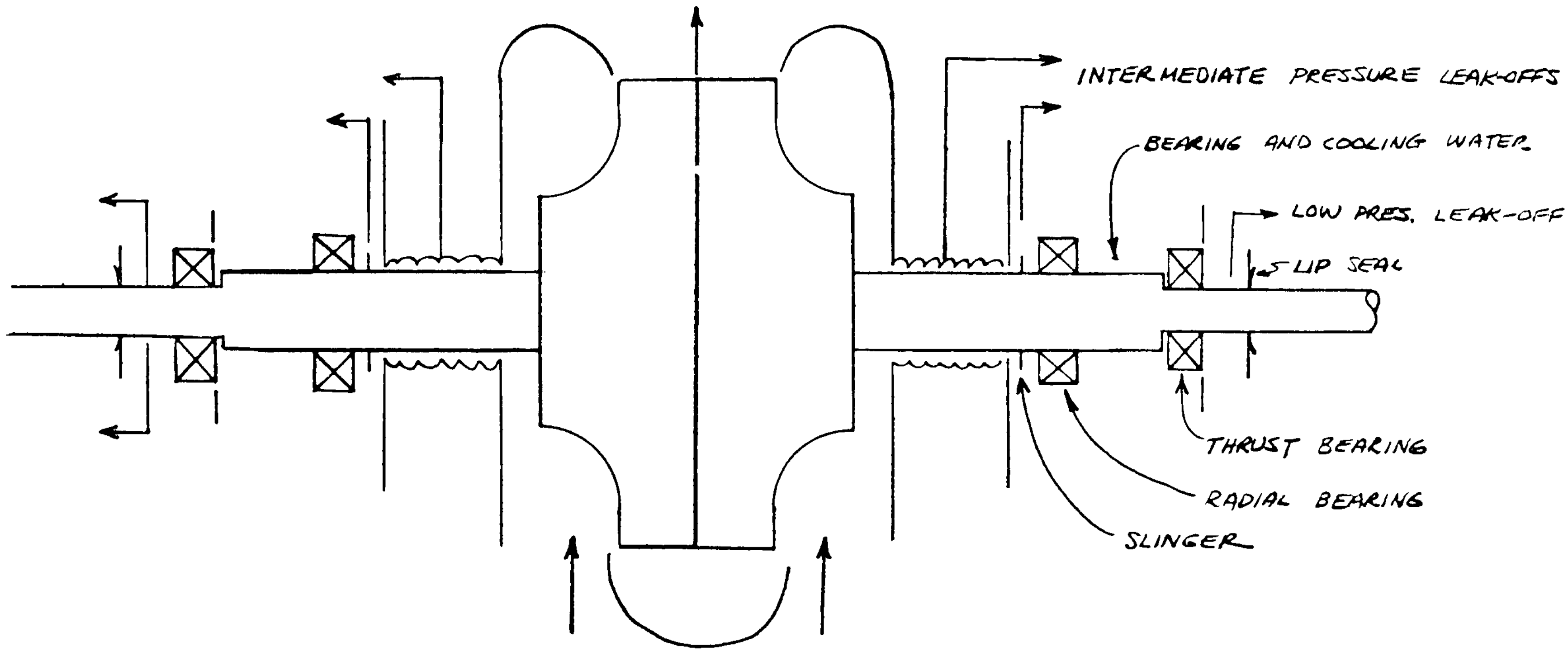
SKETCH OF A STEAM CIRCULATOR DESIGN FOR A 50 MWe PLANT

FIGURE 2



SKETCH OF A STEAM CIRCULATOR DESIGN FOR A 50 MWe PLANT

FIGURE 3



As can be seen, the design of steam circulators can be based on existing technology, at least in the case of relatively small sized SCFR plants. Difficulty in finding a design with operational history to back it up isn't encountered until scaling up to sizes larger than needed for the 50-300 MWe range or a change to a non-conventional bearing is attempted.

Reliability of the circulator, its driver, and its controls is of paramount importance in order to assure required steam flow through the core under all normal or abnormal operating conditions.

Seals are a very important facet of both the circulator and drive turbine design. Positive containment of the steam is required, either by hermetical sealing or by use of controlled leak-off systems, because the circulators and turbines must be capable of sustained operation even though the steam may be contaminated by radioactive material. A technique using clean steam with controlled leak-off appears to be operationally desirable. One such system is shown in Figure 4.

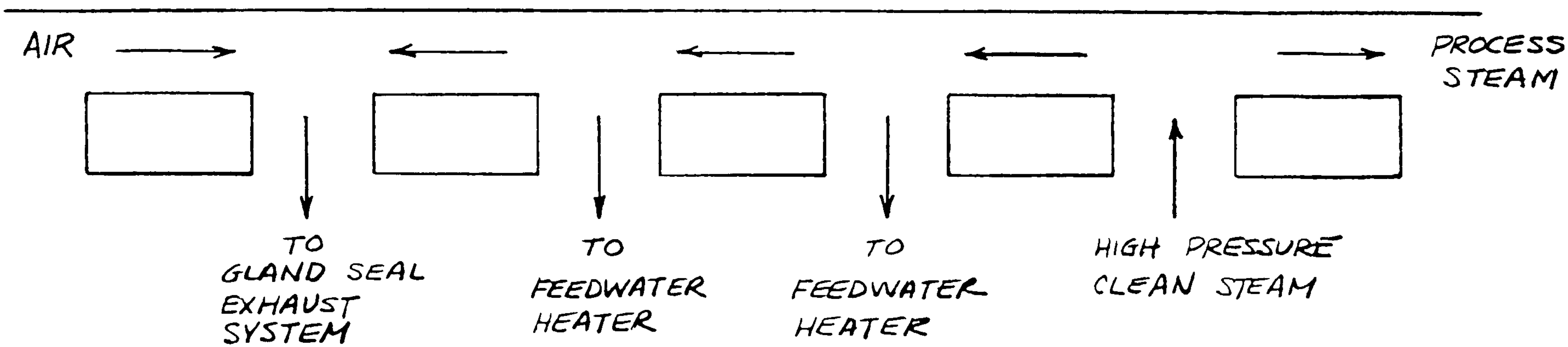
Since the steam circulator casing forms an integral part of the primary pressure envelope, it must be designed to meet the same strict integrity requirements as the reactor pressure vessel and piping.

To avoid any possible contamination of the steam working fluid, the bearing lubricant should be either steam or water. Use of either represents an area requiring development at the present time, hence an oil lubrication system may be used on early units if properly isolated.

The steam generating and drying equipment upstream of the circulators are required to provide a minimum steam quality of 99.9% to the circulators. This provides thoroughly dry steam to the core and avoids moisture difficulties in the circulators. However, the circulators should be capable of circulating steam with a quality as low as 80% (homogeneous mixture) for short periods of time ($\sim$ 30 minutes) without catastrophic failure of the impellers and bearings or loss of pressure-retaining capability in the event that excessive carryover from the boilers were experienced.

The design speed of the circulators should be such that it can be direct-coupled to a steam turbine eliminating any need for gearing. Use of steam turbines for driving rotating equipment such as the circulators and boiler feed pumps is a logical choice in the steam coolant cycle not only because of the availability of a steam supply, but because steam turbines have the following advantages over electrical drives:

1. The turbine drive can be independent of the electric supply system and therefore can possess a high degree of reliability.
2. Variable speed drive is much more easily obtained than with electric drives.
3. Conventional turbine rotational speeds are more in line with those required by the circulators (5,000-10,000 RPM); therefore, the need for geared speed increasers is eliminated.
4. A better cycle efficiency can be gained because there is less transfer loss than between the electric generator and the drive motor.



NON-CONTAMINATED SEAL ARRANGEMENT

FIGURE 4

The selection of turbine type as to superheated/saturated steam inlet, condensing/back-pressure is governed by the location of the drive turbine in the cycle. It is possible to locate the drive turbines in the cycle in several places. Some of the more reasonable locations, shown in Figure 5, are:

1. Downstream of the main turbine, using steam bled from a low pressure stage of the turbine. In order to keep the circulator running on a main turbine shutdown, an alternate line from the main steam header can supply superheated steam to the turbine by means of a dual-admission valve on the turbine. The steam discharged from the drive turbine would be condensed in the main turbine condenser. This turbine would be similar to those used for driving boiler feed pumps in conventional central stations.
2. An alternative to the one above is to route the discharge steam back to a lower pressure stage of the main turbine. In this case a bypass line would be required to send the steam directly to the main condenser on a turbine trip.
3. The turbine could be run on superheated steam exiting from the reactor, with an auxiliary line from the contact boiler discharge supplying saturated steam should there be a loss in the primary superheated steam supply. Again, one could either route the discharge steam directly to the main condenser or to a low pressure stage of the main turbine.
4. Superimposing the drive turbine on the main turbine. In this case all or part of the superheated steam would pass through the drive turbines before entering the steam chest of the main turbine. Again, a bypass line to the main condenser would be required.

There are many considerations that go into the selection of drive turbine location such as continuity-of-cooling requirements, quantity and size of pipe lines required, placement of main turbine-generator and condenser inside or outside of primary containment building, etc. A major consideration too, is the potential for contamination from radioactive steam. It is not, however, the intent of this paper to recommend the location of the drive turbines, but merely to illustrate some of the turbine types and their location which can be considered.

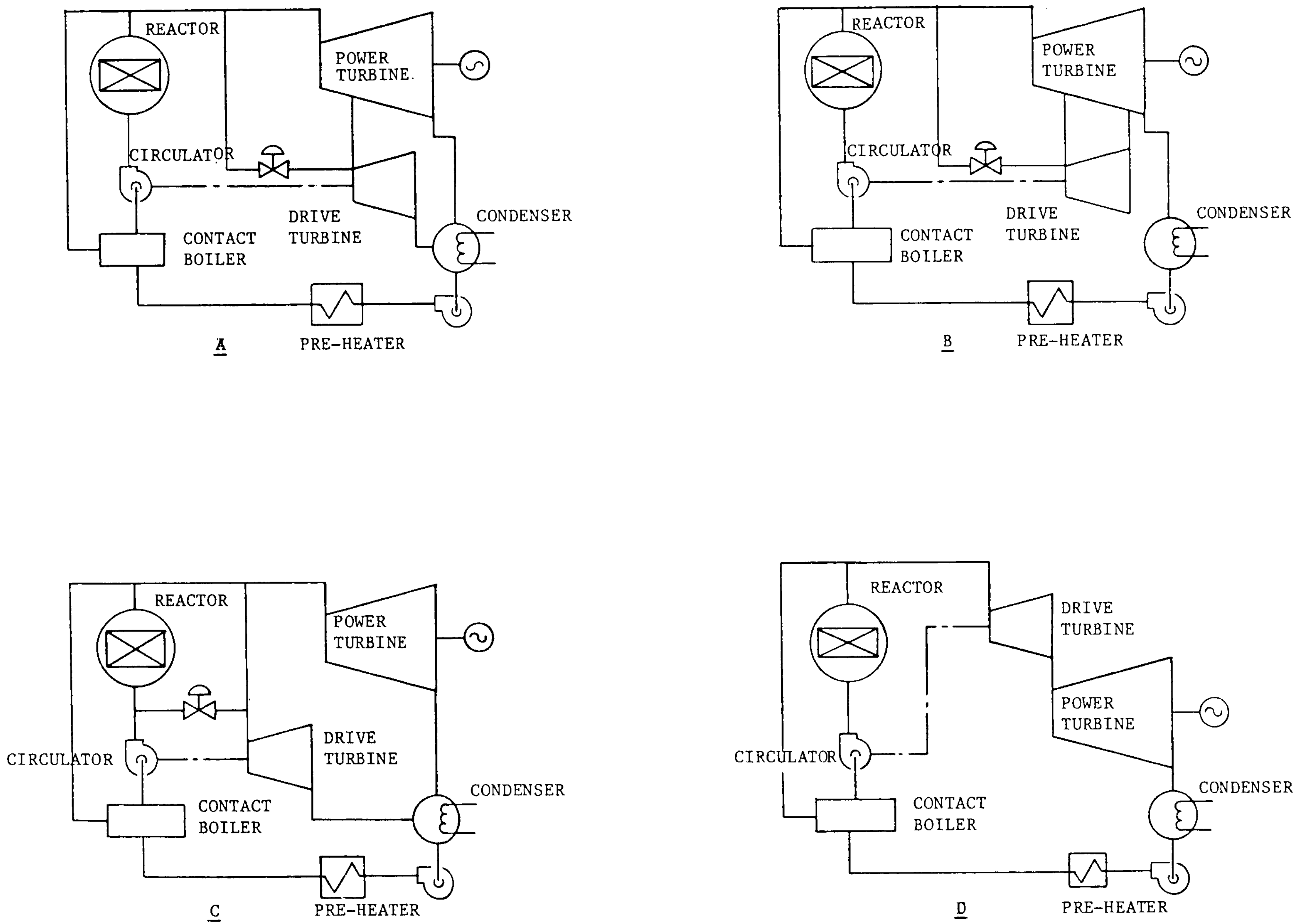
Contact Boiler

The contact boiler must be designed to desuperheat the reactor outlet superheated steam reliably to, or very near to, saturated conditions. It must be relatively insensitive to system upsets, such as a feedwater failure, and must continue to provide saturated steam while operator corrective action is taken following the upset. Drying of the steam leaving the boiler must be very effective to obtain an exit quality of about 99.9 w/o steam to provide good decontamination factors and prevent undue moisture from entering the downstream circulators and the reactor core.

Large contact boilers have also been built and successfully operated in the industrial installations mentioned previously. These have all been similar to the design Dr. Loeffler developed. The steam is released into a drum of water through multiple nozzles and the resulting wet steam is allowed to

CIRCULATOR DRIVE TRAIN ARRANGEMENTS

FIGURE 5



dry through free surface separation or is mechanically dried with moisture separators and dryers. Generation of the saturated steam using conventional desuperheaters is also possible, providing a known and controllable discharge steam condition. The technology of the desuperheating and moisture removing functions of both methods is fairly well established. What is not presently well known in either method, is the ability of the contact boiler to remove particulate or soluble gas fission products that may be in the superheated steam coming from the reactor. It is expected that this ability will be somewhat sensitive to the quantity and drop size of water that remains after the steam is completely desuperheated. Experience with the boiling water reactors points toward a decontamination factor of approximately 1,000 if the steam water conditions in the contact boiler after desuperheating are similar to those in a BWR. The technology developed by the chemical and gas industry for scrubbers of various types indicates that substantial decontamination could be provided by a spray-type desuperheater with as little as a few percent of excess moisture spray.

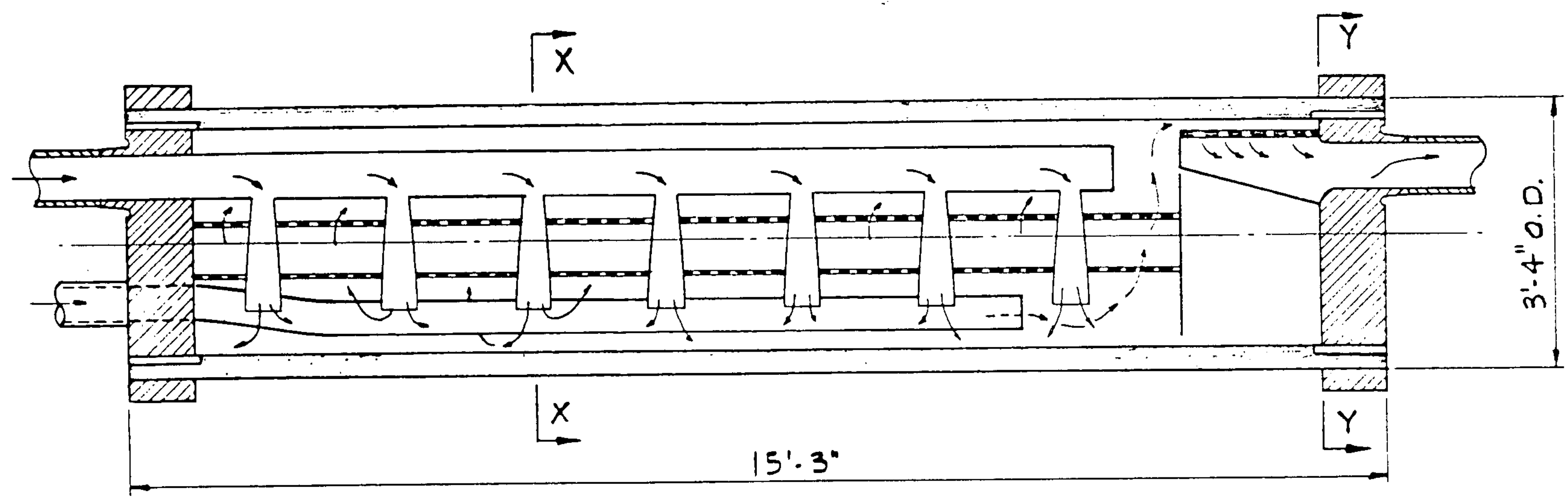
Designs currently being considered for the contact boilers of a demonstration plant utilize one or more of these technologies. For example, one design (Figure 6) involves simply a scale-up in pressure of an existing produce line drum-type desuperheater which is somewhat similar to the boilers utilized by Loeffler. As in the Loeffler design, operational control of the quality of steam-water mixture after completion of desuperheating is not practical. A secondary spray scrubber may, however, be utilized.

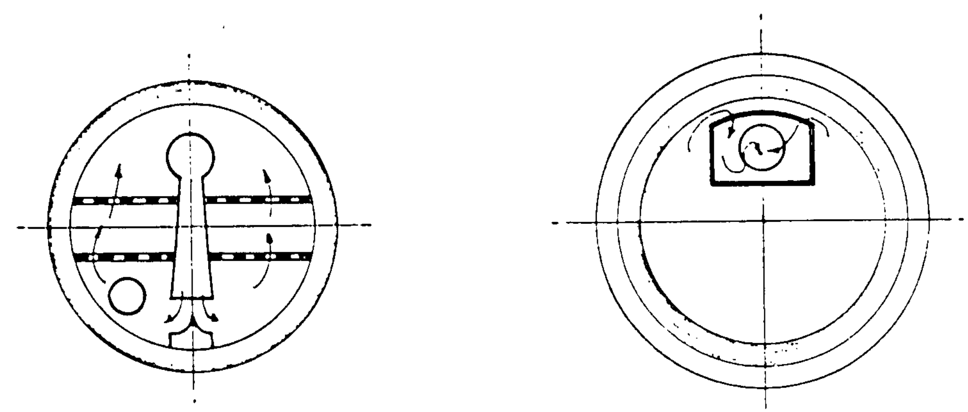
A second proposed design uses spray desuperheaters to provide both the water necessary to desuperheat the steam and an excess amount of spray water for scrubbing. The steam-water mixture is discharged into a separation and reserve water tank. The dryers utilized for separation in this tank are identical to those in a number of today's product line nuclear reactors while the steam conditions entering the dryers are also comparable to today's reactors. The disadvantage of the desuperheating spray system regarding feedwater failure is overcome in this proposed design by having a portion of the total water requirements recirculated by gravity feed and from the reserve water tank. Other designs include the use of bubble-cap trays, foaming devices, etc.

The requirements placed on the contact boiler during normal operation and during operating, or emergency transients, make it an extremely critical piece of equipment; it is the prime source for saturated steam, saturated water, or a steam-water mixture for cooling the core. During all possible conditions, it is relied upon to prevent recirculation of any steam in a still highly superheated condition. Specific transients which could cause difficulty in this regard and which the component design must accommodate, include: feedwater failure, either from valve malfunction or pump failure, rapid system pressure changes in either direction, rapid changes in steam flow in either direction, and rapid changes of inlet steam conditions. Except for some short-duration transients the contact boiler separation equipment must provide very dry steam so that carry-over of dissolved solids is kept to a minimum. Dryness of 99.9% steam is reasonably attainable in light of BWR experience, with possible improvement above that figure with slight oversizing of drying equipment.

Engineering Safeguards Systems for Continuity of Core Cooling

The continuity of core cooling requirements for the SCFR are similar to those for any of the water cooled reactors. The major difference is found in





SECTION "X-X"

SECTION "Y-Y"

SCHEMATIC OF A TYPICAL
LOEFFLER STEAM DRUM
150,000 #/hr.
(GRAND. COURONNE)

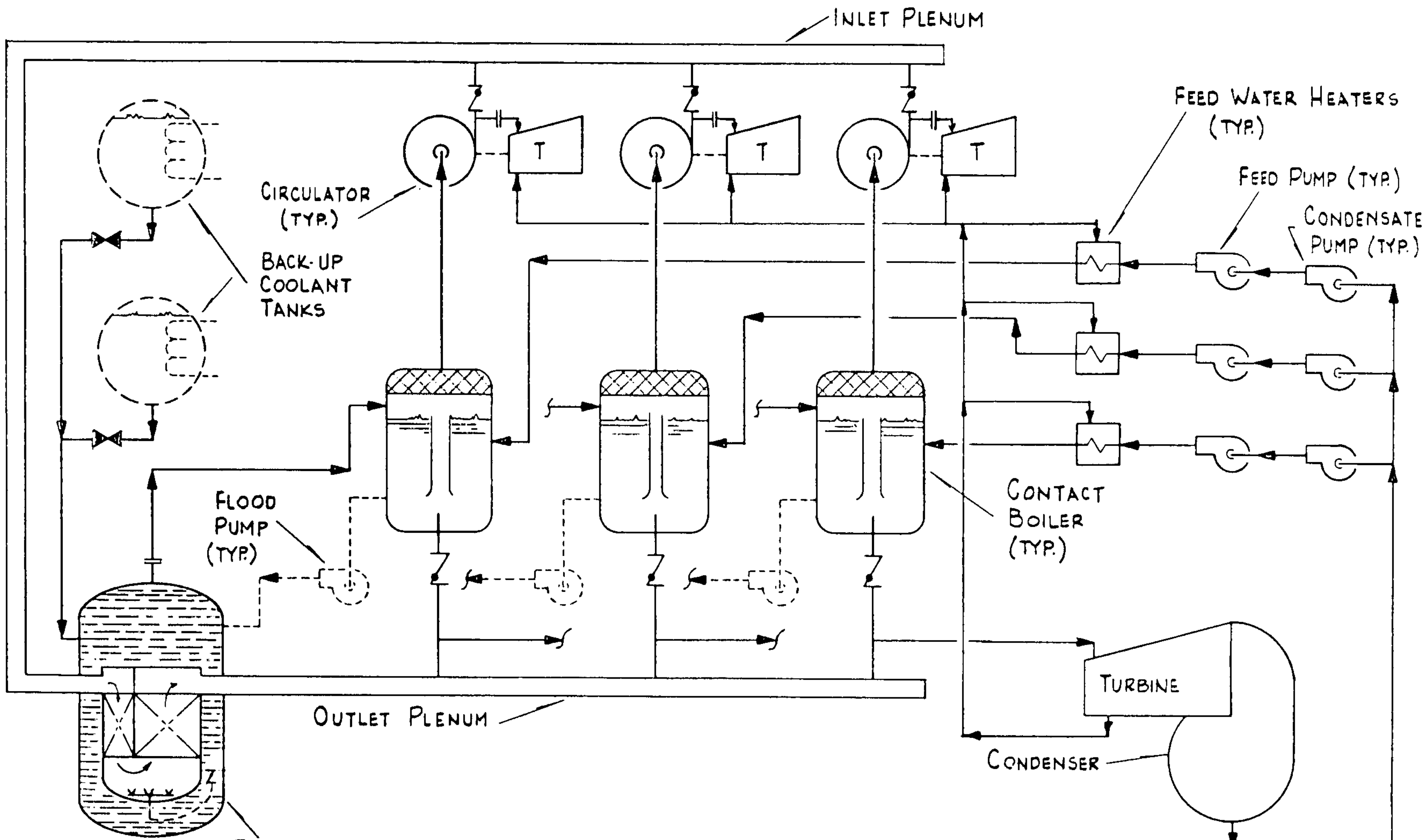
FIG. 6

the somewhat higher initial clad and fuel temperatures and higher power densities which demand somewhat quicker response times from the emergency systems. These faster response requirements necessitate refinement of the normal operating equipment to assure continuous cooling under all conditions. For example, the feedwater and condensate pumps can be upgraded in reliability and redundancy so that total loss of feedwater is eliminated as a possible accident.

A design criteria that is desirable for any reactor concept, and which has been applied to the engineered safeguards for continuous core cooling in the SCFR, is that a complete and orderly shutdown (ultimately to cold standby) should be the automatic and inherent result of any manual or safety system shutdown. Ideally, this ultimate safe condition should come about without any operator action and with minimum reliance on moving equipment. This is considered as a goal for a product line plant of well-understood characteristics, but more rigorously for a first-of-a-kind plant such as the 50 MWe demonstration design. This philosophical point and any discussion about it would perhaps better fit in a paper dealing with overall system safety rather than component requirements; understanding of the point, however, is important in establishing the components needed for continuity of core cooling. This concept is, of course, not new, having influenced the design of other reactors that were first-of-a-kind, notably the General Electric EVESR (ESADA Vallecitos Experimental Superheat Reactor).

The first logical step to maintaining continuous cooling of the core is to assure continuity of the primary steam coolant flow even during such adversities as complete loss of electrical power. The logical component to do this is the already operating turbine-driven-circulator, utilizing the stored energy in the high pressure saturated water of the contact boilers to drive the turbines. Controls for the turbine under this condition must, of course, be completely independent of any external power supplies. On the 50 MWe plant design (Figure 7) a feature has been provided whereby a small portion of the drive turbine steam does not pass through the normal admission valves which would close on certain failures, but rather passes through a pre-adjusted orifice which controls the steam recirculation flow rate and provides for a slow, non-damaging, depressurization rate of the primary system.

Although a very high degree of reliability can be obtained through redundancy and diversity of the steam circulator coolant loops, provision for a backup coolant seems prudent to provide core cooling upon loss of the primary steam coolant. The logical coolant for backup of the steam is water. The end result of injection of backup coolant is flooding of the core with saturated water followed by removal of the core decay heat in much the same manner as a boiling water reactor. The components used for this function are rather conventional tanks of high pressure saturated water with associated pumps and valves. For example, on the 50 MWe design the contact boilers have sufficient water reserve to perform the flooding function and are elevated above the reactor so that the water will flow into the core by gravity when circulator flow (and hence core pressure-drop) has dropped by coastdown to a medium-low value. Once flooded, a natural convection loop is formed between the reactor vessel and contact boilers, again utilizing BWR proven technology. Pumps can be provided in these loops to relegate the natural circulation capability to a backup position. These flooding pumps circulate water at very-close-to saturated conditions with very low net positive suction head, thus are basically the same type as presently used for BWR recirculation pumps.



MAIN COOLANT FUNCTIONAL DIAGRAM

50 MWe S.C.F.R.

FIG. 7

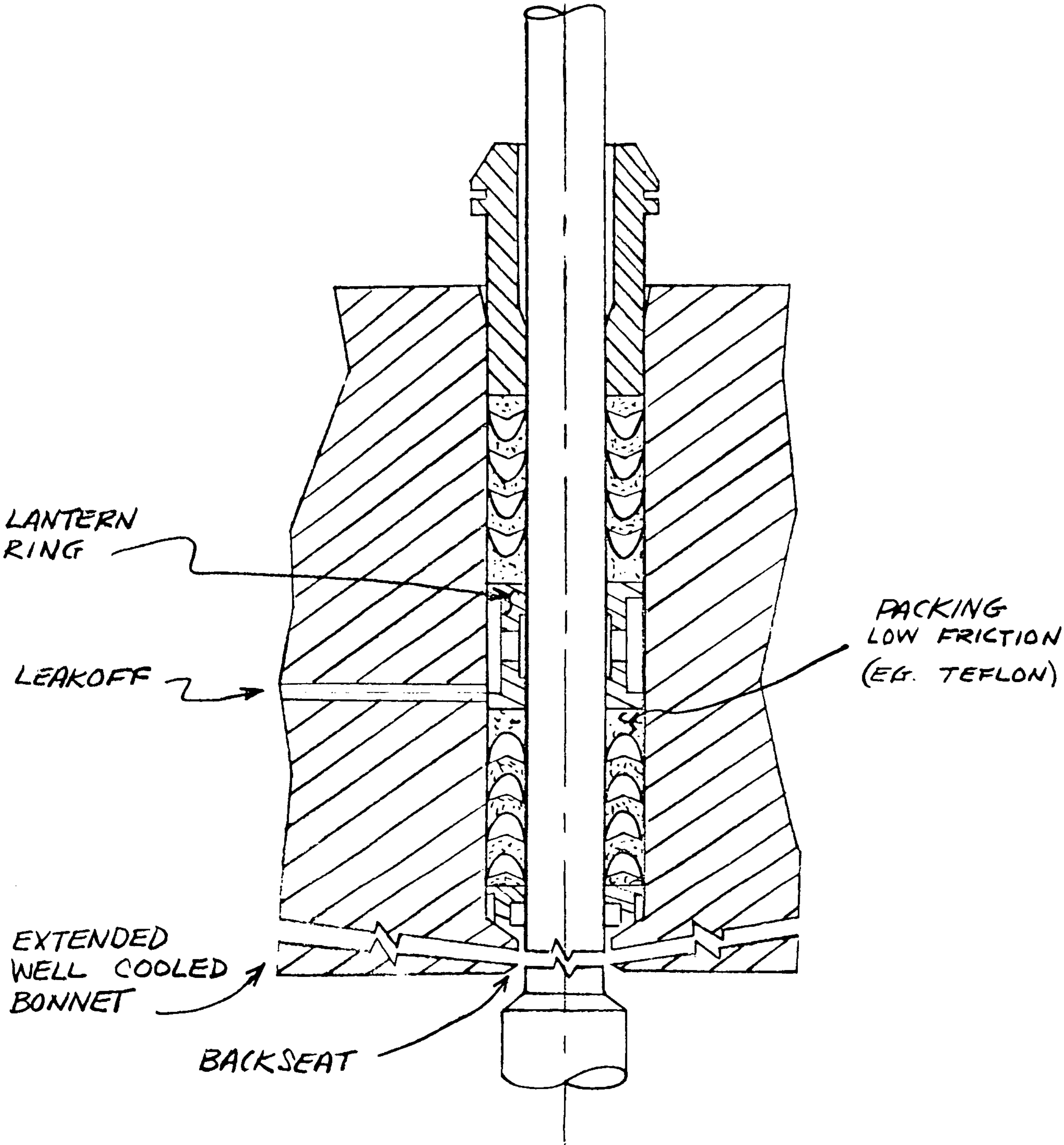
Another component which can provide this backup coolant feature is a tank of water maintained at a saturation pressure well above the core pressure. On demand, the water in this tank can be flashed through the core providing a coolant which is a mixture of steam and water, or fog, characterized by very good heat transfer and transport properties. Through proper selection of size and pressure, the end result of discharge of this tank is again a flooded core. This type of tank is planned as a backup method in the 50 MWe design and also incorporates the startup heat source of electrical heaters needed to pressurize the system from cold standby.

Valves and Seals

A somewhat higher potential for contamination than on conventional water cooled reactors places a more stringent requirement for integrity on all primary system valves and seals. Experience to-date indicates that maintaining leak tightness of system valves and atmospheric seals is critical to successful operation of a direct cycle steam plant. The most satisfactory performance in the high pressure, high temperature steam environment has been obtained (in our experience) with essentially conventional valves having well cooled, extended bonnets placed in well ventilated areas. The key to successful operation is the placement of each valve such that preventive maintenance can be performed promptly when incipient leakage occurs (Figure 8).

Development Required

For the first small experimental plant, the development needs of the circulators, drive turbines, contact boilers and valves are rather modest and present-day technology should largely suffice for these out-of-reactor components. The areas of development center mainly around the use of steam or water lubricated bearings, especially oriented toward the upgrading of reliability of these bearings under both normal and abnormal operating conditions. The operation of such an experimental plant would provide a major test facility for both identifying and investigating the possible development problems of the more advanced, central power station scale SCFR's of the future.



VALVE PACKING

FIGURE 8

DISCUSSION

J.C. Fox (Battelle-Northwest) - Because the steam-cooled fast reactor does not appear to have as great an ultimate potential in fuel utilization (and hence fueling cost) as the liquid metal-or-gas-cooled fast breeders, its prospects depend largely on whether or not it can be developed more rapidly and at lower cost than its competitors. Since the developing of reliable components for the LMFBR is an area of major concern and substantial expenditure, it is important to make a careful evaluation of the performance requirements, state of existing technology, and development effort needed for SCFBR components, in order to determine the incentive for developing this concept. The authors discuss many of the points pertinent to such an evaluation, with respect to components of the heat transport system.

The steam circulators are the most important components which would require a discrete development and testing program. Either the radial or axial flow compressors appear technically feasible, and designers are presently divided in choice. The basic technology of steam circulators and controlled leakage seals for steam service exists. Although some development would be required for the steam-or-water-lubricated bearings for this service, there appears to be no formidable technical barrier to success. It remains to put these items together, scale them up considerably in size, and demonstrate overall reliability. This probably should be done in a separate test program, somewhat analagous to the sodium pump test program for the LMFBR, although it has been estimated that the facility required and the overall development would be somewhat lower in cost than for the sodium pump program. Since the authors have been engaged in design studies of the Loeffler cycle components for some time, it would be helpful if at this point they could be more explicit about the development program visualized, as well as in identifying a preferred circulator concept and drive turbine location.

The contact boiler is a component for which considerable technology exists, and the problem here is primarily one of selecting the approach which will yield the best control over particulate or soluble contaminants and steam conditions under all operating circumstances in the system, and determining if its performance is adequate for the nuclear plant requirements. At present, most designers prefer spray desuperheaters, using a few percent excess water spray for contamination control. As the authors imply, it is probably necessary to incorporate more than one type of contact boiler into a small experimental nuclear power plant in order to make comparative performance evaluations, since this cannot be done conclusively except when the boiler is incorporated in a representative system.

In the discussion on engineered safeguards, the authors do not refer to reactivity coefficients of the SCFBR which can affect the requirements for response of emergency systems in certain abnormal situations, or could in some designs proscribe the use of core flooding. It is inferred that flooding the subject reactor has a negative effect on reactivity, which in turn implies that the procedure for startup from a flooded condition must be very carefully controlled. Being able to flood the core reliably would be a significant attribute to SCFBR safety. Not all other designers have taken this approach to backup cooling, apparently because in low pressure designs intended to achieve high breeding ratio, it can limit core design and penalize ultimate performances.

With regard to valves and seals, operating experience at EVESR has emphasized the importance of attention to detail in design and careful maintenance procedures, but these do not appear to be important development problems.

The overall impression that a reviewer of this paper receives is that the major design, development, and testing problems of the SCFBR plant (exclusive of the reactor core) are not component problems, but system problems. This is illustrated by the factors which influence location of the circulator drive turbine in the steam cycle; by the performance criteria which the contact boiler must meet; and, of course, by the engineered safeguards systems. In discussing the requirements placed on the contact boiler, the authors bring this out rather well. In planning a development program for the SCFBR, one might expect to place earlier emphasis on a reactor experiment than for the LMFBR or GCFBR, where the early thrust would be on component development and fuel testing.

F.J. Leitz, G.J. Rittenmyer, and H.J. Schneider - Your conclusion regarding the availability of steam cooled reactor components seems in agreement with our study. An integration of these components into a safe and workable system is the major task. Detailed discussion on our system was beyond the scope of this paper, but it is intended to treat that in detail in future topical meetings. To somewhat offset the optimistic picture regarding components please note our cautionary observations regarding system contamination and scale-up difficulties. A further cautionary note may be in order relative to capital costs; the amount of steam which must be handled per megawatt of reactor power is several times that of thermal reactors while the feedwater system is only fractionally smaller. Handling this steam in nuclear grade systems will cost money. Our cost estimating work for the 50 megawatt plant is going on right now.

Regarding the steam circulator type for the first generation of plants, we have placed much greater emphasis on heat characteristics than on efficiency or size. Specifically, we have cautioned vendors that the circulator should not have a narrow surge margin, displaying if possible a constantly rising head curve. To date, this criterion has been satisfied (at least until some proof testing is completed) by centrifugal designs with some hope held out for axial machines with very few stages, perhaps no more than three. We of course, like the higher efficiency claimed for the axial machine, but the results of our study indicate that the competition for the machines of at least the demonstration plant is being lead by the centrifugal concept.

As we indicated in the paper, the position of the drive turbine is affected by many system considerations and in turn affects system performance. Additionally, the preference in a small demonstration plant may be different than in a large plant. As we have already indicated, these as well as other comments you had regarding systems, will undoubtedly be treated in future papers resulting from our project.

We have approached the contact boiler requirement a little bit differently than you proposed in your comments regarding contamination control. We feel that it is most important that the thermal and safety performance of the contact boilers used in the first generation of plants be more than just adequate, but represent the best that industry can provide. We believe that if the boiler is designed to do the best possible job as a desuperheating steam generator under all steady-state and transient conditions, its performance as a scrubber will turn out to be adequate. If sufficient flexibility is incorporated in the design, then testing and in-service-modifications can be accomplished to improve the decontaminating capability. Your comment regarding the interrelationship between reactivity coefficients and action taken by emergency systems recognizes another area of conservatism which we reflected in the paper. We recognize that safety should not completely dictate core design, but feel that at least in the first generation of plants it must play a very dominant role, hence a core void coefficient and flooding relationship which enhances safety for which we have described the important components.

D.C. Schulderberg (Babcock & Wilcox) - This paper describes the requirements and development status of several major SCFR components to indicate that R&D requirements are largely confined to first-of-a-kind engineering and test associated with a particular size and configuration.

The remaining components in an SCFR plant would also appear to enjoy a similar development status. This includes the reactor vessel, core tank, containments, fuel handling equipment, startup boiler, etc. This state of the art in component development supports the point of view that steam-water technology is a logical path for translation of the electric power industry into breeder reactor technology.

It is believed that steam circulator engineering and test requirements for a power demonstration reactor (300-500 MWe) and later commercial units (1000 MWe or larger) may be reduced by using 4 circulators in the power demonstration reactor and 8 units of similar size and configuration in the 1000 MWe plant.

Perhaps, it is appropriate to add a few remarks to those given regarding the direct contact boiler. In addition to steam purification, the direct contact boiler can be made to play a very important role in the plant safety and kinetic behavior. This is referred to in a previous B&W paper[1] and has been treated in some detail in a recent German paper[2]. Excess spray in combination with a characteristic SCFR positive void coefficient can be utilized to provide a degree of system stability which enables the plant to follow load swings and minimize the consequences of system leakage. Thus, the direct contact boiler design should take into account this important function.

The discussion of core cooling continuity illustrates the variety of approaches to this problem characteristic of steam-water technology. It is felt that this discussion would be more complete if some reference were made to the role of other components (such as the pressure vessel) and the the plant arrangement (integral vs. spreadout).

F.J. Leitz, G.J. Rittenmyer, and H.J. Schneider - We are in basic agreement with your contention that pressure vessels and the like do not represent special problems as evidenced by our exclusion of them from the paper. In general, they merely require judicious application of existing BWR/PWR technology and intelligent adherence to codes. The components that we did select for study and discussion were those items that had functional requirements not usually found in either fossil or nuclear power plants. Prudent selection of a number of loops as plant size increases is certainly a good way to ameliorate scale-up problems for the circulators. Interestingly, the contact boilers in the loops also enjoy this advantage, allowing an increasing number of boilers to reduce scale-up difficulties.

The main thrust of our evaluation was to determine what today's technology could offer in way of building blocks for the SCFR. Perhaps we missed an opportunity, but we did not choose to discuss such system advantages as the affect on a positive void coefficient displayed by the direct contact boiler. As you indicate, however, this has already been treated in previous papers.

W.R. Gall (Oak Ridge National Laboratory) - I am inclined to agree with the general position of the authors that the extensive and well developed steam technology in fossil-fired and thermal nuclear plants can be applied to the development of components for steam-cooled reactors. I also agree that the development of circulators for a 50 MWe reactor experiment plant could be accomplished on the basis of the concepts described in the paper. The discussion of safeguards systems also seems adequate, especially if the goal of a complete and orderly shutdown can be met under the conditions stated. The two principal areas to which I would direct my comments are: (1) the sensitivity of the core to steam conditions as affected by performance of the components; and (2) the components of the system that have not been covered in the paper, but which may require some special attention.

Steam Conditions - The authors have stated a need for achieving a high quality (dry) saturated steam at the suction of the steam circulator. They have also pointed out in discussing the steam generator requirements, the not-so-obvious requirement for complete desuperheating of the superheated steam supplied to the Loeffler boiler. Since these requirements are somewhat convergent on the steam generator and also since passage of slugs of water or even droplets into the reactor core would have some possible reactivity effects, why is it important that steam be at saturation conditions at the reactor inlet? As an example, assuming the reactor inlet pressure of 2000 psia and Loeffler boiler pressure of 1800 psia, the pumping work adds approximately 10 BTU's per pound of steam circulated. If the steam leaving the boiler is dry and saturated at 1800 psia, it will reach the reactor inlet with approximately 10°F superheat, due to the combination of pump work and the shift in saturation enthalpy, or conversely, if exactly saturated steam at 2000 psia is required at reactor inlet, the quality leaving the 1800 psia boiler should be 96%. Thus, the provision of dry, saturated steam to the reactor alone does not impose the requirement of 99.9% quality at the boiler exit. On the other hand, it may be desirable, if not necessary to maintain this high level of quality for the protection of the circulator itself.

From a nuclear standpoint, it is less important to achieve complete desuperheating of the steam than it is to achieve complete dryness. In fact, if the steam is superheated at the core inlet, the reactor will be subjected to less danger of receiving slugs or droplets of water and therefore, would be safer from a nuclear standpoint. Therefore, I am led to wonder if the designers have considered a cycle in which the steam enters the reactor in the superheated condition by design, recognizing that this of course, imposes a heat transfer penalty in the core itself but one which is basically a design problem. With the type of Loeffler boiler that is proposed where superheated steam is injected into the water, it is obviously not likely that the boiler would produce superheated steam, but steam jets could be added in the steam phase of the boiler drum or in the exit piping leading to the circulator.

Another nuclear effect to be considered is the effect of steam density on reactivity. Since the steam density is a function of both temperature and pressure, fluctuations of either of these due to operational variations in the cycle will feed back to the core and result in reactivity changes. This imposes a need for precise and reliable controls for both pressure and temperature and perhaps flow in the external system that would not be equally necessary in a fossil-fired system. This is a development problem not discussed in the paper.

Circulators - Undoubtedly much of the experience in designing pumps for BWR and PWR systems will be of benefit in designing and building the circulators for steam-cooled reactors when combined with the experiences quoted from European practices. I question two points in the discussion of the circulator on a very minor level; one being the need for the steam lubricated bearings, the other being the similarity of the impeller in this case to that in the water circulation pump. These are minor questions, but I wonder if water lubricated bearings could not be used instead of steam lubricated bearings, and I also wonder if the impeller design for steam would not be quite different from that for water. I am led by the only cursory study of various types of circulators to the impression that the pumping characteristics of an axial flow circulator directly coupled to a steam turbine might be preferable to those of a centrifugal circulator also driven by steam turbine. So, I merely question whether the centrifugal type circulator is preferable where the drive unit is a steam turbine and the speed will be varied as the load varies.

Other Components Not Covered - Some other components external to the reactor that may have special problems in the steam cooled system and are not discussed in the paper include the control rod drives, the vessels (including the reactor vessel), the piping, and the devices for controlling pressures, temperatures and flows in the steam and feedwater systems. Although fossil fueled steam power plants use superheated steam, the design, and construction techniques for these nuclear components for temperature above 800°F are not established. (It is assumed that this temperature would be exceeded.)

Conclusions - In summary, the importance of desuperheating the reactor outlet steam to saturated conditions as contrasted to designing the system to operate entirely within the superheated region is questioned. It is also mentioned that steam cooling pushes temperature levels for nuclear components to levels above those now in use.

I believe most of the major problems external to the reactor have been recognized in the paper and some discussion of them given, but specific solutions are not entirely defined. For example, importance of reliability of the circulator is recognized, but information on reliability of proposed designs is not given.

The construction and operation of a 50 MWe plant of the type proposed would contribute much toward answering questions about the safety and kinetics of these systems, and would probably disclose problems not foreseen at this time.

F.J. Leitz, G.J. Rittenmyer, and J.H. Schneider - One of the major advantages enjoyed by the steam cooled reactor concept when utilizing the Loeffler cycle (or for that matter some other saturated steam supply system) is that the core inlet temperature can be made relatively insensitive to the system malfunctions. This advantage can of course, be strengthened or weakened by variations in the contact boiler design. For example, this advantage is nearly eliminated in a spray desuperheated system if that system relies entirely on valves and/or pumps for continuous flow of the spray water, since malfunction of a component can result in system failure. This is not to say a reactor utilizing a completely superheated cycle could not be built and operated; in fact, the gas cooled thermal reactors now in existence constitute such a system. We feel that the thermal flywheel or thermal anchor effect afforded by the contact boiler is a most important safety attribute to the SCFR. Reheating the boiler outlet steam by bypassing some superheated steam to assure dryness at the core inlet is a considered alternate; we have concluded that the combination of dryer systems such as those presently in BWR's and the slight reheating provided by the circulators will provide a reliable system. In regard to your point for the need for precise and reliable control of pressure temperature and flow, we feel that this is more of a detail design task rather than a development problem, by prudent application of development information gathered from such similar systems as the flux flow temperature computer system utilized on the EVESR.

We certainly agree with your question on the need for steam lubricated bearings. As we pointed out, early plants may in fact use properly isolated oil lubricated bearings. As we indicated in the answers to Mr. Fox, the centrifugal type impeller (in fact very similar to the ones used in water circulation pumps) seems to best meet our requirements based on the stable head flow characteristic criteria. Our transient studies of this type of circulator direct-driven by a steam turbine have not been completed and we can only preliminarily indicate that this combination looks like a good one. We have incidentally, done a reliability analysis of a multiple-loop turbine-driven-circulator system for the 50 MWe plant utilizing a code developed by the General Electric Aricraft Gas Turbine people and the answers obtained were very encouraging. We recognize, of course, that this analysis is quite preliminary utilizing only related rather than direct reliability information applied to the best of our present ability.

We feel that the design and construction techniques for high temperature nuclear systems has been sufficiently established by the construction and operation of such nuclear superheat plants as the BONUS and EVESR to allow a direct, but probably conservative route to the first generation of steam cooled fast reactors. It will remain for later revisions of applicable codes to pare out some of the conservatism to the benefit of later plants.

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COMPONENTS FOR MAIN HEAT-TRANSPORT SYSTEMS FOR SODIUM-COOLED FAST BREEDER REACTORS

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ABSTRACT

The operations and development programs of EBR-II, Enrico Fermi, SRE, and Hallam provide the bulk of our U.S. experience with components for main heat-transport systems--specifically, pumps, intermediate heat exchangers, and some elements of piping systems; the subjects of this paper. Some 50,000 hours of reactor operation and a few hundred thousand hours of experience with main heat-transport system components have been accumulated by these four plants. Components for SRE alone have logged 44,000 hours of sodium service with over half above 500°F and some 1000 hours above 900°F. EBR-II secondary-system components and Enrico Fermi primary-system components have logged separately more than 23,000 hours. While problems have been encountered with pumps, intermediate heat exchangers and piping, they have been remedied.

The thrust of component development is now being directed to meet the needs of a rapidly developing LMFBR industry. This requires the development of a firm component technology that will support commercial LMFBR central-station power plants. Recently, the AEC has prepared a comprehensive Component Development Plan. The plan is geared to the needs of large LMFBRs that may require pumps of 40,000 to 120,000 gpm, heat exchangers of 400 to 1300 MW and piping systems to handle 60,000 to 120,000 gpm of sodium at up to 1200°F. These requirements are typical of the greatly increased demands that will be made upon components for LMFBR central-station service.

INTRODUCTION

The development of components for the Liquid Metal Fast Breeder Reactor (LMFBR) dates from the early days of reactor development in this country. Starting with Clementine in 1948 at 25 kWt, we have progressed to the current EBR-II and Enrico Fermi plants with design capacities of 62.5 MWt and 430 MWt, respectively. U.S. liquid-metal-cooled reactor facilities and some of their pertinent parameters are listed in Table 1. These first-of-a-kind facilities have provided the bulk of our operating experience with main components for liquid-metal heat-transport systems.

TABLE 1. U. S. Liquid Metal Cooled Reactor Facilities and Related Parameters

Facilities	Status[a]	Power (kWt)	Coolant	First Fuel Loading	Coolant Temp In/Out (°F)
Fast Reactors:					
Clementine	O[b]	25	Hg	Pu (metal)	-
Lampre I	O[b]	1,000	Na	Pu (molten)	-
EBR-I	O[b]	1,400	NaK	U^{235} (metal)[d]	-
SEFOR	C	20,000	Na	Pu-U (oxide)	700/820
EBR-II	O	62,500	Na	U^{235} (metal)	700/883
Fermi (EFAPP)	O	430,000[c]	Na	U^{235} (metal)	550/800
FFTF	D	400,000	Na	Pu-U (oxide)	800/1100
Thermal Reactors:					
SRE	O[b]	20,000	Na	U^{235} (metal)	(e)/to 1030
Hallam (HNPF)	O[b]	256,000	Na	U^{235} (metal)	610/945

(a)O--Completed reactors that have Operated. C--Reactors in Construction. D--Reactors in Design.
(b)Subsequently decommissioned.
(c)Design Value--Ultimate power capability.
(d)Final fuel loading was Pu (metal).
(e)Inlet temperature variable.

TABLE 2. Reactor Plant Operating Data

Parameter	EBR-II (9-1967)	Fermi[4] (11-1966)	SRE[5] (1965)	Hallam[6] (9-1964)	Rapsodie[7] (2-1968)
Integrated reactor power (MWt-day)	13,895	855	6,700	26,615	~3,000
Hours of reactor operation (hr)	10,260	2,089	27,300	~10,000	-
Integrated electrical output (MWe-day)	3,547	48	1,550	8,019	-
Hours of pump operation					
Primary pump No. 1 (hr)	~14,000	26,427	44,000*	21,198	7,280
Primary pump No. 2 (hr)	~14,000	24,529	-	21,677	8,600
Primary pump No. 3 (hr)	-	23,934	-	21,897	-
Secondary pump No. 1 (hr)	23,000	14,976	44,000*	17,375	11,000
Secondary pump No. 2 (hr)	-	13,162	-	18,470	9,880
Secondary pump No. 3 (hr)	-	18,172	-	16,597	-

*The main system pumps for SRE-PEP accumulated about 17,000 hours in addition to those indicated.

A good appreciation of current component experience and technology is essential to a full understanding of component needs. The significant advances made with pumps, intermediate heat exchangers, and steam generators during the 1950s and early 1960s were discussed by Morabito and Savage[1] at the 1965 ANS Fast Reactor Technology National Topical Meeting. The operation of many of these components or related development programs has continued. In addition, some new programs have begun. The development of steam generators, in particular, has received considerable attention since 1965. The problems of fuel handling are also receiving increasing attention. The status of these components--i.e., steam generators and fuel-handling equipment--are the subjects of other papers at this meeting.[2,3] This paper describes recent experiences and development activities with pumps, intermediate heat exchangers, and components for piping systems.

Prime sources of component experience are reactor plants that have operated. Those of principal interest for components in sodium service in the U.S. are EBR-II, Fermi, SRE, and Hallam; the foreign plants of principal interest are Dounreay, Rapsodie, and BR-5. These plants constitute the bulk of our operating experience with components of practical size. A compilation of reactor and pump operating times for the U.S. plants is given in Table 2. Combined totals of about 50,000 hours of reactor operation and a few hundred thousand hours of experience with components of main heat-transport systems have been accumulated. SRE components, for example, have operated as much as 44,000 hours with about half of these hours at sodium temperatures of 300 to 500°F, half at 500 to 1030°F and about 1000 hours above 900°F.[8] Much of the operating time for the sodium systems of EBR-II, Fermi, and Hallam is for service conditions between 500 and 900°F.

In the foreign programs, Rapsodie is of particular current interest. It was filled with sodium in mid-1966 and went critical in January 1967. To date the sodium loops have been drained and filled seven times and exposed to 15 operating cycles from 300°F to ~930°F.[7] An equivalent 150 full-power (20 MWt) days have been accumulated. Since the first of this year, the normal operating power level has been raised to 24 MWt at an inlet temperature of 760°F and an outlet temperature of 925°F.

The thrust of component development in this country is changing. In the past, emphasis was focused mainly upon the specific needs of the reactor facilities listed in Table 1. For the future, the broader, more exacting needs of a competitive, multi-billion-dollar LMFBR industry are being anticipated. This is evidenced by the recent completion of a first draft of a comprehensive Component Development Plan. The plan, prepared by the AEC's LMFBR Program Office at Argonne National Laboratory, is directed to the development of a well-established, broad-based technology. It encompasses all major LMFBR components for sodium service. These include; steam generators, intermediate heat exchangers, pumps, piping system components, reactor vessel, control drive mechanisms, and fuel handling equipment. The plan assumes a 1973 completion of the Fast Flux Test Facility (FFTF), currently a major AEC project, and the commitment of 300 to 500-MWe demonstration plants no earlier than 1970. Implementation of the plan has been initiated by the AEC.

Component development needs are subordinate to, and stem from, the functional and design requirements of plants. Initially, the emphasis of LMFBR plant design will be upon safety and reliability. The selection of suitable plant concepts and design parameters, however, will do much to avoid unduly restrictive component requirements. Table 3 lists some pertinent parameters for main heat-transport components for (a) current plants, (b) plants being considered for the 1970s, and (c) more nearly commercial LMFBRs expected in the 1980s. Current technology is primarily representative of that embodied in the

TABLE 3. Component Design Parameters

	Current (To Date)	Near-Term* (1970s)	Longer-Range (1980s and beyond)
		Plant Design	
Net elec. (MWe)	150	300-500	800 and greater
Total thermal (MWt)	430	750-1250	2400 and greater
		Pumps	
Capacity (gpm)	13,000	20,000-60,000	60,000-120,000
Head (ft.)	300	-	300-450
Design temp. (°F)	1000	1000-1100	1000-1200
Type	Mechanical Free Surface	Mechanical Free Surface	Mechanical Free Surface
		Heat Exchangers	
Capacity (MWt)	143	100-500	400-1300
Sodium temp. (°F)	1000	1000-1100	1000-1200
Type	Vertical Shell & Tube	Vertical Shell & Tube	Vertical Shell & Tube
Primary side ΔP (psi)	~ 5	-	2-10
		Piping, Valves, Etc.	
Flow rate/loop (gpm)	12,000	20,000-60,000	60,000-120,000
Pipe size, OD (in.)	30	-	30-50
Large valves	Few	Yes	Yes
Fluid velocity (fps)	5-15	-	10-30

*Data from papers presented at ANS National Topical Meeting on Fast Reactors in San Francisco, April 10-12, 1967.

Enrico Fermi plant. The near-term (1970s) plant information was compiled from papers presented at the ANS National Topical Meeting on Fast Reactors in San Francisco, April 10-12, 1967. The data for longer-term plants reflect the author's views. A review of these parameters provides some insight into the future needs of components. For example, typical commercial plants in the 1980s are likely to need heat exchangers of 400 to 1300 MWt, pumps of 40,000 to 120,000 gpm, and piping components to handle sodium flow rates of 60,000 to 120,000 gpm--all operating at temperatures to 1200°F.

PUMPS

Operating Experience

Since Reference 1 was presented in April 1965, many thousands of hours of relatively trouble-free operation have been accumulated with the two 5500-gpm EBR-II primary pumps and the three 11,800-gpm primary and the three 13,000-gpm secondary pumps of Fermi. Two pump problems have been reported for EBR-II. Sticking of the shaft of primary pump M-1 occurred at a total operating time of 4442 hours. Twice within a few days thereafter the pump could not be started. Each time, the shaft-impeller assembly was freed by manual lowering and raising. The flow rate of fresh argon bleed-gas past shaft seals was increased from about 1 to 2 cu ft/hr to deter the possible buildup of sodium deposits in shaft-clearance regions. No further trouble occurred and subsequent pump operation was satisfactory until quite recently. Early this year malfunctions of pump controls and motor-generator brushes have caused some unscheduled downtime of the reactor.

The Fermi pumps have required frequent replacement of shaft seals--at intervals of about six months. Seal replacement requires removal of the pump from service. Improved seals are being designed to correct the situation.

In January 1967, Rapsodie went critical. Prior to this date, pre-operational checking of plant systems included final in-place testing of sodium-system components. During this phase, two pump seizures were encountered; first, a secondary pump and then, about six weeks later, a primary pump. In each instance system temperatures were above 500°C and the seizure was gradual--i.e., a slow loading of the drive motors was observed. As reported in Reference 9,

> ". . . a secondary pump seized up and the reactor had to be emptied.
>
> "The cause of the seizing up was not determined. However, suspecting abnormal deformations of the pump shell under solicitations coming from the piping, or displacements of the framework, systems were installed to keep these displacements under permanent inspection. In addition, the pumps were reinforced at the connection between their shell and inlet piping.
>
> "During the last temperature rise, before the sub-critical approach, a primary pump seized up under conditions similar to those of the secondary pump incident (temperatures above 500°C). As with the first seizing up this second one could not be fully explained. Analysis of the phenomena, however, suggests that it may be due to lack of resistance in the bearing materials."

The performance of materials for hydrostatic bearings was investigated. The French report [7] that satisfactory service has been attained with the following:

(1) Stellite Grade 6 on Stellite Grade 12

(2) Colomonoy Grade 5 on Colomonoy Grade 6

(3) Hard chrome on Tungsten Carbide

(4) Alacrite 554 on Alacrite 602

The Stellites and Colomonoys are currently in service in the Rapsodie pumps. Originally, the bearings for both the primary and secondary pumps were Colomonoy. After the seizing difficulties the bearing material in the secondary pumps was changed to Stellite. In addition, running clearances were increased from 280 micron (0.011 in.) to 400 micron (0.016). The Alacrite materials have accumulated 9000 hours of satisfactory service in a 440 gpm pump.

The design of the PFR pumps is based upon recent experience with a 7100 gpm prototype pump (Fig. 1). The pump design characteristics are listed in Table 4. As reported in Reference 10:

> "The pump was started for the first time in November 1964, and removed for examination in September 1965. During this period it ran faultlessly for over 3000 hours at various speeds between half and full speed, and was stopped and started over 250 times. The oil leakage past the shaft seals decreased from 1 cm^3/hr at the beginning of the tests to 0.2 cm^3/hr during the final run.
>
> "During the tests a significant amount of sodium mist was seen above the sodium pool, and some solid sodium was deposited in the sight tubes which penetrate into the main pump vessel, but when the pump was removed only minimal quantities of sodium were found on the pump components. No significant deposition had occurred on those parts which were above the sodium level, and those below the sodium had drained almost completely.
>
> "While the pump was being partially dismantled before steam cleaning, some difficulty was experienced in removing the bolts and separating those flanges which had been immersed in the sodium. Apparently the liquid metal had penetrated into the fine clearances between the mating parts and was causing some form of adhesion even when heated above its melting point, although when separated the mating surfaces showed no sign of pickup or diffusion welding. This information will influence the detailed design of the PFR pump: in particular, jacking bolts will be fitted to facilitate the separation of flanges.
>
> "After steam cleaning the pump was thoroughly examined. The sodium-lubricated bottom bearing was found to be in perfect condition, with no sign of wear on either the shaft or the sleeve. In the oil-lubricated top bearing the oil seals were in excellent condition, but the unloaded taper roller bearing showed a small amount of cage wear, possibly caused by incorrect clearance in the assembly. There was minor cavitation damage to the pump impeller and diffuser, possibly caused by running the pump away from the design point during measurements of the pump hydraulic performance."

Extensive testing is planned for the first primary and secondary pumps made for PFR.

TABLE 4. Sodium Pump Design Characteristics[10-14]

Plant	PFR	PFR	PFR	Phenix	BN-350	BN-350
Country	UK	UK	UK	France	USSR	USSR
Location	Risley	Dounreay	Dounreay	-	Mangyshlak Peninsula	Mangyshlak Peninsula
System	Prototype	Primary	Secondary	Primary	Primary	Secondary
Type	Mechanical Free-surface	Mechanical Free-surface	Mechanical Free-surface	Mechanical Free-surface	Mechanical Free-surface	Mechanical Free-surface
No. of units	1	3	3	3	6	6
Capacity (gpm)	7,100	18,000	15,000	17,600	14,200	17,000
Dynamic head (ft)	160	320	120	290	360	230
Design temperature (°F)	750	750	700	-	-	-
Motor speed (rpm)	980/100	-	925	1,000	1,000/250	1,000/250
Motor horsepower (hp)	475	1,900	700	-	2,280	1,470
Sealing arrangement	Mechanical Shaft Seal	Mechanical Shaft Seal	Mechanical Shaft Seal	Mechanical Shaft Seal	Mechanical Shaft Seal	Mechanical Shaft Seal
Material	SS	SS	321 SS	-	-	-
Type of speed control	Induction Regulator	Variable-speed Hydraulic Coupling	Variable-speed Hydraulic Coupling	Variable-speed Motor	2-speed Motor	2-speed Motor
Sodium bearing	Hydrostatic	Hydrostatic	Hydrostatic	Hydrostatic	-	-

New Plant Components

The United Kingdom is constructing a 250-MWe PFR, with operation scheduled for the early 1970s. In France, Phenix, a 250-MWe facility, is being designed with the start of construction scheduled for 1969. Russia is constructing both BOR-60 (60 MWt) and BN-350 (350 MWe), with operation expected in 1970, and is planning a new plant BN-600 (600 MWe). Germany is currently developing conceptual designs of LMFBR demonstration plants of 200 to 300 MWe. Each of these facilities will require large sodium pumps and other related components. A universal preference for the mechanical, centrifugal pump has been expressed. Table 4 lists design characteristics for some of these pumps.

PFR Primary Pump (18,000 gpm) (Fig. 2).[11,12] The design emphasizes reliability and long life. The pump is a mechanical, centrifugal, sump-type (free sodium level) unit for use with a pool-type primary system. The drive motor, fluid coupling, and pump impeller are vertically aligned on a single shaft. The drive motor, fluid coupling, and shaft seals are accessible and are located above the biological shield. A pony-motor drive is used for emergency cooling conditions. The impeller is of the mixed-flow type. Speed is controlled by a fixed-speed, squirrel-cage motor and a variable-speed, fluid coupling. Normal coolant operating temperatures are about 750°F.

Phenix Primary Pump (17,600 gpm) (Fig. 3).[13] The primary pumps are similar to those installed on Rapsodie: centrifugal pumps, with a vertical shaft and a free sodium level. The motor drive is accessible and is located above the upper shielding, as are the upper bearing and mechanical shaft seal. The shaft is approximately 16.4 ft (5 m) long and is supported near the impeller by a hydrostatic bearing supplied with sodium from the output of the pump. A variable-speed drive motor probably will be used.

BN-350 Primary Pump (14,200 gpm) (Fig. 4).[14] The primary and secondary pumps are similar in design. Each is of the free-surface type with no bearings operating in sodium. The vertical shaft is mounted on one radial-thrust sliding bearing and guided by a radial bearing about 6.5 ft (2 m) below the thrust bearing. The pump housing contains a biological shield which is partly submerged in sodium. In the region between the cover of the pump housing and the lower bearing is a NaK cooling system, which reduces the axial flow of heat towards the upper bearings. A mechanical shaft seal is used. The upper bearings and seal can be maintained without removal of the pump. The pump is driven by a two-speed motor. No other speed control is utilized.

Current Work

The AEC is currently sponsoring with Byron-Jackson and Westinghouse Electric contracts for pump development. Preliminary design and parametric analysis studies are being performed for pumps to satisfy design conditions of 60,000 gpm flow rate, 350-ft head, and 40-ft NPSH at sodium temperatures to 1200°F. Bearing and seal development programs are included, with testing to be performed at the Liquid Metal Engineering Center (LMEC) operated by Atomics International. The successful completion of this phase of the work should lead to the design and ultimately fabrication of pumps for testing in the Sodium Pump Test Facility (SPTF) presently being designed. This facility will be able to test pumps with capacities as large as 120,000 gpm at temperatures to 1200°F. It will be located at the LMEC.

Phase I of the Westinghouse study is nearing completion.[15] Two reference pump concepts have been tentatively recommended to the AEC--one for a loop (piped) system and one for a pool (sea-of-sodium) system.

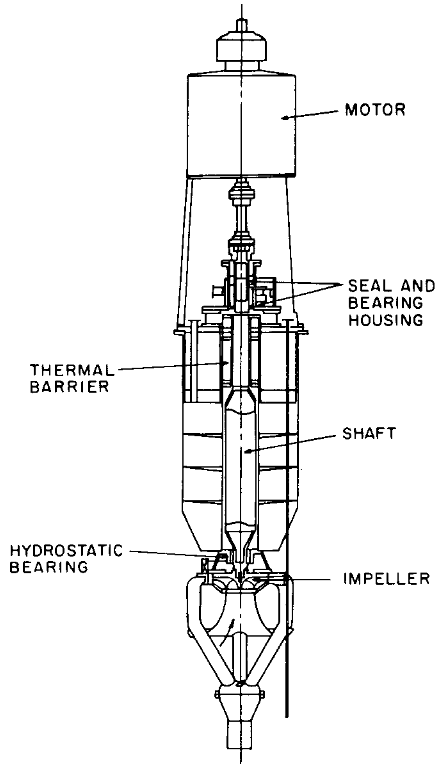

Fig. 1. PFR Prototype Pump[10]
(7,100 gpm)

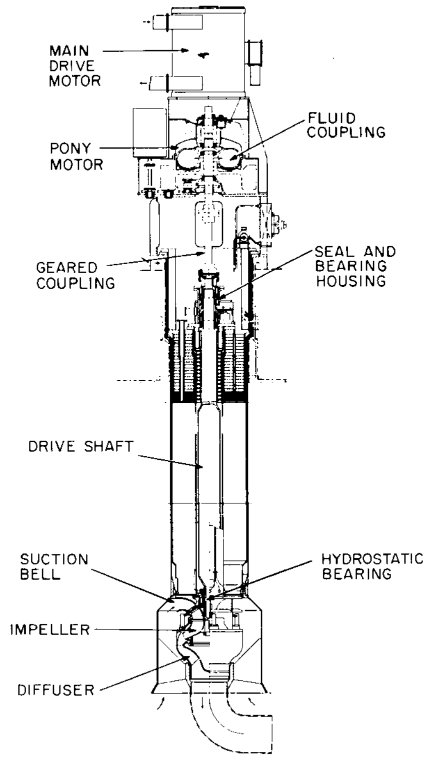

Fig. 2. PFR Primary Pump[11]
(18,000 gpm)

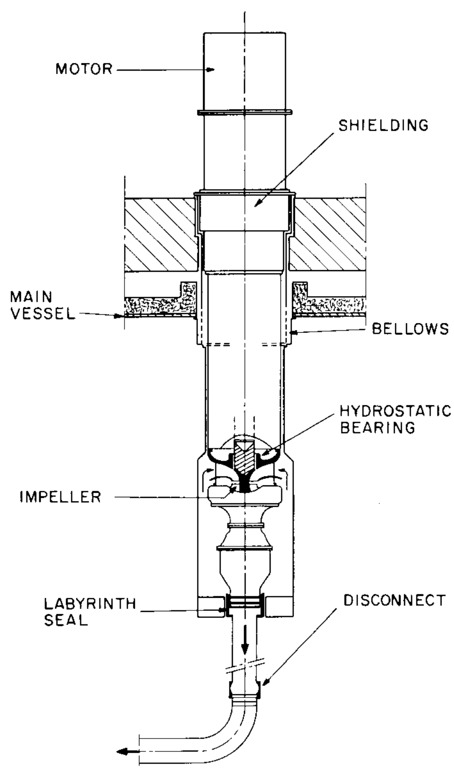

Fig. 3. Phenix Primary Pump[13]
(17,600 gpm)

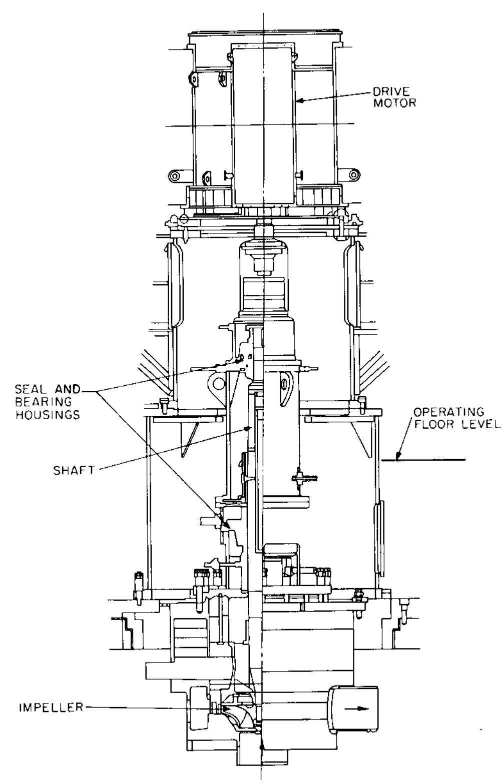

Fig. 4. BN-350 Primary Pump[14]
(14,200 gpm)

A similar study at Byron-Jackson is in progress, but final information on Phase I work has not been reported.

Development Needs

Operating problems with large, mechanical, centrifugal pumps in sodium service have resulted principally from insufficient running clearances, structural distortions, combinations thereof, and unreliable seals. Running clearance and structural-distortion problems are difficult to separate. These have been correctable, to date, by increasing clearances and, in some cases--e.g., Hallam--by improving temperature distributions to reduce distortions of the pump casing. Thermal distortion considerations and long-term effects on material stability will continue to be problems. Closer attention to design details should help to alleviate undesirable temperature gradients. Problems of material stability are not so easily resolved. These will require an improved knowledge of casing design and fabrication, and a better understanding of the stability of materials when exposed to 100,000 to 200,000 hours of LMFBR operating conditions.

Seals have been particularly troublesome. The use of hydrocarbon lubricants aggrevate the problem, since hydrocarbons cannot be tolerated in primary sodium systems. A more suitable lubricant is desirable. Above all, seals must function reliably. Unscheduled downtime of reactor-coolant pumps imposes too great an economic penalty for such conditions to be tolerated frequently. Pump-shaft seals require thorough development and prooftesting by seal specialists.

Future pump requirements for LMFBRs will create new difficulties. Pump capacities will greatly increase, resulting in larger impellers and casings; larger and possibly longer shafts; larger, higher-voltage motor drives; larger seals; larger, more heavily loaded bearings; and greater difficulties in meeting NPSH requirements. Higher sodium temperatures will increase sodium-deposition potential. Resolution is needed of conflicting needs of plant and component design--such as, shorter pump shafts vs less difficult plant configurations, low vs high cover-gas pressure, and hot-leg vs cold-leg location. The need for high plant availability will require improved reliability of component operation, reduced maintenance, and improved maintenance practices.

INTERMEDIATE HEAT EXCHANGERS

Operating Experience

Operating experience at EBR-II with the intermediate heat exchanger and other portions of the secondary system has been very good. It has been reported that:[16]

> "No significant troubles have developed. Intermediate heat exchanger performance is as predicted, both thermally and hydraulically. Sodium activity induced by passage through the heat exchanger (located in the primary tank) is negligible--only a few mr/hr can be observed at system pipe surfaces."

The EBR-II intermediate heat exchanger is comprised of a vertically oriented tube bundle attached to the lower end of a shield plug (Fig. 5). The unit is located in, and supported by, a nozzle in the primary tank. This nozzle or casing extends upward to the top of the primary-tank-support structure and downward into the tank about 10-ft to form a casing or open-ended shell for the tube bundle. An annular clearance of about 1/2 inch is provided between the shield plug and the nozzle (Detail A, Fig. 5). This totally enclosed clearance volume sees the pressure head of sodium at the inlet to the shell (primary coolant) side. This pressure head, about 3-5 psi at full flow, causes the sodium to rise into the annulus. This, in turn, influences the temperature and temperature distribution at the weld joint between the lower cover plate of the primary tank and the casing of the heat exchanger. The effects of possible thermal cycles on the weld joint, especially as caused by reactor scrams, is of some concern. An analysis has indicated that the situation should be satisfactory for projected operation at full power (62.5 MWt). However, if the heat exchanger should ever be removed for other reasons, modifications would be made to improve the situation.

Intermediate heat exchangers frequently are low-pressure-drop units--particularly on the primary-coolant side. The attainment of uniform flow distribution under these conditions is important. The EBR-II unit operates with a shell side ΔP of about 3-5 psi at full flow. Flow distribution is provided by a low-pressure-drop orifice plate at each end of the tube bundle (Detail B, Fig. 5). The orificing is comprised of an annular opening around each 5/8-in. tube. These openings range from a nominal diameter of 0.714 ± 0.002-in. for the outer row of tubes to 0.748 ± 0.002-in. for the inner row. Thermocouples are located on the upper and lower orifice plates to provide some insight into the resulting flow distribution. These data have not been fully analyzed.

The Fermi intermediate heat exchangers have a combined rating of 430 MW. These units have operated at part load--three loops operating at a total power of 100 MWt, approximately 25 percent of design rating. The reported experience(17) is:

> "The heat balance tests (at 100 MWt) confirmed that the performance of the intermediate heat exchangers between the primary and secondary sodium systems was approximately 50 per cent of that predicted by the manufacturer, and approximately 35 per cent of what had been predicted using the manufacturer's data for the total heat transfer area, combined with more recent liquid-metal heat transfer correlations. Several possible causes for the poor performance have been proposed and are listed below:
>
> (1) Sodium maldistribution on the shell side (primary).
>
> (2) Stagnation areas on the shell side at the free sodium surface and adjacent to the lower tube sheet.
>
> (3) Fouling of the tubes with sodium impurities.
>
> (4) Reduction in heat transfer properties of the secondary sodium due to high impurity levels.
>
> (5) Bypass flow around the seals between the inlet and outlet sodium on either the shell side, tube side or both.
>
> "Further testing is necessary to determine which cause or combination of causes is responsible for the poor performance. The magnitude of the discrepancy between the actual and predicted performance is disturbing, especially since the state of the art for this component was considered by most to be well advanced, requiring a minimum of further development."

The SRE and Hallam intermediate heat exchangers have been reported several times. Reference 18 is the latest. In brief:

> "The SRE intermediate heat exchanger was a horizontal U-tube design. Excessive log mean temperature differences observed during the first power runs reflected inefficiency of the unit and excessive thermal stresses at the tube sheets. The heat transfer surface in the bend area was not effective, due to sodium flow bypassing the tubes as a result of poor baffle design. Following a reactor scram, with the sodium flow reduced to a low value; it was found that a temperature difference of more than 200°F could develop between the top and bottom of the heat exchanger shell, due to stratification of the secondary sodium on the shell side and to recirculation of the primary sodium from the top to the bottom tubes.
>
> "The HNPF intermediate heat exchangers did not suffer from the heat transfer and temperature stratification problems experienced with the SRE units, because they were mounted vertically and were adequately baffled. However, in November 1962, a secondary-sodium to primary-sodium leak was detected in the intermediate heat exchanger unit 1-A. It was found that a tube had failed, due to vibration-induced fatigue in the region of a baffle plate. This condition was corrected by installing dampening shims between the tubes. Subsequent vibration testing showed that the vibration had been eliminated and the units operated with no further difficulty.
>
> "As a result of the HNPF intermediate heat exchanger vibration experience, the new SRE-Power Expansion Program intermediate heat exchanger, rated at 40 MWt, was analyzed and found to be subject to vibration difficulties. This unit was therefore shimmed before being operated. Upon testing in the system, no vibration could be detected."

Presently installed in the SCTI is an Alco-BLH heat exchanger. This unit will be tested under the AEC's Component Development Program. United Nuclear Corporation has been retained by the AEC to analyze and evaluate the performance of the unit. Analysis has indicated that forcing frequencies may be higher than the natural frequency of the tubes and, therefore, tube vibration is a possibility. Test data tend to confirm a resonant condition at approximately 40% flow, as compared with 38% by analysis. An additional peak was found at 20% flow. The amplitude of vibration is believed to be small enough that damaging impact is unlikely. Gas entrainment is considered a possible cause. The investigation is continuing.

The Rapsodie heat exchangers have performed in accordance with expectations.(7) Initial data indicates a log mean temperature difference of 27°F. This compares with the calculated value of 34°F. The only difficulty, to date, has resulted from an improper design detail. Originally, the internal volume of the heat exchanger was connected to the annular space between the double-walled pipe. A sodium leak into the piping space flooded the shield plug above the tube bundle. It was necessary to dismantle the heat exchanger to clean the unit.

New Plant Components

Several intermediate heat exchangers are in design, construction, or nearing operation for the PFR, Phenix, and BN-350 facilities. Table 5 lists some of their pertinent characteristics.

PFR Intermediate Heat Exchanger (112 MW) (Fig. 6).[11] The PFR units are of the counterflow, shell and tube type with primary coolant on the tube side. The secondary coolant is supplied to the bottom of the shell side through a central pipe. Above the tube bundle the secondary inlet and outlet pipes are concentrically located with outlet coolant flowing upward in the annulus region. A shut-off valve is provided at the primary inlet. Flexibility between tubes and shell is provided by sinusoidal bends in the tubes. The shell is unbaffled. Tube supports at periodic intervals are expected to minimize tube vibration. Tests on full-scale rigs have shown no problems with vibration or coolant distribution.

Phenix Intermediate Heat Exchanger (100 MW) (Fig. 7).[7,13] The Phenix heat exchangers are of the counterflow, shell and tube type with primary coolant on the shell side. The units are similar to the Rapsodie heat exchanger. Some of its features are; a primary-coolant inlet valve, staggered inlet and outlet piping in the shield plug region, and a cylindrical sleeve of borated steel around the tube bundle for neutron shielding. One of the differences from the Rapsodie unit is the use of straight tubes. Primary sodium enters the shell side of tube bundle through a 16-in. high opening, flows downward, and exits through a 24-in. opening in the tube-bundle shroud. Secondary sodium flows downward through the center of the tube bundle in a 15-in. pipe and then upward through some 2450 tubes. The tubes have diameters of 12 mm internal and 14 mm external. Hydraulic pressure losses are calculated to be $\sim$0.6 psi on the primary side and $\sim$8 psi on the secondary side. A logarithmic temperature difference of $\sim$45°F is projected. Hydraulic testing with water on a full-scale unit is planned to investigate flow distribution, pressure losses, and tube vibration considerations.

BN-350 Intermediate Heat Exchanger (200 MW) (Fig. 8)[14] The BN-350 units are described in Reference 12:

> "The intermediate heat exchanger is a shell and tube structure in two sections connected in parallel. The section is made in the form of a horizontal vessel of rectangular cross-section with 3 heat-transfer bundles in it. Each bundle consists of 343 U-shaped tubes 28 x 2 mm in diameter arranged with a pitch sideways and in depth of 35 mm. The heat exchanger is made of 18 Cr 9 Ni steel. The bundles in each section are connected in series with respect to the coolant in the primary circuit and in the secondary circuit. Movement of the coolants is counter-current with cross flow. The radioactive sodium of the primary circuit enters the space between the tubes where it gives up its heat to the sodium in the secondary circuit which is moving inside the tubes. The vertical U-shaped tube bundle has a cylindrical shape. Therefore, to ensure uniform distribution of the flow of coolant across the shell, the tube system of each bundle is extended to a rectangular shape by fitting fixed non-operative straight tubes.
>
> "To create a uniform primary sodium flow an equalizing grid is fitted at the entry to the first bundle. When necessary, any bundle in the heat exchanger can be taken out and replaced by a new one. To ensure the possibility of carrying out the work of extraction of the bundle, biological shielding is provided at its inlet and outlet chamber in the secondary circuit, with special channels for the passage of the secondary sodium. The bundle is

TABLE 5. Design Characteristics of Heat Exchangers[7,11,13,14]

Plant	PFR	BN-350	Phenix
Country	U.K.	USSR	France
Location	Dounreay	Mangyshlak Pennisula	--
Capacity (MW)	112	200	100
No. of Units	6	6	6
Shell-side fluid			
Flow rate (10^6 lb/hr)	3.87	5.8	4.1
Temp. in (°F)	698	523	1060
Temp. out (°F)	1045	847	790
Tube-side fluid			
Flow rate (10^6 lb/hr)	4.25	5.2	3.1
Temp. in (°F)	1085	932	680
Temp. out (°F)	788	572	1040
Heat transfer			
No. of tubes	1800	343/bundle 3 bundles/section 2 sections/unit	2450
Tube size, OD (in.)	0.75	1.1	0.56
Wall thickness (in.)	0.04	0.08	0.04
Area (sq. ft.)	~ 5400	~12,100	~4670
Material	SS	18 Cr 9 Ni	SS

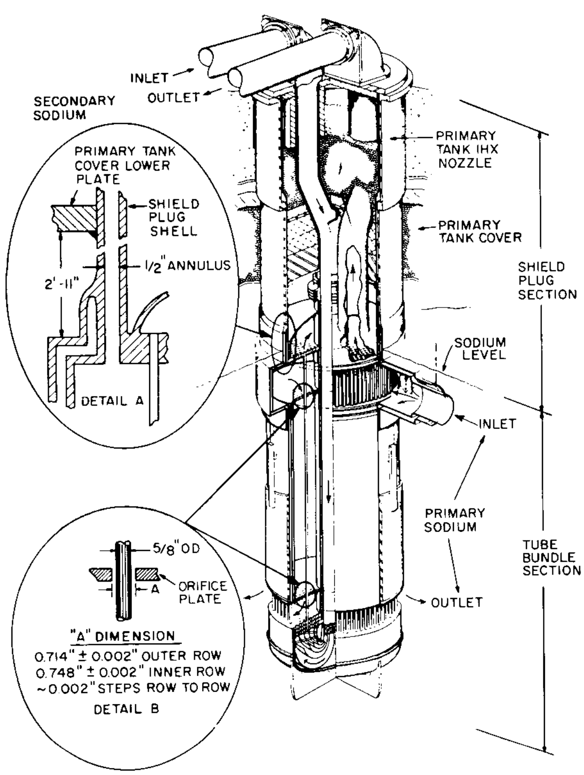

Fig. 5. EBR-II Heat Exchanger (62.5 MW)

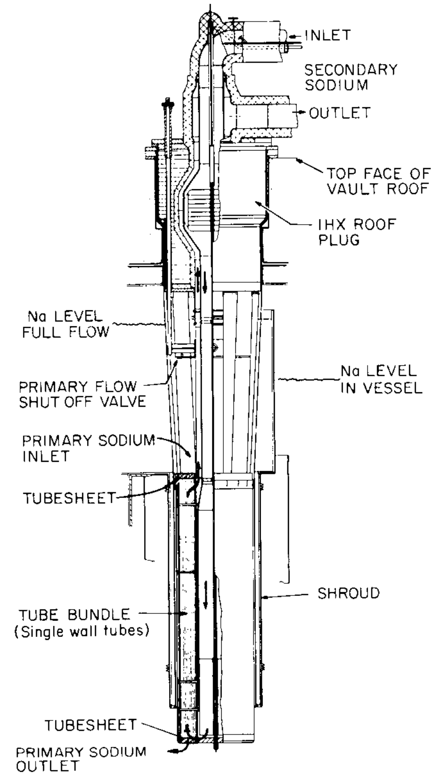

Fig. 6. PFR Heat Exchanger(11) (112 MW)

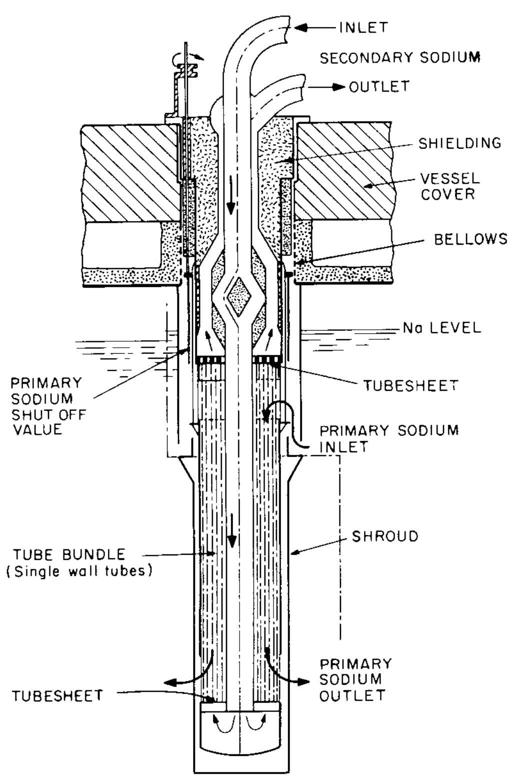

Fig. 7. Phenix Heat Exchanger(13) (100 MW)

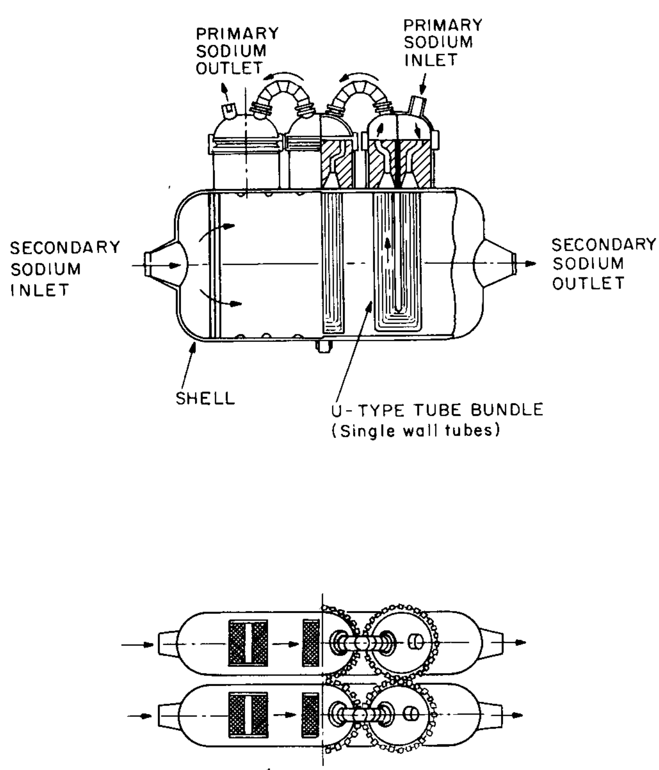

Fig. 8. BN-350 Heat Exchanger(14) (200 MW)

fastened to the neck with a special locking device and the bundle chamber and neck are sealed by "moustache welding."

Current Work

The AEC's Component Development Program calls for the thorough development of a technology for intermediate heat exchangers, culminating in the fabrication and testing of test units. The plan includes the following tasks:

(1) Determination of functional requirements

(2) Documentation of the state-of-the-art and the development of criteria, quality assurance procedures, and standards

(3) Development of design technology

(4) Development of new heat exchanger concepts

(5) Development of fabrication technology

(6) Testing of model heat exchangers

The program provides for the complete development of a number of heat exchangers with the testing of models of 30 to 50 MW. This program is a continuation and extension of the current program which includes testing the Alco-BLH unit in the SCTI. Preparation for implementation of the work is in progress.

Development Needs

Principal problems with heat exchangers have been undesirable flow and temperature distributions and tube vibration. These problems have not severely limited plant operations, to date, but portend conditions that could be troublesome in the future. Horizontal heat exchanger units are inherently susceptible to maldistribution of temperature especially during conditions of low flow. The return-bend portion of a U-bundle unit requires special attention to flow distribution or short-circuiting of flow will occur. Vertical exchangers with low hydraulic pressure losses are also vulnerable to poor flow distribution. Very careful attention to flow analysis, baffle design and placement, and flow patterns is required. The results of poor distribution of flow can cause high stresses and strains, structural failures, and poor thermal efficiency.

Tube vibrations are a particular concern because, in most instances, their natural frequency characteristics are low. Tube vibration can quickly result in wear, material fatigue, and structural failure. Structural vibration in sodium heat exchangers is a complex situation developing from many contributing causes. Because of difficulty in performing maintenance, e.g., finding and repairing a leak, LMFBR heat exchangers must have a high degree of reliability--higher than heat exchangers for service in water reactors. Therefore, there is a considerable incentive to investigate tube vibration problems and to develop better methods to analyze and avoid these difficulties.

Other areas of concern include the effects of thermal transients, gas entrainment if the use of a free internal liquid level is considered, and fabrication methods and quality assurance procedures for very large units. Improved design practices and procedures are required. All of these should be reflected in codes and standards that adequately reflect LMFBR requirements.

PIPING, VALVES, ETC.

Operating Experience--Piping

The bulk of experience with large-diameter sodium-piping systems exists for design temperatures up to 1000°F and for piping diameters of 12 inches or less. The stainless steels (mainly, Type 304) are the principal materials of construction, although some ferritic piping (2-1/4 Cr-1 Mo) is used in the EBR-II secondary system. Conventional design procedures have been adequate to date for these conditions.

The principal sources of experience with piping of diameters greater than 12 inches are Hallam and Fermi. Piping up to 16 inch diameter was used at Hallam, and the then current technology was deemed adequate for these systems. At Fermi, piping of 30 inch diameter by 3/8 inch wall is used between the outlet of the reactor vessel to the intermediate heat exchanger; a length of about 70 ft. The line is designed to operate at 900°F, at virtually atmospheric pressure. Lack of experience with such systems required modification of usual piping design and fabrication procedures. Some practices instituted for Fermi, as reported by Peters[19] are:

(1) Studies to determine the effects of mass transport, corrosion, and thermal shock.

(2) Design requirements, which included 100% cold spring, minimum number of field welds, no rings (e.g., riser clamps) on piping circumference, and in the primary system, no backing rings for welding.

(3) Fabrication testing, including complete ultrasonic testing of flat plates prior to shaping, radiographic inspection of all circumferential and longitudinal welds, liquid pentrant examination of all weld joints, hydrostatic tests in the shop--none in the field, and mass spectrometer leak tests of all piping assemblies and field welds.

(4) Rigid specifications on the quality of inside surface finish, material chemistry, cleaning, shipping, and storage.

Experiences with LMFBR piping is generally favorable. However, some difficulties have been reported. These include:

(1) Difficulties[19] in welding large diameter piping due to the shrinkage characteristics of austenitic material. In one instance, welding a circumferential joint of a 30 inch Fermi pipe reduced the diameter by one inch. Careful control of heat input during welding can minimize such difficulties.

(2) Many cracks [20] were discovered in a 2-1/2 inch Type 304 L stainless steel pipe tee and adjacent piping that were used to mix two streams of sodium at different temperatures in the LASL 2000-kW Sodium Test Facility. Two major cracks and numerous smaller ones were observed upon disassembly of the loop. Each large crack was about 1-3/4 inch long and and penetrated about 75 percent of the wall thickness from the inside. The inside surface of the piping at the mixed-flow outlet end of the tee was severely cracked for about 18 inches. The cracks were transgranular and penetrated about 12-1/2 percent of the thickness of the pipe wall. It was concluded that thermal-stress fatigue resulted from cyclic temperature conditions caused by the mixing of hot and cold streams. Stream temperature differences varied from a maximum of 361°F to a minimum of 270°F during steady operation.

Mixing coolant streams of different temperatures is of particular concern in sodium systems as illustrated by the conditions described in Reference 20. Low thermal resistance of the fluid at the boundary of the pipe wall induces the temperature of the pipe wall to follow closely the temperature of the fluid. Rapid changes in fluid temperature causes rapid changes in the temperature and stress condition of the inner fibers of the pipe wall. With sufficient cycles, a function of the ΔT, these conditions can lead to thermal-stress fatigue and eventually failure of the material. System areas especially susceptible to these conditions are the mixing juncture of bypass lines around heat exchangers, the point of return of effluent from hot and cold traps to the main system, and the mixing of bypass streams around the reactor.

(3) After about 15,000 hours of operation, a secondary expansion tank at Hallam failed at the point of junction of the tank wall and an internal baffle.[21] The tank was fabricated of Type 304 stainless steel. The baffle was joined to the tank by filet welds. The primary leak was caused by stress corrosion cracking caused by locally high stresses and highly corrosive material trapped within the filet weld. A secondary leak developed owing to gross attack of the outer surface of the vessel by sodium reaction products resulting from the leak.

(4) A report on the operations of BR-5 includes the following:[22]

> "At the opening stage of operation when the pile worked on a low power level, two cases were recorded when wire electric heaters burnt first the pulse tube and then the drainage pipeline of the primary circuit. In order to prevent such developments an electric heating supply diagram was carried out on the installation for the circuits through the separating transformer, which made it possible to rule out burning through the pipelines when the electric heaters shorted."

(5) Finally, a leak occurred in the DFR early in 1967. The plant has been shut down since July 1967 for repairs. Late in 1967, the leak was located in one of the 24 heat exchanger connecting pipes, No. 10A, about a foot from the bottom in the region of the reactor-vessel nozzle. Repair procedures are in progress, with plant operation not expected for some months.

Operating Experiences--Valves

With some exceptions, valves for sodium systems have conventional configurations and are used for conventional purposes, such as isolation, anti-flow reversal, and flow control. Many of the larger valves that have been used for sodium service are described in Table 6. The largest that have seen significant service are the Hallam (HNPF) valves and the Fermi check valves. The configurations of these valves are shown in Figures 9, 10, 11, and 12.

The blocking valve (Fig. 9) for the HNPF main heat-transfer system is a 14-in., all-welded stainless-steel gate valve (sold-wedge type) with a stem freeze seal. There is one in each of the three primary and secondary sodium loops. The valves are operated through drive shafts by remotely located pneumatic-turbine operators equipped with manual override. These valves performed well. Some leakage across the seat was detected but the rate was sufficiently low to be inconsequential.

The throttling valve (Fig. 10) for the HNPF main heat-transfer system, is a 14-in. all-welded stainless-steel valve of the venturi-ball type with a stem freeze seal. There is one in each of the three primary and secondary sodium

TABLE 6. Summary of Valve Designs

	HNPF Throttling Valve	HNPF Blocking Valve	HNPF Check Valve	HNPF Moderator Throttling Valve	SCTI Butterfly Valve	SRE Blocking Valve	LCTL Blocking Valve	Fermi Check Valve	Fermi Blanket Throttle Valve	EBR-II Pump Test Loop Throttling Valve	Flow Control Valve	Flow Control Valve
Size (in.)	14	14	14	6	8	6	8	16	6	12	10	12
Type	Venturi-ball	Solid-wedge gate	Swing gate	Venturi-ball	Butterfly	Lift-plug	Double-disc gate	Balanced-disc	Plug	Globe	Modified plug	Modified plug
Operating conditions												
Fluid	Sodium	Sodium	Sodium	Sodium	Sodium	Sodium	Sodium	Sodium	Sodium	Sodium	Sodium	Sodium
Temp (°F, max)	610	945	610	610	1,300 (Design)	525	1,000 (Design)	550	550	1,000 (Design)	1,000 (Design)	1,000 (Design)
Pressure (psig)	50	50	50	50	125	100 (Design)	N/A	50*	50*	150 (Design)	100 (Design)	100 (Design)
Flow rate (lbs/hr, max)	2,800,000	2,800,000	2,800,000	N/A	1,400,000	510,000	480,000	2,950,000*	350,000*	N/A	2,000,000	N/A
Stem seal	Freeze	Freeze	None	Freeze	Freeze	Freeze	Freeze	None	Double bellows	Bellows	Torque-tube	Enclosed stator assembly
Manufacturer	General Kinetics Corp.	Cooper Alloy Corp.	Cooper Alloy Corp.	General Kinetics Corp.	Continental Equipment Co.	Wedgeplug Valve Co.	Cooper Alloy Corp.	Edward Valves Inc.	Copes-Vulcan Division Blaw-Knox Co.	Crane Co.	Ohio Injector Co.	Westinghouse
Operator			None					None				
Type	Pneumatic	Pneumatic	-	Pneumatic	Pneumatic	Manual-handwheel	Manual or Pneumatic	-	Manual-handwheel	Manual-handwheel	Manual or Electric	Electric Permanent magnet coupling
Manufacturer	EIM Co.	EIM Co.	-	EIM Co.	Fisher Governor Co.	Atomics Internat'l.	Same	-	Same	Same	Same	Limitorque

*200-MWt reactor power.

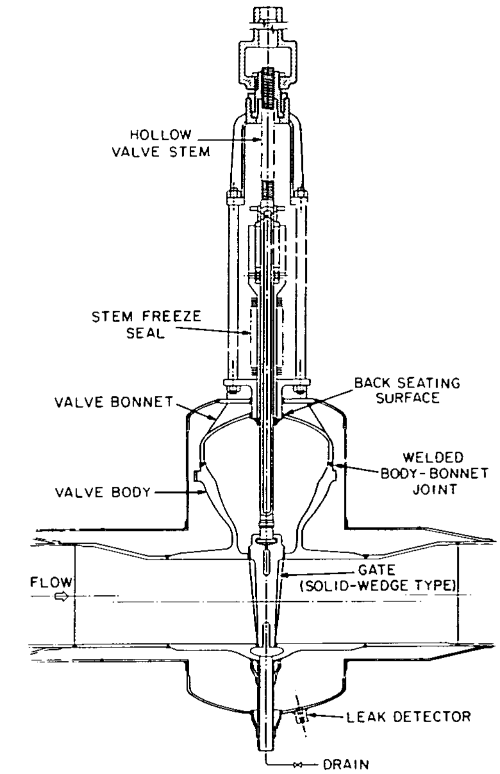

Fig. 9. HNPF 14-in. Primary Blocking Valve(27)

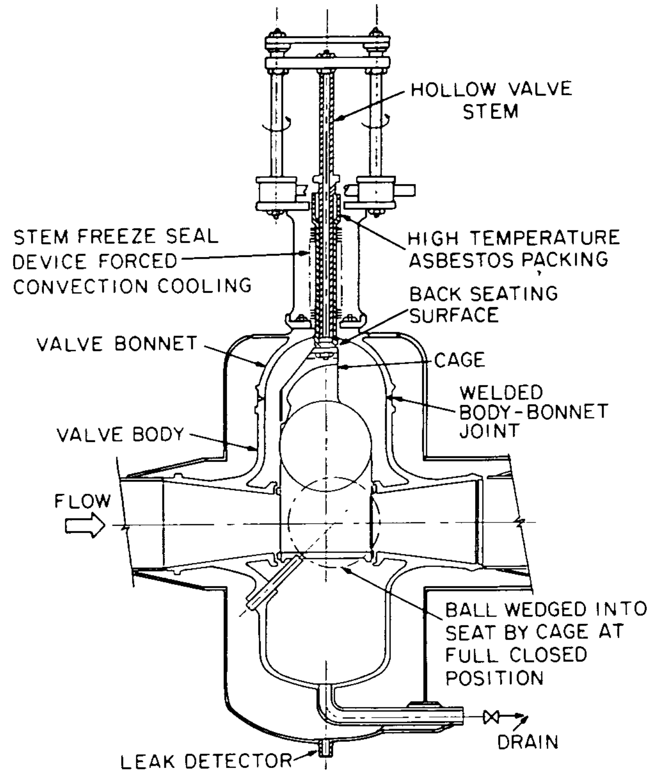

Fig. 10. HNPF 14-in. Primary Throttling Valve(28)

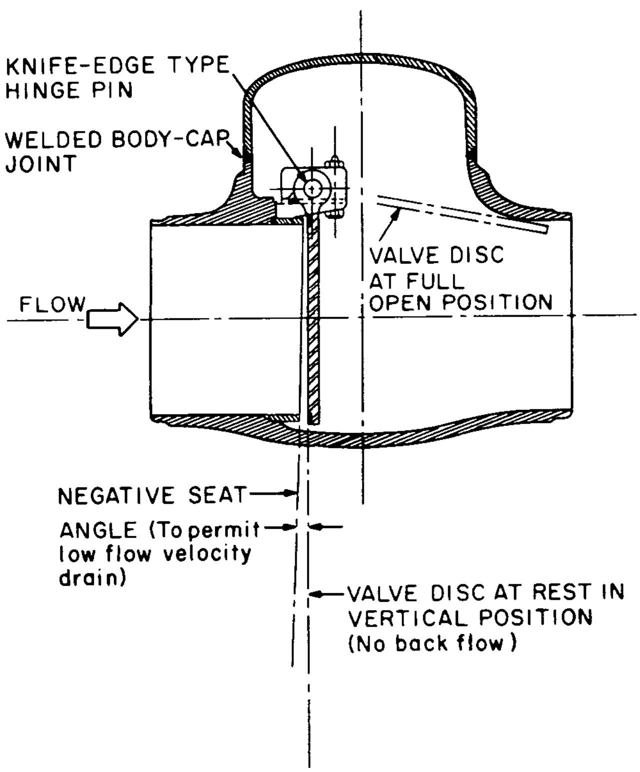

Fig. 11. HNPF 14-in. Primary Check Valve(28)

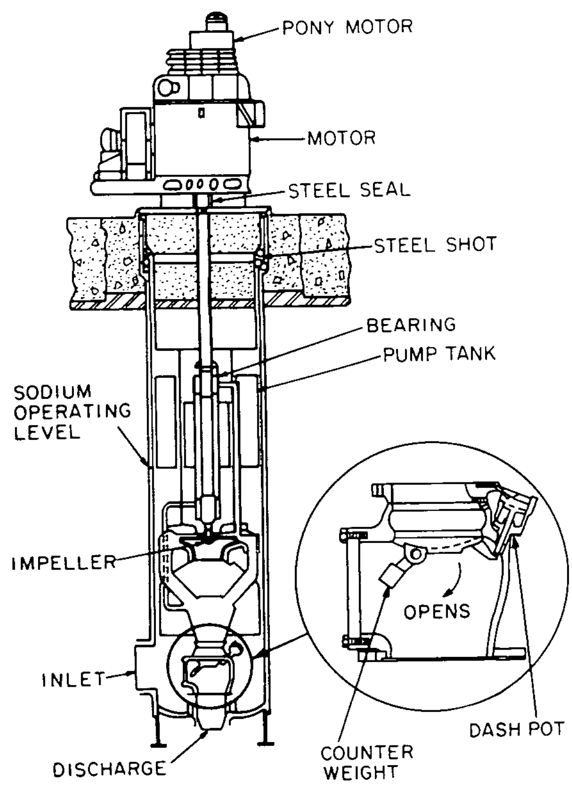

Fig. 12. Fermi Primary Pump and 30-in. Check Valve(28)

loops. The valves operated through drive shafts by remotely located pneumatic-turbine operators equipped with manual override. The primary functions of the throttling valves were to regulate natural-convection sodium flow in the primary and secondary loops following a reactor scram, and to serve as a blocking valve as required during a loop shutdown. The throttling valve was designed to be quick acting, automatically controlled by the plant control system, actuated by the reactor scram circuits, and tight at shutoff.

Several malfunctions occurred in the throttling-valve systems during plant startup and the initial operating period. Most of the difficulties occurred in the drive system, which consists of the operator, drive train, and topworks. Only one valve proper failed. The cause of this failure was not determined precisely; however, examination of the valve after removal from the system showed a cracked back-seat surface and a slightly bent stem.[23] The valve performed without difficulty when it was re-installed after repair by the manufacturer. During the periods of plant startup and initial operation, a series of modifications were made to the drive systems of the throttling-valves, especially the topworks. Initially, the valves performed satisfactorily. Later, minor difficulties occurred and the application of torque required to actuate the valves often damaged the topworks. These difficulties, in conjunction with the need for faster-acting valves, resulted in the decision to redesign and fabricate new topworks. The valves and valve-drive systems performed without difficulty after new topworks were installed and a slip clutch was added to each of the valve operators.

The HNPF check valve (Fig. 11), one in each primary loop, is a 14-inch, all-welded stainless steel, swing-gate type with a negative off-set seat. No difficulties were encountered with these valves.

Check valves are employed in each of the three primary sodium loops of the Fermi Plant to prevent reverse flow of sodium in the event of a primary-pump failure. The 16-in. balanced disc-type valves (Fig. 12) are attached to the discharge nozzle of each of the three primary pumps and are removable with the pump. The valves are fabricated of Type 304 stainless steel and lined with Stellite to prevent galling wherever metal-to-metal contact exists. Sodium-circulation channels are provided along contacting surfaces to prevent an accumulation of solid impurities.

During pre-operational testing of the primary system abrupt closure of the valve was detected. Visual inspection disclosed pipe movements of considerable magnitude--both when a single pump was started and when a single pump was tripped with two or three pumps running in parallel. The cause was the rapid closure of the check valve, which slammed shut with an audible sound. As a result of special system tests and studies[24], the check valves were replaced with valves of a new design, which incorporated a larger body, spring-loaded hinge pins, and a sodium-filled dashpot. In the new design, the disc operates through the first 40° by the action of hinge pins, while the dashpot controls the disc through the last 12° of closure. In each loop, natural circulation is adequate because the valve is designed to hang open at 12° when the pump is shut down. System operation since installation of new check valves has shown the slamming problem to be resolved.

Argonne National Laboratory (ANL) purchased three valves for use in the EBR-II Pump Test Loops--one valve for each of three loops. The valves were 12-in., stainless-steel, Y-pattern globe valves with a bellows-sealed stem, designed for sodium service at 150 psig and 1000°F. They were manually operated with a handwheel drive through an integral bevelgear drive.

The valves were satisfactory except when used to throttle flow. Throttling between 75 and 100 percent of full closure produced excessive vibration of the plug. No effort was made to correct the vibration, since the tests were designed for pump development, not valve development. One of the valves is now in use in the drain system of EBR-II. Thus far, it has performed satisfactorily. The valve is not used for throttling flow and is used either in the full-open or full-close position.

In the SCTI, sodium-flow-control valves are 6- and 8-in. cast stainless steel, butterfly type, with a metal piston ring around the disc and a stem freeze seal cooled by natural convection. There are five in the primary system and one in the secondary system. Remotely controlled pneumatic-piston type operators are used. The valves regulate flow in the main primary and secondary sodium loops and component bypass loops. The use of a butterfly valve results in low pressure drop, relatively small valves, and a minimum of distortion due to thermal transients. The primary-system valves have operated without difficulty through 5000-6000 operating hours of the SCTI primary system, at sodium temperatures ranging from 300° to 800°F. The flow-control valve in the secondary loop also has operated without difficulty, but for a shorter time.

The SRE blocking valves are 6-in. stainless-steel, plug-type valves with a stem freeze seal. Originally, they were installed in the primary sodium loop. They were manually operated from outside of the primary system vault through a handwheel and flexible drive shaft. Frequently, these valves leaked across the seat when closed and were difficult to operate.

A flow-control valve and operator were developed by the Ohio Injector Company (OIC), Wadsworth, Ohio, (under AEC contract) for use in sodium systems. The development program included design, development, construction of a prototype, and testing in air and water. In addition, the prototype device was tested successfully[25] (by Atomics International) in a sodium test loop at design temperatures and flow rates. Subsequently, the prototype was installed in the Sodium Reactor Experiment (SRE) primary loop during the Power Expansion Program modifications.

The OIC flow-control valve prototype is a 10-in. modified-plug valve, with a torque-tube shaft seal. The torque-tube seal concept was evaluated through environmental tests of the seal conducted by Atomics International. The seal withstood 1000 operational cycles in sodium at 1000°F and 1200°F without developing a leak. The device utilizes a cylindrical body-control element arrangement, which provides hydrostatically and hydrodynamically balanced fluid forces on the movable control element. Throttling characteristics are predetermined and controlled by contoured throttling apertures in the body wall. The operator design permits three drive modes: (1) electric-motor drive for fast operation (about two seconds for full closure), (2) electric-motor drive for slow operation (about 60 seconds for full closure), and (3) operation with a handwheel.

A flow-control valve and operator assembly was also developed by Westinghouse Electric Corp. (under AEC contract) for use in sodium systems. A horizontally positioned control cone actuated by a screw-type, duplex-toggle-arm linkage throttled the flow. Roller nuts, equipped with Stellite ball bearings transmitted the rotary motion of a drive screw into linear motion. Toggle arms converted the vertical linear motion into horizontal motion of the cone, thereby effecting a variable orifice between the cone and the valve body. The drive screw was driven by a totally enclosed stator driven by flux linkages supplied by an electric drive through a thin-wall container. During a hot-gas test at 1000°F the cone actuating mechanisms failed. Further development was discontinued.

Current Work

A recent review(26) of piping system requirements for 1000-MWe fast breeder reactors, based upon results of the 1000-MWe design studies, concluded that:

> ". . . (piping) design based upon the present ASA Power Piping Code rules seems to present no great difficulties. However, . . . , it is questionable whether these rules are adequate to insure a completely safe design in this highly critical service."

It was concluded further that improved design procedures must be developed. These procedures must be able to assess properly: inelastic behavior of materials; likelihood of instability or buckling due to creep and/or loading conditions; effect of local overstrain, plastic deformation, or thermal ratcheting; and effects of dimensions on stress and strain distributions.

Within the last five or six years, several studies on piping have been initiated. The Navy's Non-Nuclear Pipe Weld Project was established to determine the causes of high rejection rates in welded piping. As part of this investigation, pipe-weld defects were described and defined and acceptance standards were recommended. The American Gas Association's NG-18 Line-Pipe Project (performed at Battelle Memorial Institute) included full-scale experiment and fundamental studies to define the significant factors associated with the strength and fracture of line pipe. The Southwest Research Institute conducted full-scale studies on pressure vessels of various materials and designs. General Electric (at San Jose, California) also has made surveys of piping failure, established design considerations for primary nuclear piping, and conducted work on fatigue at high temperature.

Undoubtedly, some of the information developed will be applicable to LMFBR systems. However, specific studies are needed to develop LMFBR piping technology.

The AEC's Component Development Plan addresses piping needs. It recognizes that a better understanding of failure modes and improved design procedures are needed. Fabrication techniques need to be established, codes must be updated, and standards modified to suffice for LMFBR service requirements. The plan of action calls out the following tasks.

(1) Determination of functional requirements

(2) Documentation of state-of-the-art of LMFBR piping and development of criteria, quality control procedures, and standards

(3) Development of design technology

(4) Development of designs for typical LMFBR piping systems

(5) Development of fabrication technology

The validity of the present "stress-range" concept will be investigated--both analytically and experimentally. The practical problems of system design will be explored. Fabrication procedures, codes, and standards will be upgraded for LMFBR applications.

Currently the AEC is preparing to solicit proposals from industry to begin development work on piping. Proposals for the development of valves have been solicited.

Development Needs

Prevailing practices have been adequate for piping system components for current plants. Large LMFBRs, however, will require components that far exceed those now in use. Because of the vital role of these components, a thorough investigation of piping technology is required. Fabrication procedures and inspection techniques for thin-wall stainless steel components must be developed and/or applied to field erected piping. Design practices must be revised and developed, praticularly in regard to methods for flexibility calculations, use of existing flexibility and stress-intensification factors, and creep in the area of concentrated loads. Piping support and heating practices are faced with new problems such as large load changes from empty to full, the effects of maldistribution of temperature during heatup on an empty piping system, and the accommodation of thermal expansion--bellows are unproven and currently unacceptable for this service. Instability of large, thin-walled piping operating at temperatures to 1200°F is a further consideration.

Valves have always required maintenance and have been susceptible to maloperation. Valve problems stem chiefly from the need to transmit motion through the coolant-pressure boundary. A variety of seals, such as bellows and freeze seals have been used to exclude leakage. Bellows are quite vulnerable to maloperation, whereas freeze seals tend to require high torque loads by valve operators. Until problems with valve seals are solved, permitting more reliable valve operation and less maintenance, designers will avoid their use where possible.

These problems emphasize the need for development work in these areas and the importance of a thorough review and upgrading of codes and standards for LMFBR conditions.

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DISCUSSION

K. Goldmann (United Nuclear) - This paper presents an authoritative survey of past experience, current work and development needs for sodium system components.

The long list of failures, described in the paper, may at first seem discouraging. Considering, however, that these were all first-of-a-kind, untested components, the incident rate does not appear to be excessively high. It should be noted further, that the difficulties could usually be overcome with engineering measures and that none could be traced to a lack of sodium materials technology. This provides further support to similar statements which this reviewer made earlier today.*

One area, which was not covered in my paper was heat transfer predictions. There are still considerable uncertainties as to the proper formulation of cross-flow heat transfer correlations for sodium. Of special concern here are the predicted shell-side heat transfer coefficients for sizing surface areas of intermediate heat exchangers. A much larger fraction of the difference between predicted and observed performance of the EFAPP exchangers than that stated in the paper, may be due to this uncertainty. It may be desirable, therefore, to add the prediction of shell-side heat transfer coefficients to the development needs.

R.W. Dickinson (Liquid Metal Engineering Center) - The experience summarized in this paper indicates that sodium system components can be designed and constructed to give reliable service, and that sodium per se is not an intractable fluid. I believe, however, that the components built and demonstrated to date are evolutionary, and represent extrapolations of laboratory scale equipment. In the LMFBR Program, we are facing more than evolution and extrapolation, as the capacity and capability of components must be increased approximately by an order of magnitude. While components of this size in other service have certainly been constructed, they have not been required to operate through the temperature ranges necessary in a sodium system, nor face the rates of change of temperature which may be encountered. Consequently, I do not believe that technology derived from the construction of large, low temperature systems can be applied "across the board" to sodium systems. It is recognized that the author does not recommend this; my point is that development of these larger components is not an easy matter. For instance, the wall thickness of a 30-in. pipe for sodium systems is apt to be 1/2 to 3/4-in.; ordinary piping calculations applicable to 14 inch Schedule 40 piping do not contend with this situation as buckling and inelastic behavior mechanisms are apt to enter prominently. The same philosophy applies to other components; for instance, simple extrapolation of valve technology would result in massive valve bodies which would be susceptible to thermal shock in sodium service. I believe there is a real challenge to component designers, who must understand all the facets of sodium systems, including maintenance, fabrication, resistance to thermal transients, and full utilization of the unique properties of sodium, rather than relying on past experience to simply make components "bigger and better." The program plans briefly outlined by the author must be fully recognized by the potential vendors in this field to assure success.

*"Liquid Sodium as a Reactor Coolant" by K. Goldmann, this conference. Reference No. 26.

R.W. Lockhart (General Electric) - In this paper, Wally Simmons has updated the Morabito-Savage sodium component paper of 1965, delineated the present AEC-Sodium Component Development plans and has also described development needs for sodium system components. Thus, this is a useful state of the art summary. It is encouraging to me to see so much hardware oriented operation and development work in planning and in progress.

Recently the U.S. light water and British gas-cooled thermal power reactor business has grown to the point where multiple "identical design" plants are operating or are under construction. I believe the two SIR plants are the only "near duplication" sodium-cooled plants. When the FBR plants are ordered in multiplicity, I think it will be soon enough to consider upping plant operating characteristics, particularly the reactor vessel outlet temperature.

Today's programs should include the developments that have been recommended in the paper for the various components. However, I would like to see fuller utilization of existing and near term data for system aspects of design. For instance:

1. System availability analyses
2. System cost effectiveness analyses
3. System material application and mass transfer predictions
4. "Old fashioned" type engineering-operations system and component design integration.

Component reliability and/or integrity is most desirable but plant availability is the real system measure. Considerable (additional) design, accessibility and repair time work is warranted at this time. Realistic cost effectiveness relationship of these systems and components will affect the design and operating characteristics of the primary system, system components and auxiliaries. These system integration thoughts are not intended to delete or delay the component development, but I think the collection of input data and fuller utilization of statistical techniques and analyses will provide sharper focus on the real problem areas and enhance the possibility of meeting some of the mid-1970 schedule dates.

W.R. Simmons - Each of the commentators has a wealth of experience in the field of components for sodium systems -- dating to the early days of SIR. Each has made a number of pertinent comments with which I'm in general harmony.

It is generally agreed that much work lies ahead to develop commercial LMFBRs and that one of the larger areas of effort will be component development. One of the more urgent tasks currently before us is the delineation of equipment needs. This requires a good understanding of those plants that are likely candidates for commercial application and the functional requirements of components for these systems. AEC and industry sponsored plant design studies are currently in progress. The former is directed to 1000-MWe size plants for application in the late 70's and early 80's. The latter is directed toward smaller size demonstration plants, for construction in the early 70's. These studies will be the principal sources of system information during the next few formative years ahead. The systematic delineation of this information is urgently needed to establish definitive functional needs to guide the development of components. In conjunction with these developments, the advice and experience of component vendors is needed to determine the practicality of these requirements.

R

SODIUM HEATED STEAM GENERATORS

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ABSTRACT

This paper is a report of the available information on the development of sodium heated steam generators in the United States. It updates similar reports that were made at previous ANS National Topical Meetings (ANS-100 and ANS-101). The paper includes discussions of: repairs to the Enrico Fermi Atomic Power Plant steam generators; design and development of the Atomics International modular steam generator; construction of the Babcock & Wilcox prototype unit; results of the large-leak sodium water reaction tests at Atomics International; and the small-leak sodium water reaction tests at Atomic Power Development Associates, Inc.

ENRICO FERMI ATOMIC POWER PLANT STEAM GENERATORS

This paper reports the experience for the approximate period March, 1967 to March, 1968. Detailed descriptions of the steam generators and of the experience prior to March, 1967 have been published[1,2] so only the highlights are repeated here.

In the Enrico Fermi Plant there are three steam generators. They are vertical, shell-and-tube, cross and counter flow, once-through units with sodium on the shell side and water and steam on the tube side. Figure 1 is a schematic diagram of the units. Installation of the units at the plant was made in 1960. In 1961, prior to operation with sodium, but subsequent to hydrostatic testing, leaks which later were found to be due to cracks occurred in the tube bends in No. 2 unit. Examination of the cracks and chemical analysis of residues inside the tubes showed that the cracks were attributable to stress corrosion caused by residual cleaning solution left inside the tubes. The No. 2 unit was completely retubed in 1962. Service in sodium was initiated in November 1962. In December 1962 several tubes in the No. 1 steam generator failed due to vibration. The vibration problem was solved by installation of impact baffles between the sodium inlet nozzles and the tube bundle and the vertical tube risers were laced.

One of the principal difficulties with the units has been the leaking tube sheet welds. The original tube-to-tube sheet welds consisted of a fusion weld located at the top surface of the tube sheets as is shown in Figure 2. During the several years of sodium service numerous leaking welds have been experienced in all three units mainly in the water header, and hundreds of the welds have been field repaired in an attempt to alleviate

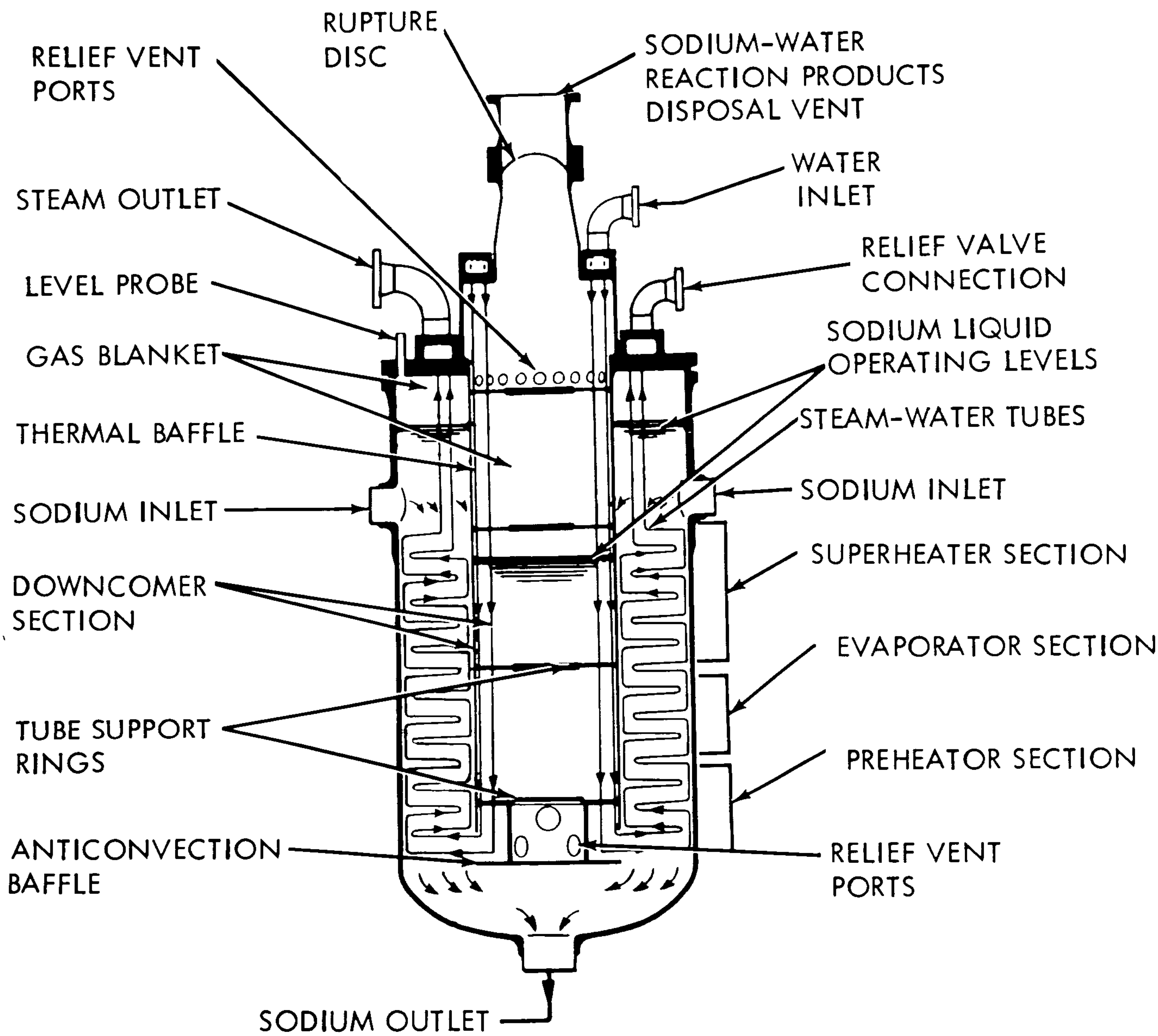

FIG. 1 EFAPP STEAM GENERATOR

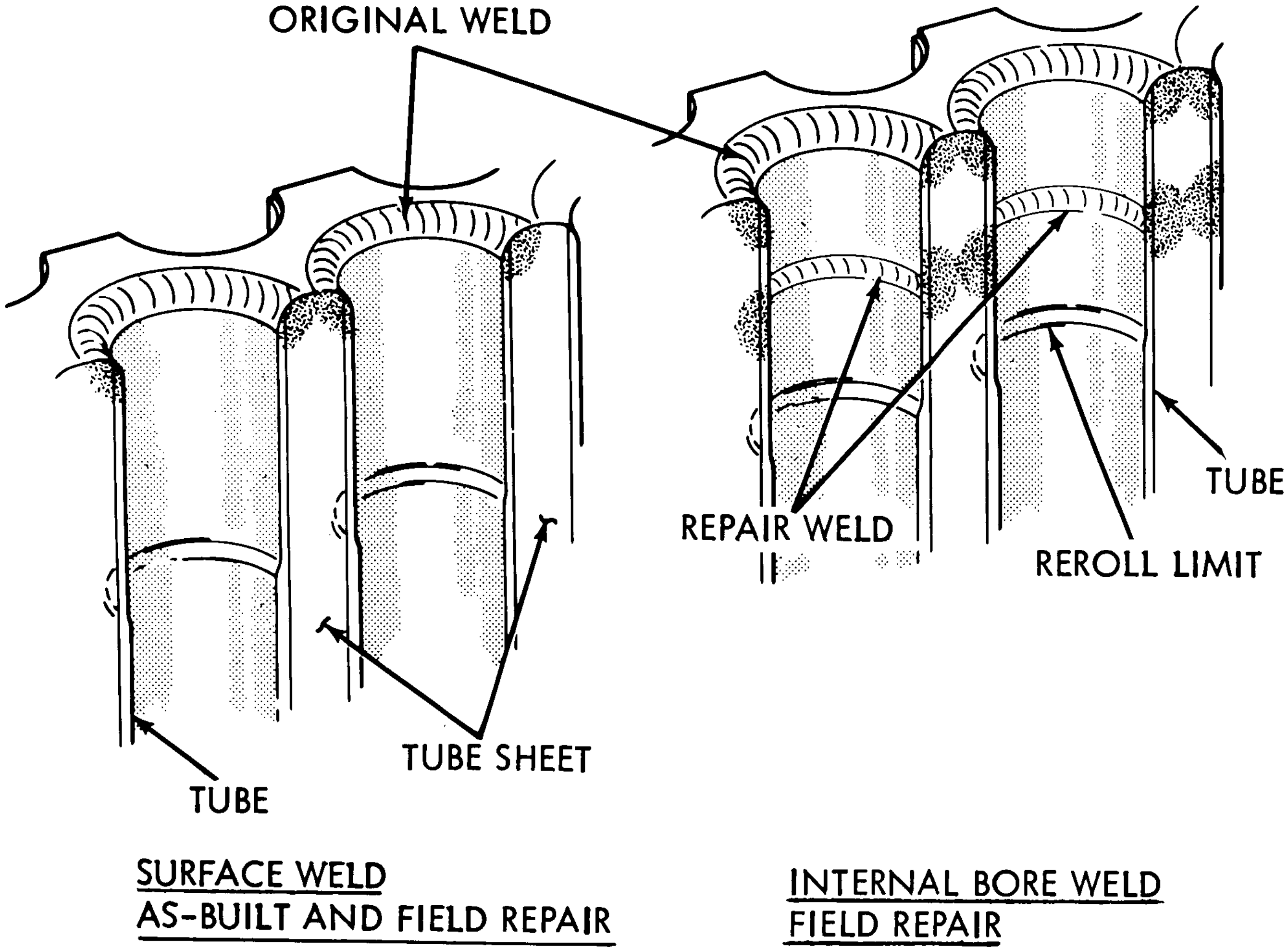

FIG. 2 EFAPP STEAM GENERATOR TUBE TO TUBE SHEET WELD REPAIR

the leakage problem. Generally the defects were found to be pinholes although there were some cracks. The field repairs involved overlaying the original weld with additional weld metal or with nichrobraze 180.

During operation of the steam generators at power levels of 33 and 67 Mwt instability was experienced which resulted in abnormally high oscillations in the sodium outlet temperature. Detailed analysis together with test data showed that the instability was due to flow reversals in the water tubes which was caused by a combination of several factors. To prevent the flow reversals it was decided to install an orifice in the tubes that would increase the pressure drop between the water and steam manifolds.

Throughout the past year the units have been out of service because the plant has not been in operation following the October 6, 1966 fuel meltdown incident. During this time major efforts were undertaken to repair the tube-to-tube sheet welds and to install flow restrictors in the tubes.

The weld repair consists of making an entirely new separate seal weld between the tube and tube sheet utilizing an internal bore welding method developed by the Foster-Wheeler Company. This method employs a machine-rotated tungsten inert gas welding head which is inserted into the steam generator tubes. As shown in Figure 2 a single-pass fusion weld through the tube wall into the tube sheet is made about 3/4 inches below the tube sheet top surface. The welds are leak tested by water flooding the tubes and tube sheet, pressuring the shell with argon gas at 30 to 40 psig, and visual observation for gas bubbles on the water side. All 1200 water manifold tube-to-tube sheet welds in each of the three steam generators are being repaired. The repairs were begun in 1967 and will be completed in 1968.

The modification to prevent flow instabilities consists of installing an orifice in each of the 1200 downcomer tubes of each steam generator. The orifice is designed to increase the pressure drop from the water manifold to the steam manifold. With the original design the pressure drop at full load is only 5 psi and is less at partial load, so that pressure drop imbalances in the water and steam manifolds and in the tubes are believed to have caused flow starvation and even flow reversal in some tubes. With the orifice the pressure drop at full load will be approximately 50 psig. As shown in Figure 3 the orifice consists of a 16-1/2 foot length of 3/16 inch O.D. x 20 gage stainless steel tubing inserted into the tube at the feedwater header. A 5/8 inch diameter stainless steel ball is attached to the top of the orifice and rests on a stainless steel sleeve insert as shown in the sketch. In normal operation, gravity and the hydraulic force of the water will seal the ball on the insert causing the feedwater to flow through the orifice. However, in the event of a water dump operation such as would be required following a large sodium water reaction, the ball will be lifted off its seat and the entire cross section of the downcomer tube will be available for removal of the water. Thus the ball and sleeve function as a check valve. The sleeve also provides a thermal shock protection to the tube-to-tube sheet welds.

Orifices were installed in all tubes of all three units in 1967.

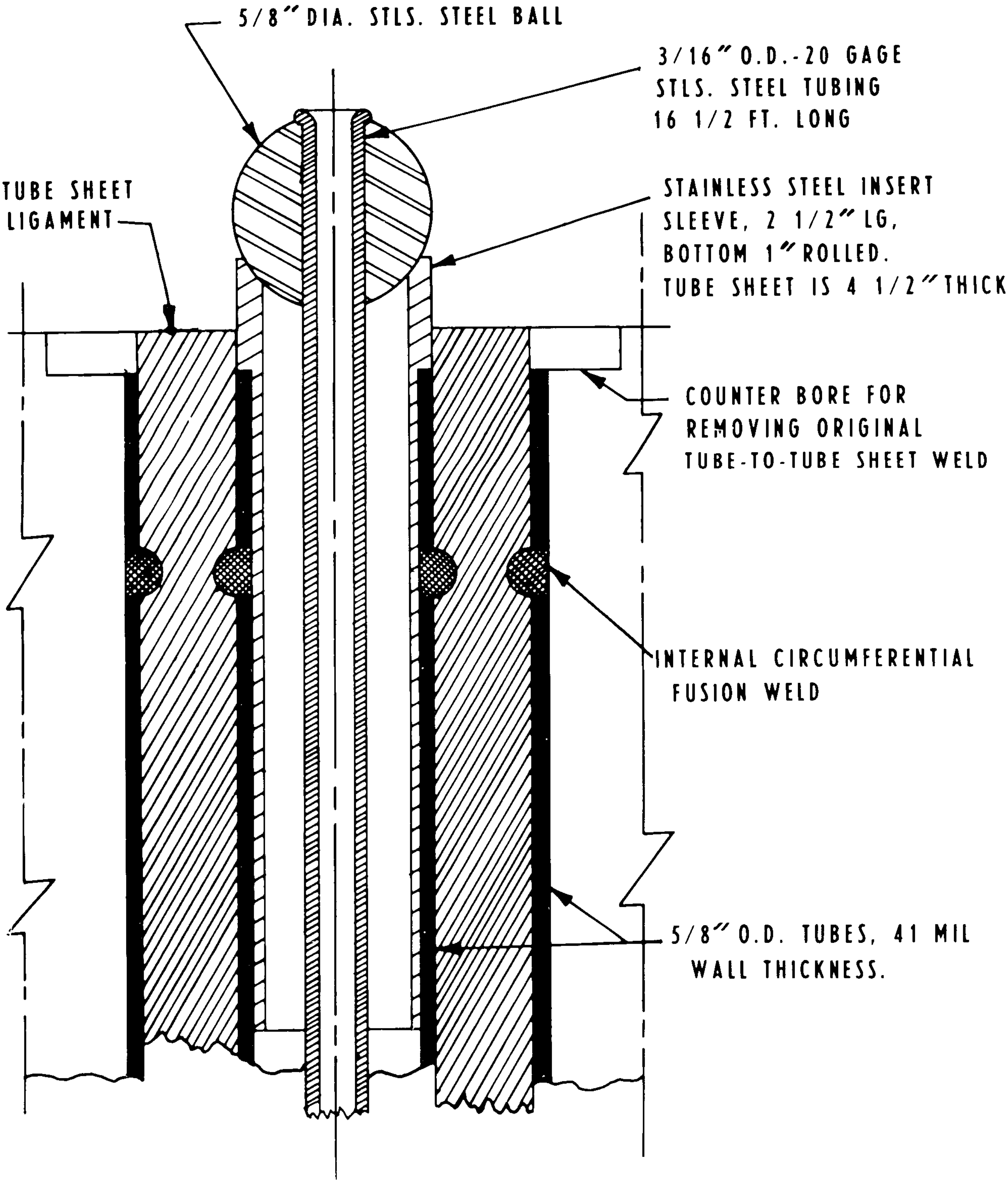

FIG. 3 EFAPP STEAM GENERATOR ORIFICE ASSEMBLIES

BABCOCK & WILCOX STEAM GENERATORS

Design

Under contract to the United States Atomic Energy Commission, the Babcock & Wilcox Company has been carrying on a program to develop a safe, reliable, economical sodium heated steam generator. The program has included preliminary design of a full size steam generator, supporting research and development work, preliminary design of a prototype steam generator, and detail design and fabrication of the prototype. Fabrication of the prototype is in progress and testing will be done at the Sodium Components Test Installation (SCTI) next year. Description of both the full size design and the prototype design are published.[2]

To have high reliability and low cost Babcock & Wilcox has selected large integral superheat units as shown in Figure 4. The once-through type was selected rather than the recirculating type because it has shown better ability to follow load changes and, in large sizes, has lower capital cost. A single tube wall separates the water from the sodium.

Experience has shown that double wall tubes do not absolutely prevent a sodium-water reaction, and by using single wall tubes the steam generator is simpler and easier to inspect; and because of this it will be more reliable in operation. The heating surface is arranged in concentric helical coils. The sodium flows downward over the coils of superheater and boiler tubes. Water flows upward through the tubes, and leaves as superheated steam.

A prototype of the full size steam generator is being manufactured and is to be tested at SCTI. The prototype is not an optimum heat exchanger in itself, but is designed to model significant problem areas in design, manufacture and operation. The diameter, length, and thickness of the tubes of the prototype are the same dimensions as the full size steam generator; however there are proportionately fewer tubes in the prototype than in the full size steam generator. The coil bundles and the riser and downcomer tubes of the prototype are arranged to present the same restrictions to access for assembly and welding as the full size steam generator. Tube sheets and nozzles are areas of concern in this type steam generator, so these areas will be instrumented to measure thermal gradients and strains during steady state and transients. The test operation will be arranged to insure that the prototype undergoes transients at least as severe as the full size steam generator. After completion of testing at SCTI it is planned to do a destructive examination of the prototype. Visual and metallographic examinations will be made of selected areas to confirm that the prototype withstood the testing without damage.

Manufacturing of the Prototype Steam Generator

Fabrication of the prototype steam generator was approximately 75% complete when an overheated spot was found on a superheater riser tube. These tubes are Type 316 stainless steel. They were cold bent to shape and stress relieved. After stress relieving they went through a "check-and-set" operation which meant each tube was compared to a full size layout and if any bends were out of tolerance they were reset by heating them and adjusting them by hand. There was an inadequate specification

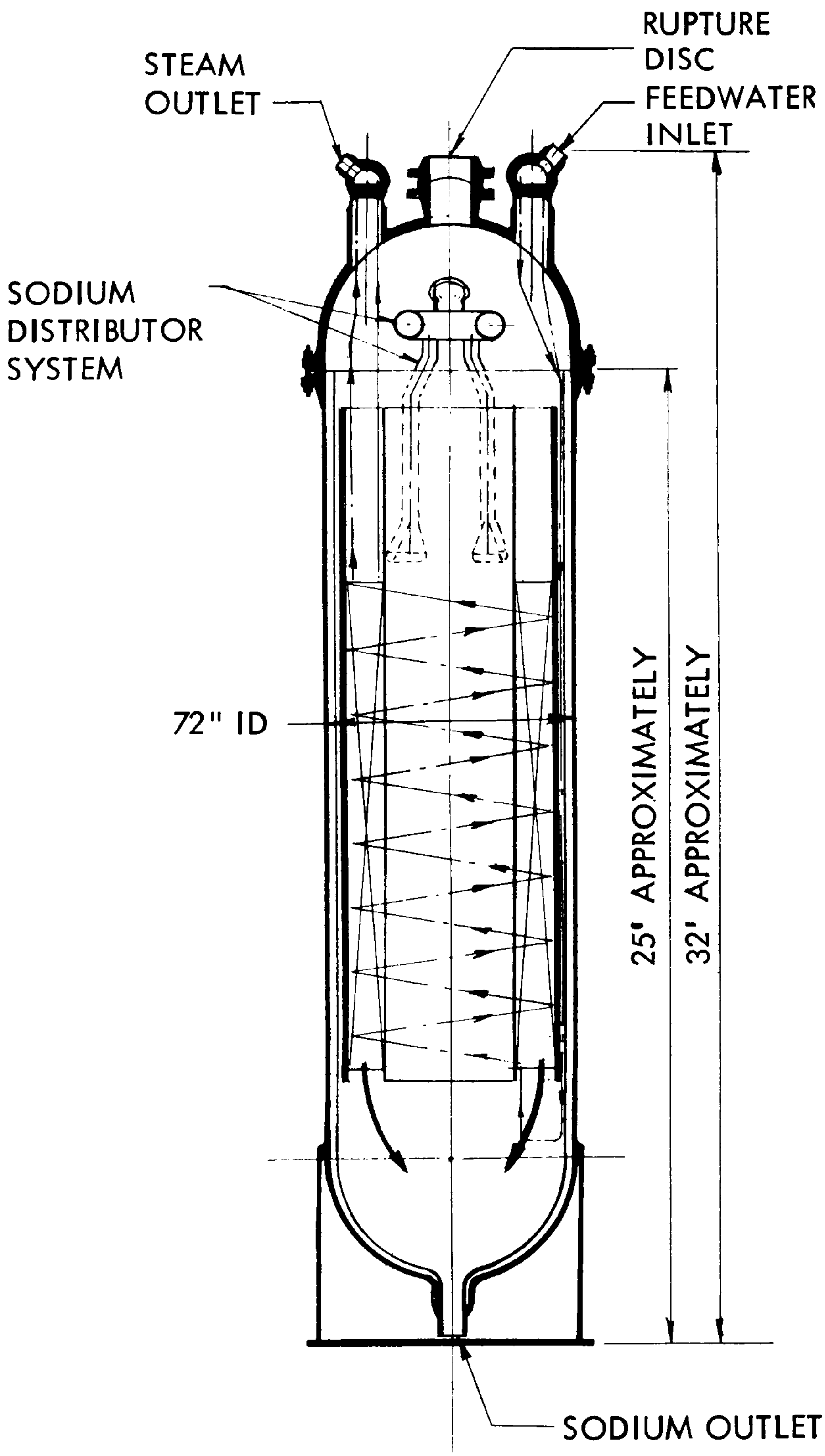

FIG. 4 B&W INTEGRAL SUPERHEAT STEAM GENERATOR

for this operation in that it did not specify limits to the temperature at which the tubes were to be worked, or how the temperature was to be measured. The result was one tube with a burned spot due to improper torch heating. All the coils were disassembled, cleaned again, and dye checked. In addition to the one definite overheated spot three other areas were found that showed some dye-penetrant indications. Although these areas could be cleared by buffing, all the affected tubes were removed and scrapped.

Although all surface indications of overheating had been removed there were still reservations about microfissures or grain boundary damage that did not have surface indications and thereby could not be detected. Even though the steam generator might withstand a year of testing without leaking there was a possibility that unknown conditions within the depth of a tube wall might interfere with interpretation of the results of the destructive examination. Because of these reservations about the exact conditions of the stainless steel tubes, and because there is considerable concern about stress corrosion of stainless steel from momentary upsets in control of feedwater chemistry, a study and recommendation was made to the AEC to re-design the steam generator to eliminate the stainless steel pressure parts.

LMFBR plant studies carried on since the prototype steam generator was designed have indicated that the optimum steam temperature is approximately 900°F to 950°F. B&W has selected 950°F instead of the 1050°F originally specified for the prototype. The sodium inlet temperature is 1025°F instead of 1140°F. The lower tube metal temperatures allow utilizing Croloy 2-1/4 throughout the steam generator. Elimination of the separate stainless steel superheater will allow use of a single Croloy bundle for both boiler and superheater. This will simplify the design, eliminate the need for external interconnecting piping and a dissimilar weld, and reduce the cost. The simpler design will have better access for assembly and inspection so the overall steam generator will have better assurance of reliable and trouble free operation, thereby meeting the overall program objective of demonstrating safe, reliable, economical power from LMFBR systems.

ATOMICS INTERNATIONAL MODULE DESIGN

In connection with its Fast Breeder Reactor program efforts, Atomics International has adopted a steam generator concept employing a number of small modular units in a bank in order to benefit from several intrinsic advantages which such an approach offers over a unitized steam generator. These advantages include: 1) localization of and reduced susceptibility to adverse effects from tube leaks; 2) with hydrogen detectors at the exit of each module, higher sensitivity for leak detection; 3) possible higher availability through ease of locating leaks and replacing defective modules with spares; 4) simplifies achievement of high level of inspectability; 5) facilitates full scale prototype proof tests. The steam generator array is designed to furnish 2400 psig/900°F/900°F steam at the turbine throttle. Simple straight shell and tube configurations were selected for the separate evaporator, superheater, and reheater modules. Figure 5 shows an evaporator module. Sodium will flow through the shell countercurrent to water and steam in the tubes.

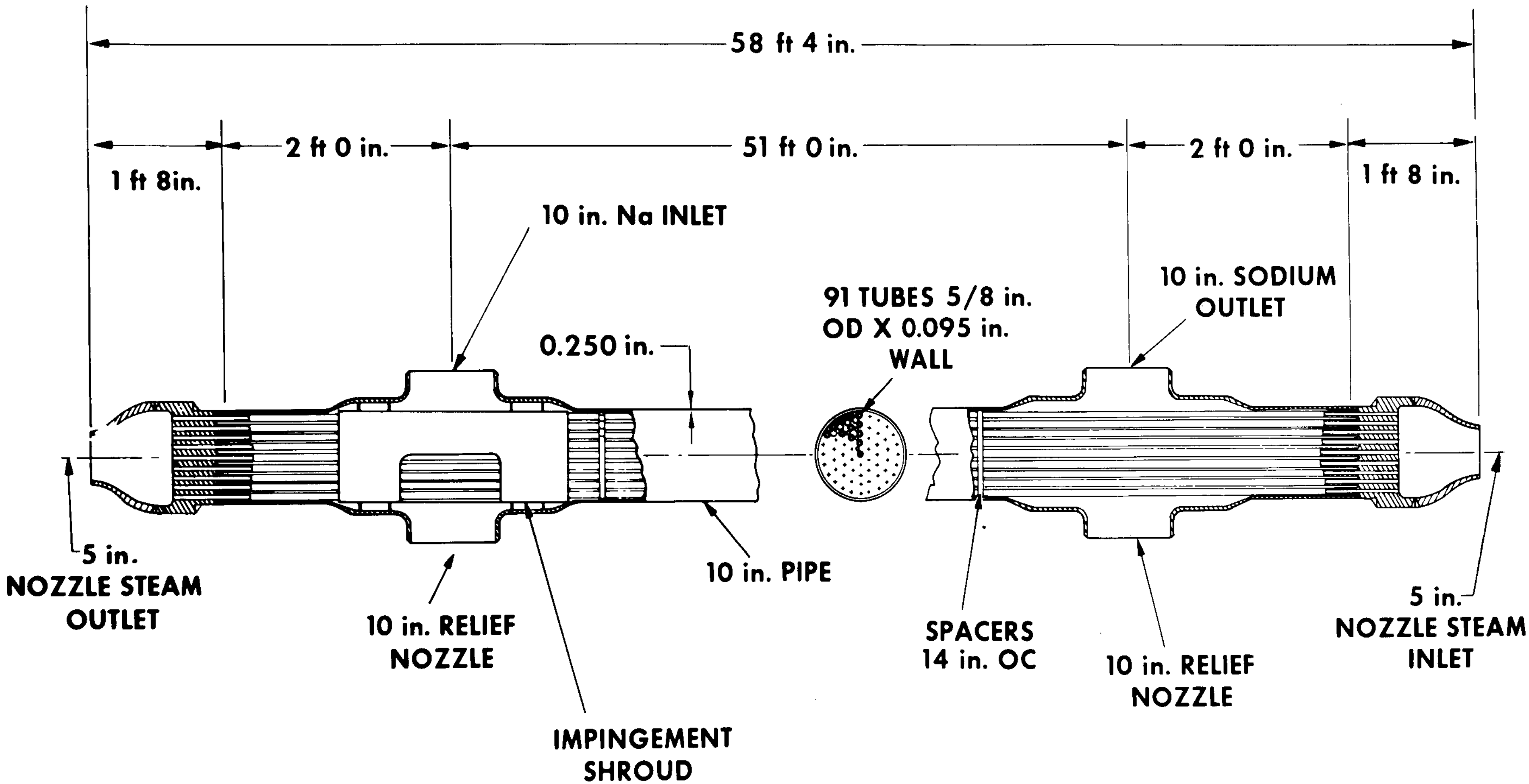

FIG. 5 AI FBR PLANT, STEAM GENERATOR EVAPORATOR MODULE

All modules will be mounted vertically with sodium inlets and steam exits at the top to avoid problems with sodium thermal stratification at low flows. The elevation of the steam generator units relative to the intermediate heat exchanger will permit removal of post-shutdown afterglow heat by natural convection flow without reaching excessive temperatures or temperature differences. Key characteristics of the reference modules in the 500 Mwe fast breeder reactor plant conceptual design are summarized in Table 1.

Module Size Selection

In Table 2 are indicated ways in which various factors influence the selection of module size. The approximate 100 tube limit shown for purposes of detecting tube leaks depends on the use of detectors at the exit of each module and is predicated on two estimates: 1) a "minimum damaging" tube leak rate, and 2) a projection of sensitivity achievable with diffusion type hydrogen detection devices similar to those experimented with at AI[3] and APDA.[4] To a rough first approximation, the "minimum damaging" leak rate is estimated to be $\sim 6 \times 10^{-5}$ lb/sec (for minimum 1/4 inch distance between surfaces of adjacent tubes) based on an analysis of available experimental data. This leak rate corresponds to flow through a 1 mil diameter orifice near the evaporator water inlet. A leak of this magnitude will not cause high pressures in the module. However, if the water flow issues in a well directed, unimpeded jet (as from a pinhole defect), its impingement upon an adjacent tube may cause heating (due to sodium-water reaction) and thinning (probably due mainly to erosion) of the wall--ultimately resulting in a large secondary leak which causes high pressures in the module. It is estimated that for leaks of smaller size than that above the jet strength will be sufficiently diminished before impingement as not to damage nearby tubes. It is possible that smaller leaks may over the long term cause enlargement of the defect and damage of tubes in the vicinity due to corrosion resulting in the development of larger leaks. However, more test data are needed in order to determine whether or not this will be the case as well as to more accurately evaluate the minimum leak rate and other conditions with which the designer must be concerned.

The leak detector referred to above[4] involves 1) diffusion of hydrogen (present in the sodium as the result of a leak) through a nickel membrane, where 2) it is catalytically recombined with oxygen present in an argon carrier gas, and 3) sensed as moisture by a hygrometer. A program of further development and qualification testing of such detectors for plant use is under way at AI under a Company funded program. With a device of this type, indications are that it may be possible to detect leaks of smaller size (down to ~3 ppb hydrogen in sodium equivalent) than the estimated minimum potential damaging level, provided that sodium flow through the module is no more than 10^6 lb/hr. For the selected design, this would be the flow rate through a module having ~ 100 tubes. For leaks of this size and larger (up to $\sim 10^{-2}$ lb/sec) it should be possible to detect and correct the problem prior to there occurring a major sodium-water reaction. Extrapolation of maximum penetration rates measured for well directed jets indicate, that pinhole type leaks approaching 10^{-2} lb/sec can cause failure of adjacent tubes before detection and shutdown can be accomplished. Still larger initial leaks ($\gtrsim 1$ lb/sec) can by themselves cause major sodium-water reactions even without secondary tube failures. It is considered that few of the leaks which may develop are likely to produce a well directed

TABLE 1

AI-FBR STEAM GENERATOR MODULE CHARACTERISTICS*

	Evaporator	Superheater	Reheater
Power (Mwt)	12	7.5	18
Number **	63	30	12
Area per Module (ft^2)	760	652	1705
Number Tubes	91	79	161
Shell Diameter (in.)	10	10	18
Tube (in.)	5/8 x 0.095	5/8 x 0.109	1 x 0.095
Length (ft)	51	50.6	50
Temperature (°F)			
Na in/out	862/700	950/862	950/862
H_2O in/out	472/715	715/905	540/905
Steam Pressures (psia)	2700	2600	540

*500 Mwe FBR

**Estimated requirement to provide 95% probability of achieving 100% of rated performance

TABLE 2

MODULE SIZE SELECTION

Factor	Effect
Leak Detection Sensitivity	~ 100 Tube Limit
Thermal-Mechanical Limits of Simple Configuration without Expansion Compensation	~ 91 Tube Limit with Safe Design Margin
Capital Cost	Very Slight Decrease with Size Increase
Availability	Minimize Size
Ability to Test Prototype	~ 35 Mwt Limit (SCTI)

damaging jet toward an adjacent tube.

The size limit shown for the second item in Table 2 applies to the simple configuration pictured in Figure 5. The lower limit on shell thickness for a given module size is set by the design requirement to safely withstand pressures arising from major tube leaks. The specific design basis leak criteria have not as yet been finalized. The upper limit on shell thickness is set by the need to match the thermal time constants of shell and tubes in such a way as to avoid excess differential temperatures between the two during thermal transients. These two constraints on shell thickness together with considerations of economic tube wall thickness and reasonable tube spacing and sodium velocities set the following specifications for the evaporator and superheater modules: 1) 10 inch diameter, schedule 40 shell, 2) 5/8 inch diameter tubes, 3) 91 and 75 tubes for evaporator and superheater modules respectively. Analyses of the reference modules indicate that, under the worst design transients a small and acceptable amount of inelastic action may occur. Several subassembly tests are planned for the verification of the structural integrity of the design, including studies of both tube and tube sheet response to thermal transients, shell margin for withstanding pressure pulses, and tube-tube sheet weld margin testing. Further confirmation will be provided by the planned United States Atomic Energy Commission supported tests of modules at the Liquid Metals Engineering Center.

In the event that a leak is detected, the loop involved will be shut down and the defective unit replaced. It is estimated that about 8 days will be required for replacement of a defective unit and return of the loop to service. Should high pressures caused by a large rupture in a tube cause blowout of a rupture disc, a signal from a switch indicating disc failure will indicate in which module the accident occurred. In such cases, both sodium and water sides of the steam generator will automatically be valved off, and the water side will be dumped (in less than 30 seconds) and backfilled with inert gas at a pressure above that of the shellside sodium. The sodium will be drained from the steam generator tank after completion of the water dump. Studies indicate that damage should be limited to the module in which the problem originated. Analyses further indicate that with larger modules, lower pressures result in the faulted module from major tube leaks. In Figure 6 are shown the calculated peak pressures that occur following the simultaneous guillotine ruptures of three 5/8-inch tubes in the evaporator at the location marked by the asterisk. The three numbers at various stations in the loop correspond to calculated pressures when the evaporator module diameters are 6-inch (37 tubes), 10-inch (91), and 12-inch (127 tubes). The latter case was included to aid in assessing size effects even though it exceeds the size limit indicated in Table 2.

Design Features and Materials

Undesirable crevices and transitions from thin to thick structural sections are avoided in the tube-tube sheet joints by butt welding the tubes to bosses machined into the tube sheet. This type of joint eliminates the uncertainties associated with crevices such as those involved in conventional rolled and seal welded joints, and it can also be readily X-rayed. It is suspected that if leaks do occur, they are most likely to appear in the weld region. Incoloy 800 sleeves, several inches in length, will be placed concentrically around all welds. As discussed in a later

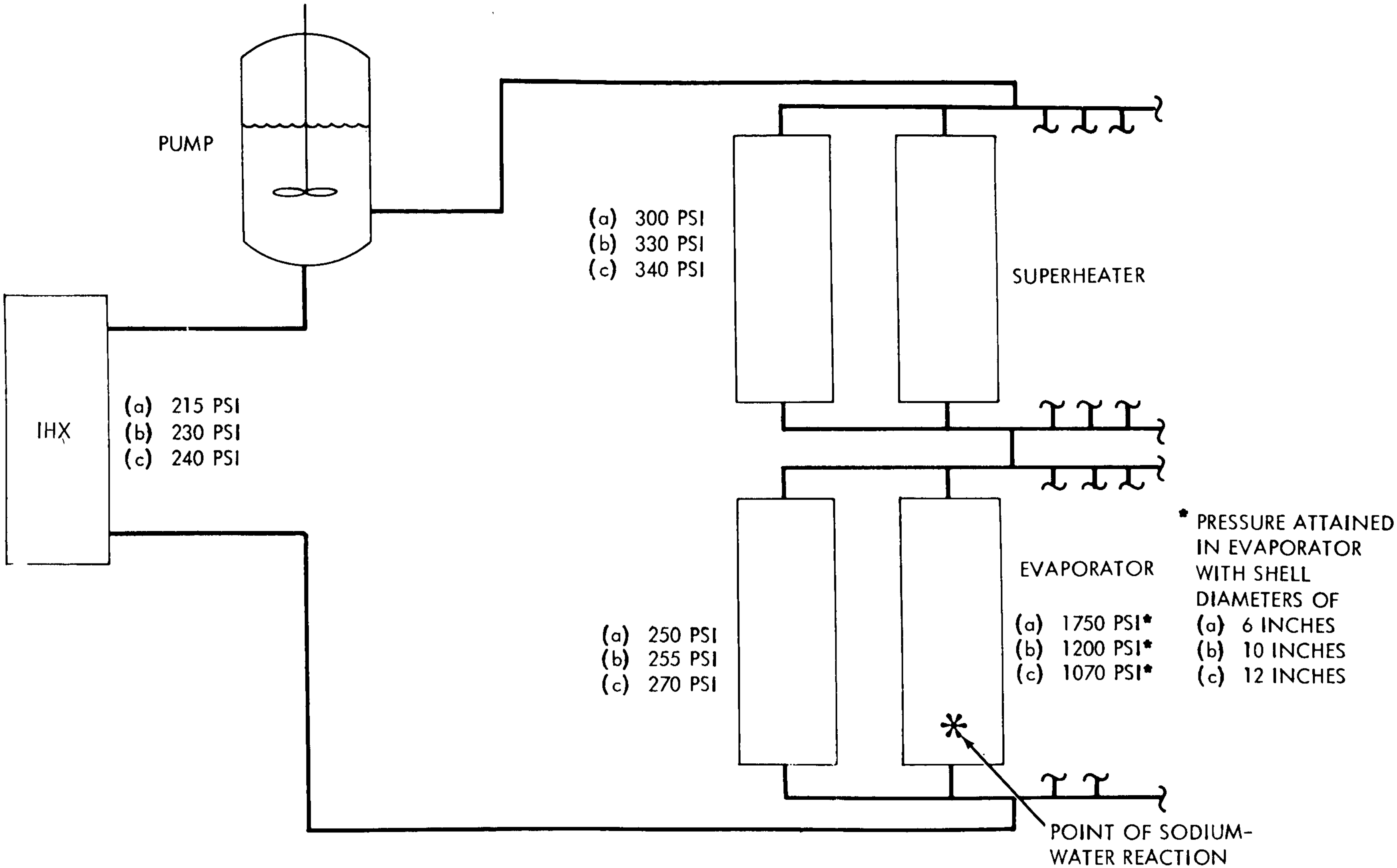

FIG. 6 AI FBR PLANT, SECONDARY SYSTEM PRESSURE ASSOCIATED WITH TUBE LEAKS

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section, Incoloy 800 has proven in tests to be highly resistant to wastage. Protective sleeves of this material will substantially increase the time available for detection and corrective action (before significant wastage takes place) in the event that leaks do occur. Investment castings will be used for tube supports. Calculations indicate that there should be no tube vibration and fretting problem; however, tests will be conducted for verification. Analyses also indicate that no flow instability problem should exist at any operating condition. Nevertheless, the the possible addition of orifices at all evaporator tube inlets in order to provide an added margin of stability is being considered.

Ferritic 2-1/4 Cr - 1 Mo material was selected for all modules in order to avoid the risk (attendant to the use of austenitic stainless steels) of experiencing chloride stress corrosion failures during the 30 year plant life of the units. Incoloy 800 was given serious consideration as a steam generatormaterial, but was decided against because of limited experience with it in steam plant service and in sodium systems. Other means for alleviating the stress corrosion risk were considered (such as recirculating arrangements), but were discarded on economic grounds in favor of the all ferritic steam generator. The 2-1/4 Cr - 1 Mo design allowable stresses were adjusted to 7500 psi at 950°F (less than the code allowables), corresponding to the 250,000 hour design life and accounting for the effects of decarburization due to sodium service. These allowable stresses -- along with wall thickness limits on internal welding of 5/8 tubes -- dictate a limit of approximately 950°F for maximum secondary sodium temperatures. Test results and analyses indicate that the carbon to be transferred over the plant life to the stainless steel intermediate heat exchanger from the ferritic steam generator will cause no structural problems in either the steam generators or intermediate heat exchanger.

A sizeable North American Rockwell-General Public Utilities development and proof test program is now under way to back up the steam generator engineering and design effort. These activities will involve the areas of structural and materials integrity, development of welding and production specifications, inspection and quality assurance, leak detectors, and tube leak effects -- relief and recovery. Under Atomic Energy Commission support, two evaporator and two superheater modules have been fabricated and are awaiting testing in SCTI. These units have been described previously.[5,6]

SODIUM-WATER REACTIONS

The successful development of a steam generator is one of the most critical requirements of the Liquid Metal Fast Breeder Reactor program. The large energy release from sodium-water reactions and the large quantity and chemical nature of the reaction products makes this a most difficult requirement. If a number of LMFBR plants are built, some leaks in steam generators are certain to occur during their operating lifetime -- therefore, the consequences of leaks on the steam generator and on the entire secondary heat transport system must be understood.

Concern over sodium-water reactions in Na and in NaK cooled reactors led to the use of double-wall tube designs in the SRI, EBR-I and EBR-II, SRE and HNPPF. This type of design is less economical than the single-wall design and furthermore it does not absolutely prevent sodium-water reactions. Compared to the double wall design, the single wall design is simple and easier to inspect; so because of this it is expected to be more reliable in operation. All developmental effort on sodium heated steam generators in the United States is now on the single-wall tube design. To date the only single-wall tube designs in the United States on which there is operating experience are the 150 Mwt Enrico Fermi Plant units, the LAMPRE units at LASL and the 30 Mwt Alco-Baldwin-Lima-Hamilton prototype which has been tested for 730 hours in the SCTI at various power levels up to 9 Mwt; other single-wall designs are two 7.4 Mw evaporator units and two 3.4 Mw superheater units built by Atomics International and the 26 Mwt Babcock & Wilcox prototype which is under construction. These units are scheduled for testing in the SCTI.

The major concern to date has been over large leaks, that is single and multiple tube ruptures, and their effects on structural integrity. Tests involving single tube ruptures have been conducted for the Fermi units and for the AI modular units; and these tests demonstrated that the energy release was accommodated without damage to the steam generator shell. Mathematical analysis of the Fermi units and of the B&W steam generator design for a 1000 megawatt-electric plant show that multitube ruptures can be accommodated safely.

Recently, attention has been given to leaks that are less-than-single tube ruptures, for example leaks that would result from weld and tubing material defects. Experience with the Fermi units indicated that small leaks could cause high metal loss rates on adjacent tubes.[1] A program to measure tube damage rates caused by small leaks is in progress at Atomic Power Development Associates, Inc. under AEC sponsorship.

ESADA Sponsored Sodium-Water Reaction Studies

In 1965-1966, sodium water reaction tests were conducted by Atomics International for Empire State Atomic Development Associates, Inc., at the Field Test Laboratories in the Santa Susana mountains.[7] The primary objectives of this effort were to study the transient phenomena and determine the damage attendant to major ruptures of steam generator tubes. Initial sodium and water-steam temperatures and pressures during the tests were those appropriate for steam generators supplying 2400 psi/1000°F steam.

Test System - Shown in Figure 7 is a diagram of the system in which the tests were conducted, and Figure 8 is a photograph taken prior to the beginning of testing. The system consisted of a test section in which the reaction took place, a relief system and receiver tank for the products expelled from the test section by reaction, a high-pressure steam supply system, a high-pressure water supply system, and a sodium storage and supply system. A static sodium system was used since it was considered that the test results should be the same regardless of whether or not the sodium was flowing initially. The steam supply system was designed to provide 2400 psi steam at 1000°F, and the water supply system was designed to provide water at 2800 psi and 470°F. Each system was capable of providing flow for at least 40 seconds.

The shell of the main test section was made of 8-inch diameter, Schedule 80, Type 304 stainless steel pipe, and its overall length was 16 feet. Test section lengths of up to 46 feet were obtained

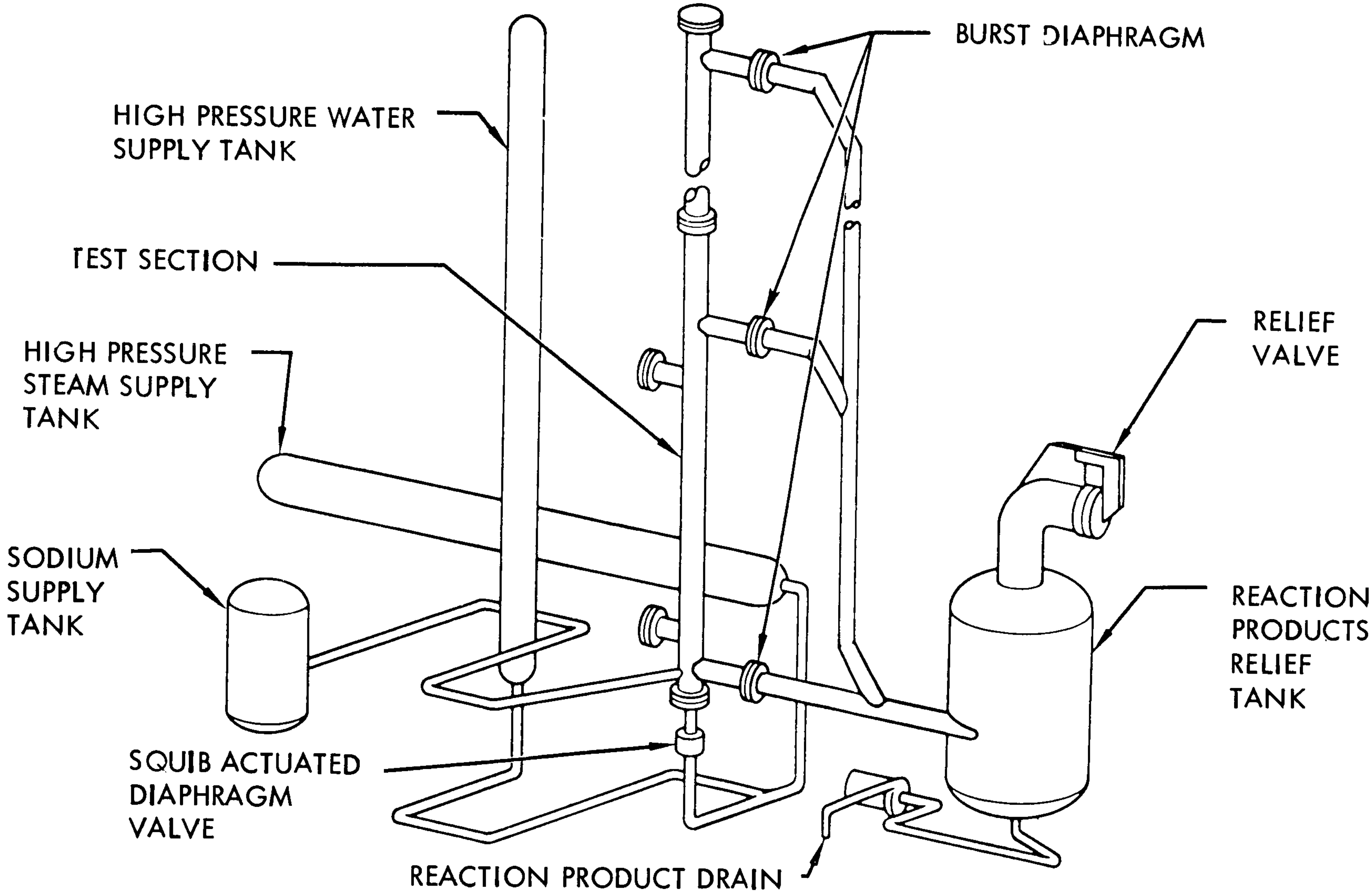

FIG. 7 ESADA SODIUM-WATER REACTION TEST FACILITY SCHEMATIC

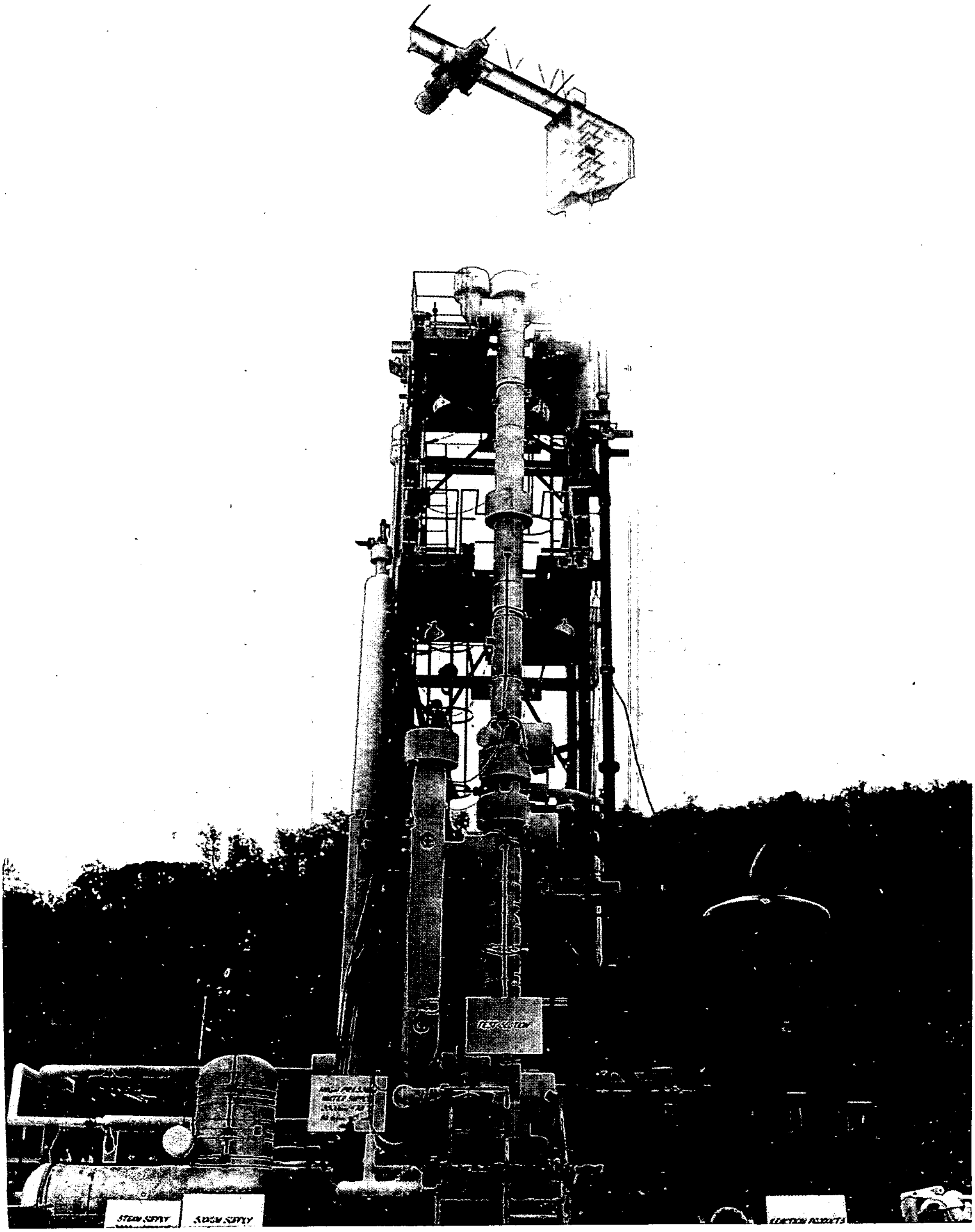

FIG. 8 ESADA SODIUM-WATER REACTION TEST FACILITY

by adding pipe lengths to the main test section. The tube bundle mock-up within the test section consisted of a central rupturable tube surrounded by three rows of 5/8 inch stainless steel, gas filled tubes (total of 36 tubes) and sealed at both ends. In one of the runs (designated 3J) the rupturable tube was manifolded with the six immediately surrounding tubes and all were filled with pressurized water during the test. The tubes were positioned on a pitch of approximately 1 inch by investment casting spacers. In 15 of the 17 tests the mock-up tube bundle was of 4 foot length, but in the remaining two tests a 14 foot bundle was employed. The main test section was equipped with four lateral nozzles. Two of these were closed with blind flanges, except during one series of runs when they were used in linking a simulated parallel module to the main test section. The other two flanges were fitted with 6-inch diameter, 125 psi rupture discs and were connected to the relief system. The 6-inch lines connecting the rupture disc to the reaction products relief tank were of carbon steel. The reaction products tank was also made of carbon steel, and it was rated for 150 psi at 850°F. The 16-inch relief valve on top of the tank was of sufficient size to pass up to 100 ft^3/sec vapor at 5 psi. During each test except the last, low-pressure steam flowed into the tank to complete the chemical reaction of sodium received from the test section. This steam was supplied at 90 psi by a gas-fired utility boiler. Following a test, the relief system, test section, and reaction products tank were flushed with water and drained. The relief valve cover was designed to pivot upward to open, and the adjustable weights on the valve cover were such that 2 to 3 psi could be maintained in the tank.

Two different schemes were employed for the rupturable tube. In the first, which was used for 12 of the 17 tests, a 4-inch long flat region was machined on the tube OD -- reducing the wall thickness to ~0.006-inch. Toward the end of the experimental program, "dead end" type ruptures were produced through the use of 0.0035-inch thick Inconel rupture discs secured over the end of the rupture tube with a high-pressure tube fitting. Tube ruptures were caused by pressurizing the central tube with either water or steam. Initially, the water or steam was prevented from entering the weakened central tube by means of a blocking disc located in the supply line at a distance of approximately 25 feet from the failure site. To initiate the failure sequence, the disc was pierced and expanded in a period of 5 msec by a spring-driven, pointed valve stem, thus allowing high pressure fluid to enter the tube.

At the test system console, manual controls, interlocks, and indicators were provided for remote operation of the system solenoid and motor-operated valves. Additional console indicators were provided for monitoring explosive bolt arming status, equipment power, and wind conditions. Also available at the console were the process temperature- and pressure-indicating recorders, a closed circuit television system, and the facility communications system. The sodium-water test itself was initiated by the test event sequencer. Test procedures were as outlined in the flow diagram of Figure 9.

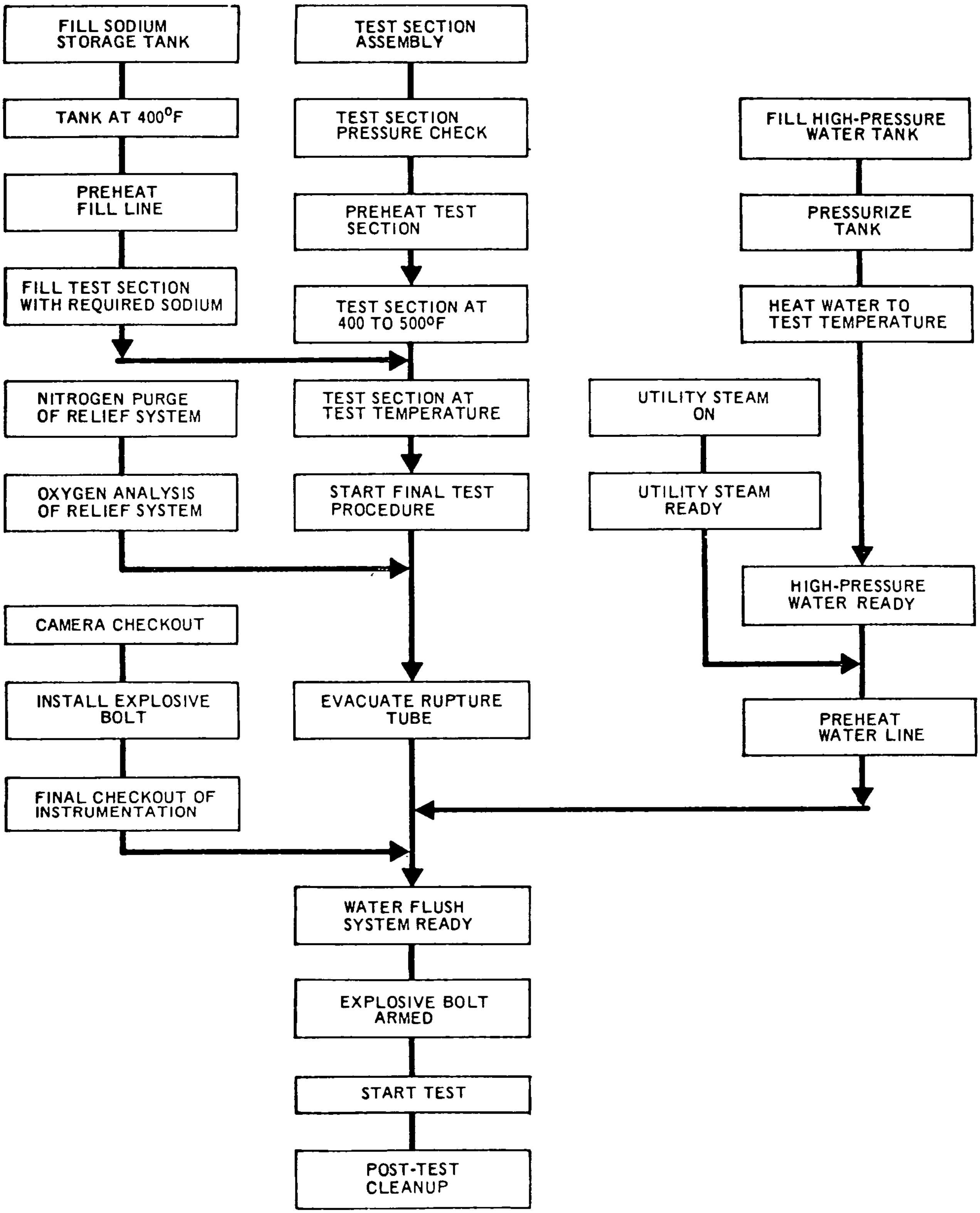

FIG. 9 ESADA SODIUM-WATER REACTION TESTS - TEST PROCEDURE FLOW DIAGRAM

During the tests, temperatures, pressures, shield strains, water and steam flow rates, and flow rates of the mixture through the relief system piping were measured. The types of pressure sensors used were: 1) quartz crystal transducers (frequency response ~150 kcps); 2) unbonded strain wire transducers; and 3) diaphragm-actuated differential transformers -- which proved to be inadequate for the purposes of this test. Calibrations of the pressure measurement system were made before and after each test. Because of temperature limitations of the transducers, stand off mounting arrangements were used in many cases to place the instruments in a cooler environment. Two types of strain gages were used: 1) flame-spray bonded (which proved to be unsatisfactory above 700°F and 2) spot welded. Temperatures were monitored with chromel-alumel thermocouples. Sodium ejection rates were determined by the use of shorting switches installed in series in the discharge lines. Gross measurements of water flow rates were obtained during the tests using a turbine type volumetric flow meter located in the high pressure water tank; however, it was not possible to determine true transient flows through the tube opening with this arrangement. The signals from the above sensors were transmitted to the control room for conditioning, amplifying, and recording on high-speed (~6 msec/in.) oscillographic recorders.

Tests were conducted with the system configurations pictured in Figure 10, which also indicates the locations of pressure transducers on the main test assembly. Test Series A and B served to verify the structural integrity of the test section and to check the system performance, the test procedures, and the instrumentation. In addition, they provided data for a baseline case of system inertia and capacitance values. The second configuration was used for Test Series C and D. This assembly was developed by the addition of two pipe spools of 6-inch Schedule 80 pipe to the main test section, thus increasing the total test section length from 16 to 46 feet. Series C represented a tube rupture occurring near the lower end of a superheater module, where sodium at 915°F might react with water at 470°F. Series D conditions were for the case where tube rupture would occur near the lower end of an evaporator module where the sodium temperature is ~675°F. The configuration for Series E placed the main test section in the center of the 46-foot assembly, providing a study of the effects of having columns of sodium both above and below the rupture point. In this configuration, the rupture discs were equidistant from the tube rupture point. The other configuration in Figure 10 was used for tests in Series J. The test assembly was identical to that used for Series C and D, except that a parallel section of 6-inch pipe, 16 feet long, was adjoined to the main test section. This series was intended to simulate to some extent the effects of a closely coupled parallel steam generator module. The parallel section was connected to the two lateral connections that for previous tests had been closed with blind flanges. Provided in each case, including the parallel shell in Series J, was a cover-gas space equivalent to 8 to 10% of the total sodium volume in the assembly. The cover gas was nitrogen at 50 psi.

<u>Test Results</u> - Indicated in Table 3 are key data from the experiments. This test effort, and in particular the early runs, were in a sense

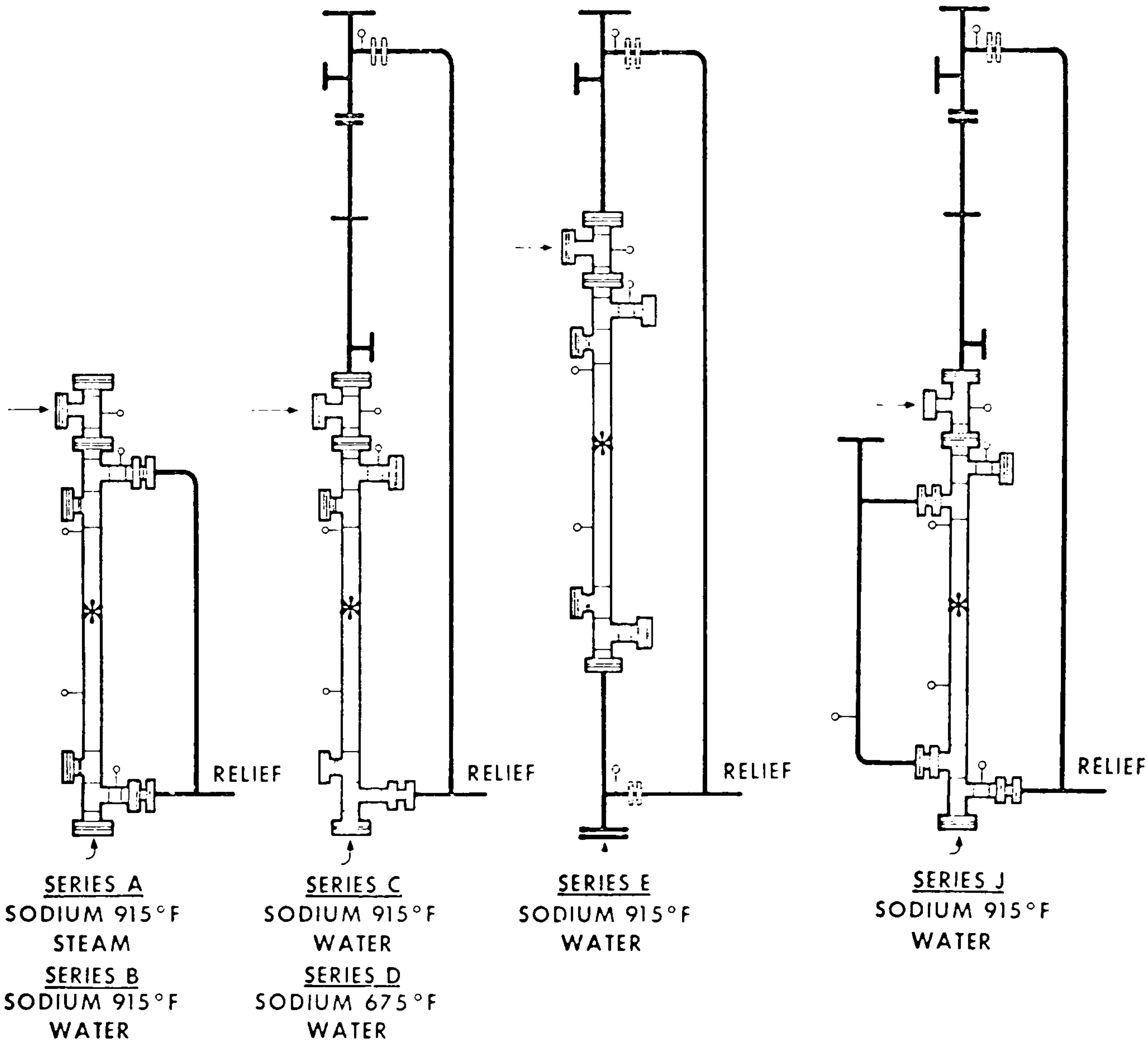

FIG. 10 ESADA SODIUM WATER REACTION TESTS - TEST SECTION CONFIGURATIONS

TABLE 3

ESADA SODIUM-WATER REACTION TESTS - SUMMARY OF TEST DATA

TEST	INITIAL SODIUM TEMP., °F	INITIAL WATER TEMP., °F	INITIAL WATER PRESSURE PSIG	• TIME TO INITIAL PRESSURE PEAK, MSEC	• TIME TO FAILURE OF LOWER DISC, MSEC	• TIME TO FAILURE OF UPPER DISC, MSEC	MAXIMUM TUBE LEAK RATE, LB/SEC	INITIAL PEAK PRESSURE AT RUPTURE SITE, PSIG	SECONDARY PEAK PRESSURE AT RUPTURE SITE, PSIG	PEAK PRESSURE 5 FT ABOVE RUPTURE, PSIG	PEAK PRESSURE 5 FT BELOW RUPTURE, PSIG	PEAK PRESSURE-TOP OF PARALLEL SHELL, PSIG	PEAK PRESSURE-BOTTOM OF SHELL, PSIG	MAXIMUM SHELL CIRCUMFERENTIAL STRAIN, IN/IN.
1A	757	970	2060	-	-	-	-	-	-	-	-	-	-	-
2A	917	1038	2180	-	-	-	-	> 800	-	400	365	-	-	-
1B	920	480	2750	-	2.0	~1	-	>1500	-	380	580	-	-	-
2B	900	455	2560	-	-	-	-	-	-	347	335	-	-	-
3B	918	465	2740	-	1.5	6.3	11.1	-	-	160	481	-	-	~80
1C	922	469	2760	0.13	2.5	154	12.6	604	~450 (N)	300	410	-	-	~95
2C	919	472	2770	0.33	2.5	151.8	11.6	484	~250 (N)	276	335	-	-	~85
3C	922	467	2690	0.33	2.9	175.7	12.2	915	~180	485	-	-	-	~95
4C	920	470	2780	0.25	1.7	169.9	12.2	745	~300 (N)	475	455	-	-	90
1D	676	471	2760	0.25	1.8	105.1	13.2	1850	380 (N)	1136	770	-	-	213
2D	674	466	2770	0.25	1.4	108.7	12.8	2780	~300 (N)	1030	654	-	-	117
3D+	691	426	2650	0.5	1.5	109.8	-	2110	~700	-	318	-	-	156
4D+	681	400	2650	0.3	1.5	108.2	19.2	1290	530	970	400	-	-	121
1E+	680	478	2770	-	4.1	134.3	14.6	-	-	1100	770	-	-	114
1J+	911	468	2760	0.2	1.5	105.1	17.3	1060	420	526	437	548	703	170
2J+	886	468	2760	0.3	1.5	110.7	17.3	1140	720	1080	986	340	958	235
3J**	933	476	2740	0.25	1.6	104.8	-	905	1640	1890	880	1870	1510	328

• TIME AFTER TUBE RUPTURE.
** 7 CENTRAL TUBES CONTAIN PRESSURIZED WATER.
(N) NO SECONDARY PEAK - PRESSURE SHOWN IS AVERAGE FOR ~ 10 MSEC FOLLOWING INITIAL PULSE.
+ "DEAD END" RUPTURE TUBE USED.

exploratory since there was little previous data upon which to base expectations of the test results. As a consequence, some of the instrumentation in early runs proved to be unsatisfactory and gaps in the test information resulted. Cross comparisons of data indicate that the values for peak pressures at the rupture site and for time to rupture disc failure should be fairly accurate.

In general, all runs for a particular test series (e.g., 1C through 4C) were intended to be duplicates; however, it may be noted that significant variations in peak pressures occurred from test to test. As the program progressed, it was deduced that these deviations were attributable primarily to variations in the pressures at which the milled tubes failed. To examine this point, three-milled flat rupture sections were hydrostatically tested at room temperature. One ruptured at 1940 psi, one at 2400 psi, and one at 3200 psi. The variations in failure pressure corresponded to variations in thickness due to machining tolerances. To improve pressure reproducibility, the "dead end" type Inconel rupture disc device was adopted. Ten room-temperature tests were made of this device, and the resulting rupture pressures ranged from 1675 to 1780 psi. This design was used in all but one of the tests subsequent to test 2D.

Evaluations of the data during and immediately following the tests -- and also currently with improved analytical models -- indicate that events following the failure occurred generally as discussed below. Upon rupture of the tube, a bubble is formed which consists of a mixture of moist steam and hydrogen evolving from the reaction of sodium and water. Immediately a pressure wave originates at the failure site. About 0.3 msec later, the wave attenuated by reflection and refraction from intermediate tubes, is detected by the pressure sensor adjacent to the rupture site. The recorded traces show the initial pulse rising to a maximum in another 0.1 to 0.5 msec, followed by a rapid decay. The initial pulse travels through the assembly to the relief system rupture disc, arriving there approximately 1.5 msec after the rupture. In all tests the energy of the initial pressure pulse was insufficient to cause rupture disc failure. Flashing water continues to flow through the tube breach and a secondary rise in bubble pressure develops due to the inertia of the sodium. The lower relief disc blows approximately 2 msec after the tube rupture, tending to relieve pressure in the bubble while the in-flowing water mixture tends to cause pressures to rise. The bubble pressure causes acceleration of the column of sodium in the shell (with a coincident decline in pressure) to velocities up to 150 ft/sec. Most of the sodium in the shell below the rupture point vents to the reaction products relief tank after a period of ~150 msec. The sodium column above the "bubble" behaves like a piston and compresses the inert gas cover gas until the upper relief rupture disc fails. That portion of the sodium which is not vented through the upper rupture disc falls back and mixes with the "bubble" region. If intimate mixing of water and/or steam and sodium occurs, the resulting reaction causes delayed pressure pulses, seldom of magnitude comparable to the initial pulse. Less energetic reactions also occurred when the sodium clinging to the interior surfaces of the vessel reacted with the expanding steam-water mixture. These reactions cause temperatures of the order of 2000°F about 1 sec after the initial tube rupture. On occasion, rapid reactions occurred in the reaction products relief tank, thus retarding the continuing outflow of reactants and reaction products from the shell. During the tests water flowed into the shell for a period of about 40 seconds. Low-pressure steam continued to flow into the reaction products tank for five to ten minutes, after which the reaction products tank became quiescent and recovery operations began.

Efforts to develop a full understanding of results obtained in the ESADA tests are continuing. In Figure 11 is shown a representative plot of pressures measured by a transducer on the shell opposite the rupture site. Also shown for comparison are analytical results obtained for the same initial conditions employing one dimensional model. The high frequency ringing was found to be caused by the influence of shell structural dynamics.

The temperature data were obtained with thermocouples having comparatively slow responses ($\approx$50 msec) and indicate only gross effects. There was considerable fluctuation of temperatures with time, probably due in part to the breakup characteristics of the sodium column after the upper rupture disc was blown. The peak measured temperatures within the shell ranged from 1800 to 2100°F; however, the shell temperatures remained within approximately 100°F of their initial values. Pressure measurements in the relief system lines during some of the tests show peak pressures up to 425 psi for short durations. These pressures were also recorded at the top of the test section in some of the tests. The maximum temperature measured in the relief line was 2120°F (Test 1C). Surface temperatures at the bottom of the reaction products tank exceeded 1200°F (maximum case ~1350°F) for about 10 minutes during the C Series tests. In Series D, J, and E, the tank temperatures were less than 1200°F.

The material vented from the reaction products tank consisted of steam, sodium, sodium oxide, sodium hydroxide, and hydrogen. The discharge continued as long as sodium and incoming steam or water were available, sometimes for a 15 minute period. In Test 1E, where utility steam was withheld from the reaction products tank, the discharge ceased abruptly when the high-pressure water flow stopped. The discharge restarted when the steam was turned on, about 1 minute later.

The 17 tests were completed with very little damage to the overall test facility. In several of the tests the tubes surrounding the failure site were bowed slightly outward toward the shell. No metallurgical examination was made of tubes in the vicinity of the failure; however, there was no visual evidence of tube wall wastage. The only damage observed other than the tube bowing involved the austenitic stainless steel nozzle leading to the upper rupture disc. Examination showed the nozzle to have branching transgranular cracks, typical of stress corrosion cracking. The chlorinated city water used for flushing together with the non-stress-relieved welds probably provided the conditions necessary to produce the observed cracks.

In summary, it was found that following a major tube rupture there is 1) an initial high pressure pulse of insufficient strength to cause blowout of the rupture discs, after which 2) more moderate

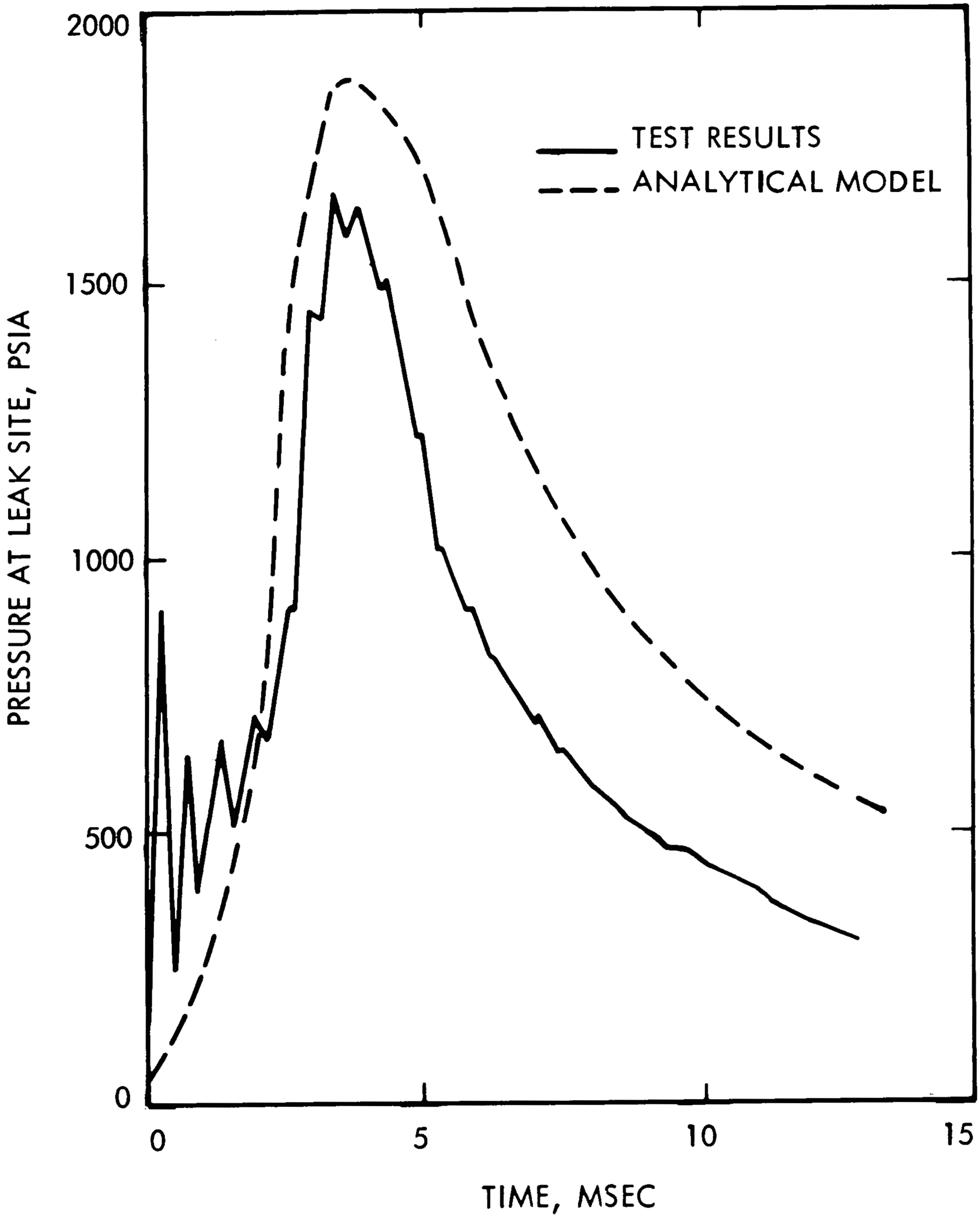

FIG. 11 ESADA SODIUM WATER REACTION TESTS - TRANSIENT PRESSURES

pressures (generally less than one to two thirds of the initial peak) persist for up to 100 msecs. The initial peak pressures in the tests were equal to or less than the water side pressure. Strains in the test section shell near the point of tube rupture were very low, and tubes surrounding the rupture site experienced negligible wastage and minor bowing. It was concluded from the test results that it should be possible for modular steam generators to safely contain the pressures resulting from major tube leaks, and damage should be largely restricted to the defective module. Further, it should be possible to safely handle the reaction products resulting from such an accident.

APDA Small Leak Studies

Tube Wastage Tests - In December 1962 a large sodium water reaction was experienced in one of the Enrico Fermi Plant steam generator units as a result of tube failure caused by vibration. Subsequent examination revealed that four tubes in the vicinity of the sodium-water reaction failed by pressure rupture due to general thinning of the tube wall. This observation was surprising because metal wastage had not been experienced in large sodium-water reaction tests. It was concluded that the tube wastage was caused sometime during the 45 minutes long period when the leak rate was small, i.e. before the large leak occurred. This experience led to investigations into the cause of the tube wastage and the potential consequences of small leaks in the Fermi units. Tests conducted by Detroit Edison Company with leakage rates in the range of 0.03 to 0.5 lb/sec showed wastage rates ranging from 0.2 mils/sec to 2.0 mils/sec over a localized area of about 1/8 inch diameter. In 1963 a program to study tube wastage rates caused by small leaks was begun at APDA under AEC sponsorship. A detailed discussion of the early planning and of the preliminary test results have been published.[8] This paper presents the results obtained since about January 1967.

A special test apparatus designated as Rig 10 and shown in Figure 12 was constructed to carry out the test program. Prior to starting a test, a measured amount of water is added to the injection water storage tank, and the circulating water system is filled. Both systems are pressurized to 2650 psig with nitrogen gas. The loop system is brought up to temperature of 400°F and is filled with approximately 7000 pounds of sodium. With the sodium circulating, the system temperature is gradually increased to the test temperature of 625°F. During this period the sodium is cold trapped until the plugging temperature is below 400°F. The injection water and the circulating water are heated by the sodium to 625°F, and the circulating water flows through the tube bundle by natural circulation.

A cross sectional view of the tube bundle shown in Figure 13 shows the relative position of the water injection nozzle to the target tube. The injection nozzle, which simulates a leaking tube, can be positioned between 1/4-inch to 1-1/2 inches from the target tube. The leak is initiated by breaking off the tip of the carburized capillary tube from the injection nozzle by an air actuated guillotine. Thermocouples swaged down to 0.040 inch in diameter at the tips are located throughout the tube bundle with a

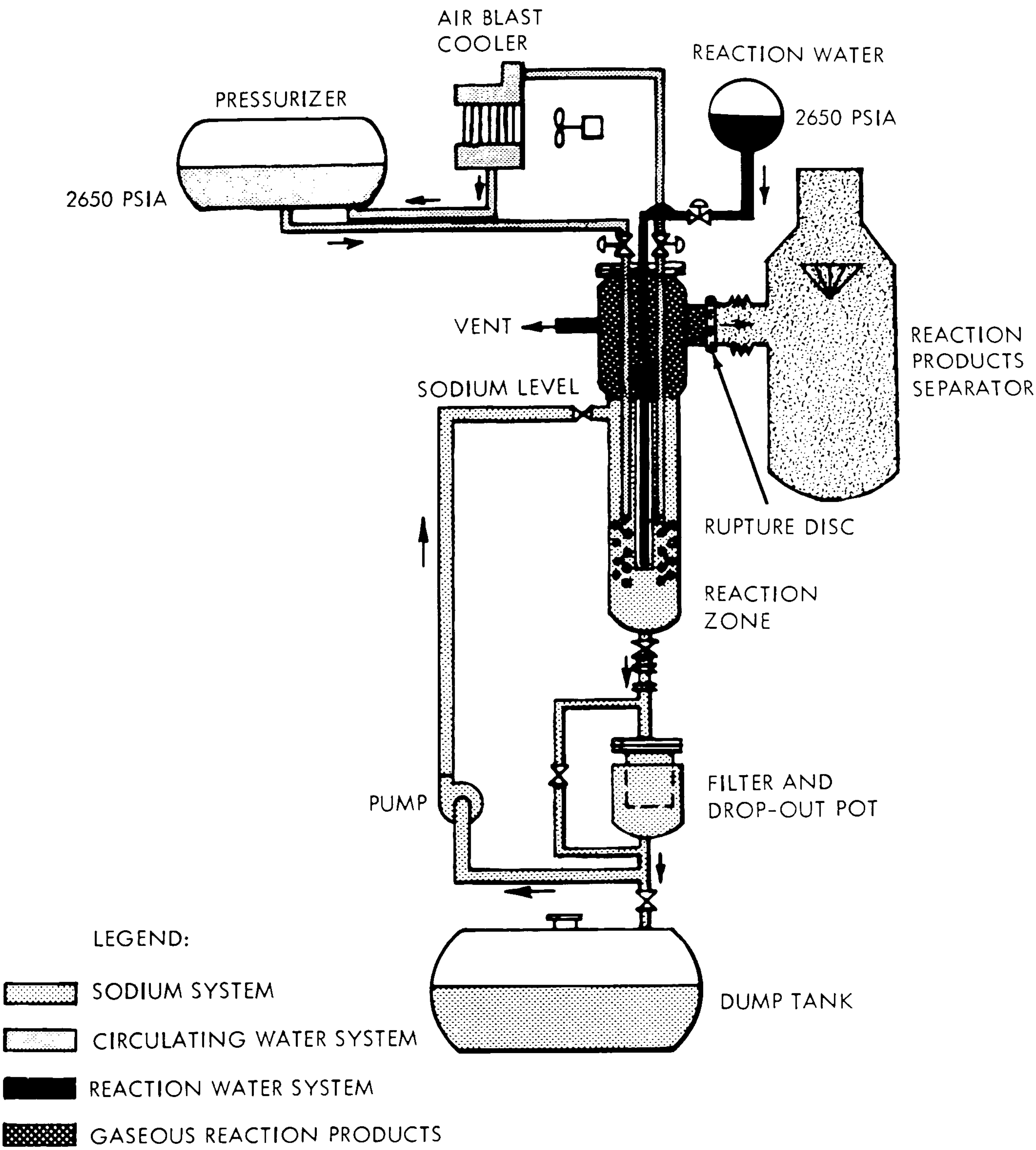

FIG. 12 APDA SMALL LEAK STUDIES - TEST RIG 10

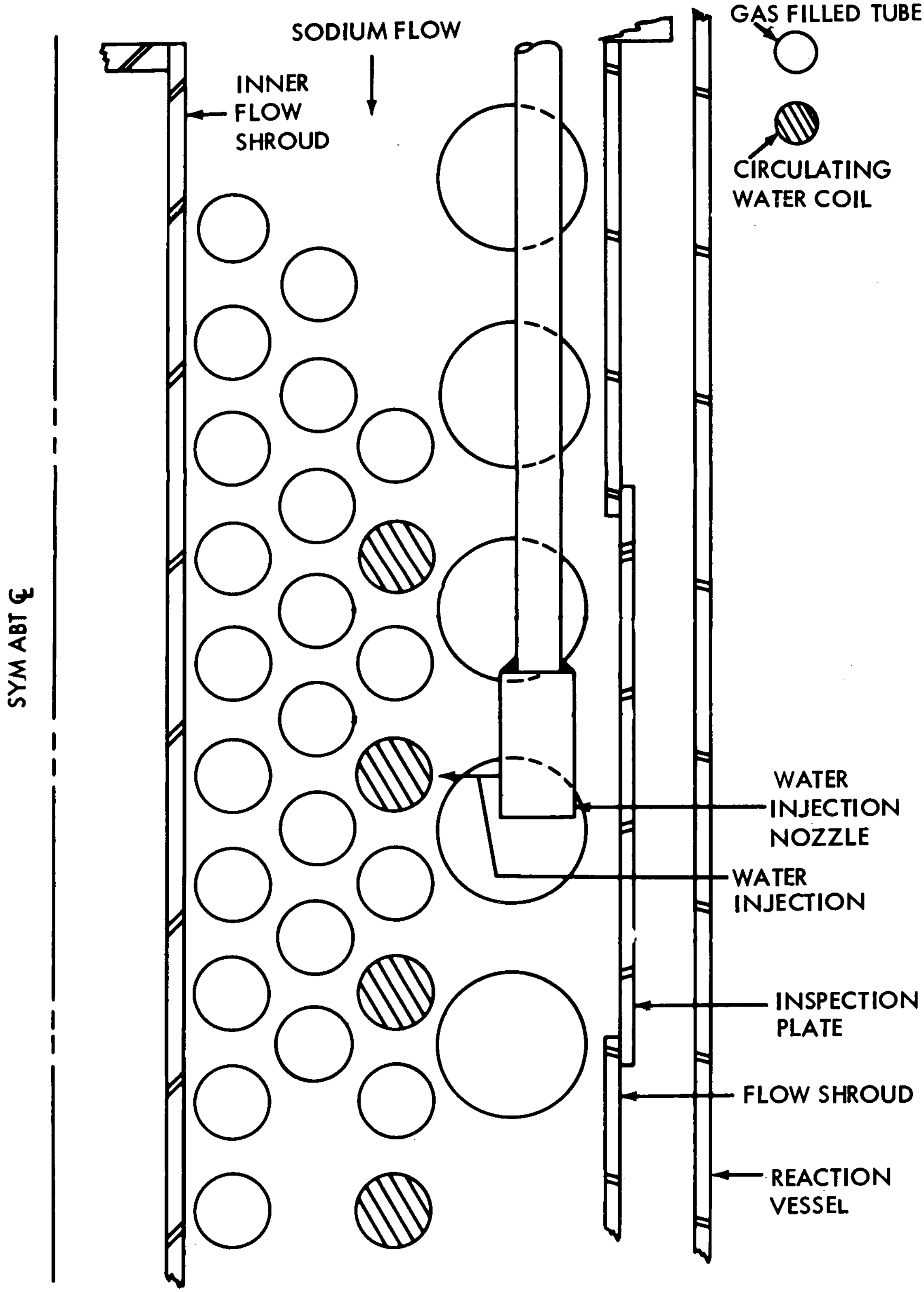

FIG. 13 APDA SMALL LEAK STUDIES -
ELEVATION CROSS SECTION OF TUBE BUNDLE ASSEMBLY

circular array of six thermocouples mounted on the target tube in the target area. In addition to measuring test temperatures the thermocouples are connected to an optical oscillograph used to indicate when the test is initiated and completed.

The initial phase of the small leak test program consisted of a series of statistically designed tests to assess the significance on tube wastage rate of water leak rate, duration of leak, tube spacing, and sodium velocity in the vicinity of the leak. Other pertinent variables were identified which were considered to be less important, namely: water temperature, pressure, and flowrate; sodium temperature and sodium head; and tube material. A half replicate statistical program consisting of eight tests was designed based on two levels of test for the four variables. An additional four tests were planned to establish the range of variables to be used in the half replicate test program.

The general test conditions selected for the statistical program were as follows:

1.	Sodium temperature, °F	625
2.	Injection water temperature, °F	625
3.	Injection water pressure, psig	2650
4.	Recirculating water temperature, °F	625
5.	Recirculating water pressure, psig	2650
6.	Recirculating water flowrate, gpm	1
7.	Tube material	2-1/4 Cr-1 Mo
8.	Tube dimensions	1 inch OD x 0.120 inch wall

The actual test conditions and the metal wastage results for this test and the initial series of twelve planned tests are summarized in Table 4. In the first test the water injection nozzle inadvertently was 180°F out of position so the leak impinged on the carbon steel flow shroud instead of on the target tube. In Test No. 2 after 322 seconds the target tube failed from pressure rupture caused by excessive thinning, which permitted leakage of water from the water circulating system into the sodium. This secondary leak, which was at a much higher rate than the primary leak, impinged on the injection nozzle and on the flow shroud causing severe metal wastage. A detailed discussion of the results of Test Nos. 1 and 2 has been published.[8]

Test Nos. 3, 4, and 5 were scoping tests to establish the range of variables for the half-replicate statistical program; based on the results of these tests the following levels were selected for the remainder of the program.

	Variables	Levels	
1.	Injection rate, lb/sec	0.0012	0.0022
2.	Injection duration, sec	100	250
3.	Spacing, inch	1/4	1
4.	Sodium velocity, ft/sec	1	2

TABLE 4

APDA SMALL LEAK STUDIES - TEST NOS. 1-13

	TEST NO. 1	2	3	4	5	6	7	8	9	10	11	12	13
Sodium System													
Flowrate, gpm	400	400	420	395	400	200	400	400	200	200	400	400	200
Velocity past target tube, ft/sec	~ 2	~2	~2	~2	~2	~1	~2	~2	~1	~1	~2	~ 2	~ 1
Bulk Temperature, F	632	618	611	615	625	608	605	605	610	603	613	628	605
Injection Water System													
Water added, lbs	.88	.88	.24	.60	.48	.24	.24	.48	.24	.30	.11	0.24	0.14
Temperature, F	632	618	611	615	625	608	605	605	610	603	619	628	605
Pressure, psig	2650	2650	2650	2650	2650	2650	2650	2650	2650	2650	2650	2650	2650
Orifice size, in. (nominal)	.008	.008	.006	.006	.006	.006	.006	.006	.006	.004	.004	.004	.004
Injection point-to-target spacing, in.	2	1.0	1.0	1.0	1.0	1.0	1/4	1.0	1/4	1.0	1.0	1/4	1/4
Injection duration, sec	(c)	322	98	269	290	104	107	230	125	235	78	196	125
Injection rate, lbs/sec(d)	(c)	.0029	.0025	.0022	.0017	.0023	.0022	.0021	.0019	.0013	.0014	.0012	.0011
Recirculating Water System													
Pressure, psig	2650	2650	2650	2650	2650	2650	2650	2650	2650	2650	2650	2650	2650
Wastage													
Material	C.S.	←			2-1/4 Cr-1 Mo Steel								→
Depth of penetration, mils	16	(b)	25	100	13-15	19	80	28	72	0	2	38	24
Wastage rate, mils/sec	.05	–	.25	.37	.05	.18	.75	.12	.58	0	.03	0.19	0.20
Wastage rate, mils/lb water	1.8	–	97	167	29	79	330	58	298	0	7	158	175
Maximum measured tube temperature, F	(d)	~ 800(c)	930	1030	947	1042	1075	892	970	778	777	1214	1030
Hydrogen Behavior in Cover Gas													
Concentration before test, ppm	(j)	300	1250	3250	10000	2000	5050	5000	3000	2250	4500	5500	6250
Peak concentration, ppm	-	(k)	17600	49000	48350	23300	28000	42500	20500	35500	20000	35000	26000
Hydrogen concentration change, ppm	(a)	(a)	16350	45750	38350	21300	22950	37500	17500	33250	15500	29500	19750
Elapsed time between leak initiation and initial increase in H_2 concentration, sec	(a)	(a)	80	103	100	89	97	110	105	110	122	110	60
Elapsed time between leak initiation and peak H_2 concentration, sec	(a)	(a)	300	470	460	294	327	420	315	390	315	480	320

(a) Unknown; assumed to be approximately equal to that in Test No. 2.
(b) Tube failed by pressure following thinning by wastage.
(c) Temperature before target tube rupture.
(d) No thermocouples in wastage area.

<u>Note</u>: In Test No. 5 the water injection was displaced from the target center; injection nozzle was possibly partially plugged.

From a comparison of these values with the data in Table 4 it can be seen that the injection rate and the injection duration in the individual tests varied considerably from the desired values. This situation is a result of the non-uniformity in the bore size of the capillary tubing that was used to control the leak rate. Capillary tubing of 0.004 inch and 0.006 inch nominal bore was procured from a commercial source. Efforts to obtain higher precision bore capillary or orifices in time for the tests were not successful.

Figure 14 shows a correlation of tube wastage rate vs. leak rate for Test Nos. 3 through 13. It is evident from the graph, and verified by statistical analysis, that of the four variables the leak rate and the leak-to-target distance were the significant variables. Analyses were made to determine if there are other and/or better correlating parameters, namely total metal loss, metal loss rate, and total water injected; but no such correlation was found.

During the conduct of the initial test series in the Rig 10 test apparatus, some similar tests were run in a simple static system for screening candidate tubing materials prior to testing them in Rig 10. The static system, designated as Rig 43, is shown in Figure 15. The Rig 43 reaction vessel is a 12 inch diameter, 42 inch long, low carbon steel vessel. A pressure control valve, set at five psig, normally vents the hydrogen; and for abnormal conditions, a six inch diameter rupture disk, designed to rupture at 30 psig, is provided. Both relief devices discharge through a six inch pipe to an oil filled reaction products tank which removes solids and liquids. The hydrogen passes through a flame arrestor to the atmosphere. The water injection system is very similar to the one used in Rig 10. Nitrogen at 2650 psig pressurizes the injection system, and low pressure nitrogen is used as a cover gas in the reaction vessel and the reaction products tank. Prior to filling the reaction vessel with sodium, the water injection and trigger assembly is installed, filled with distilled water, and pressurized. The sodium is then loaded into the reaction vessel and brought to the test temperature. The water injection trigger assembly is then actuated injecting the water into the sodium pool.

In planning the materials screening tests to be run in Rig 43, a question was raised concerning the expected reproducibility of Rig 43 and Rig 10 results inasmuch as there are major differences between the two rigs namely: 1) velocity of sodium past the target; 2) sodium head above the reaction point; and 3) congestion around the target, i.e. in Rig 43 a single target tube is used whereas a tube bundle is used in Rig 10 and 4) cooling of the target tube. Evaluation of the significance of these differences with respect to test results was undertaken by conducting a series of three tests in Rig 43 with test conditions that were within the range of the Rig 10 half replicate test series. The test conditions and results are given in Table 5. Based on the Rig 10 test results penetrations of 70 to 80 mils were expected in the Rig 43 tests; so it was very surprising that no penetration was experienced.

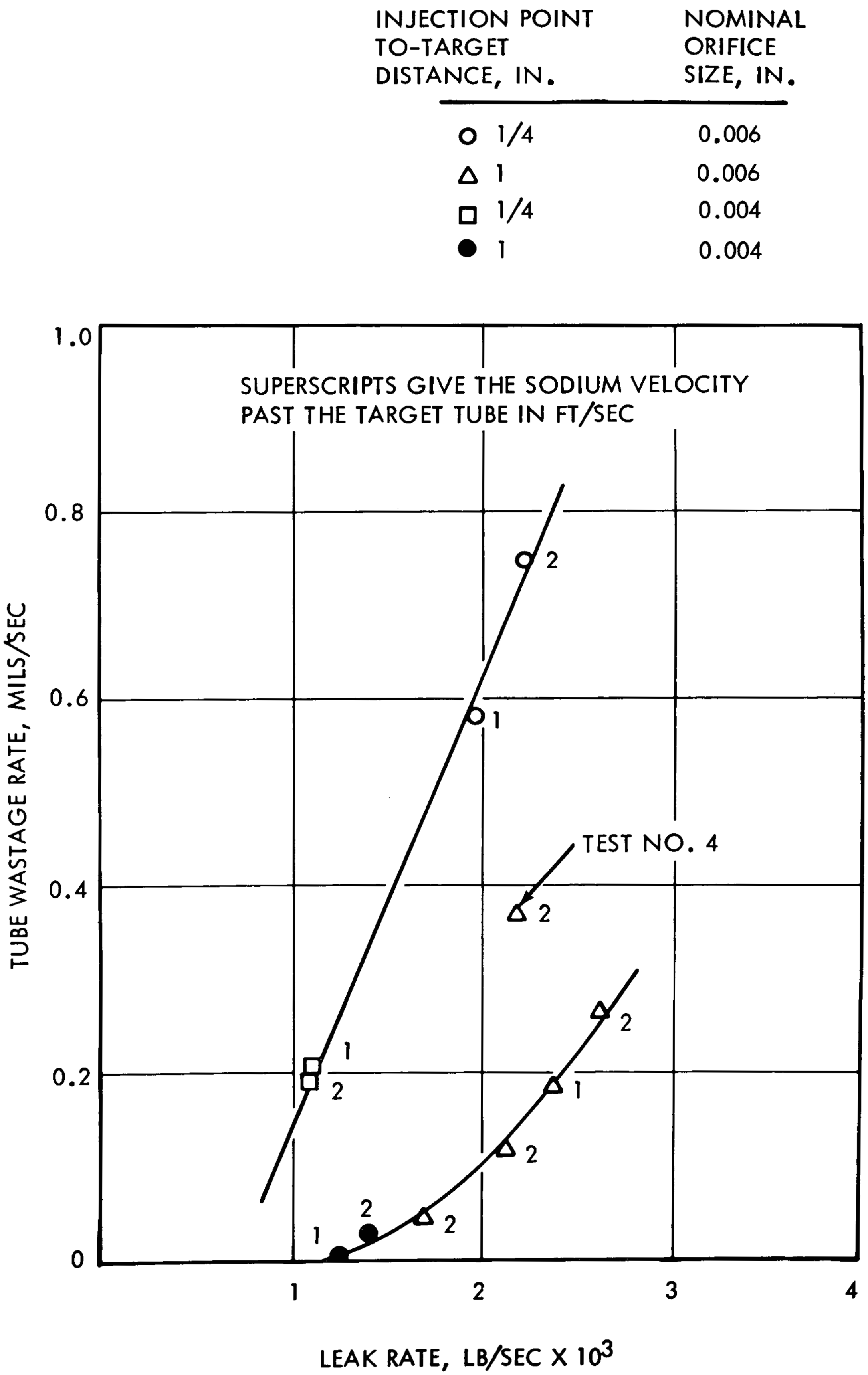

FIG. 14 CROLOY 2-1/4 TUBE WASTAGE RESULTS - TESTS NO. 3 THROUGH 13

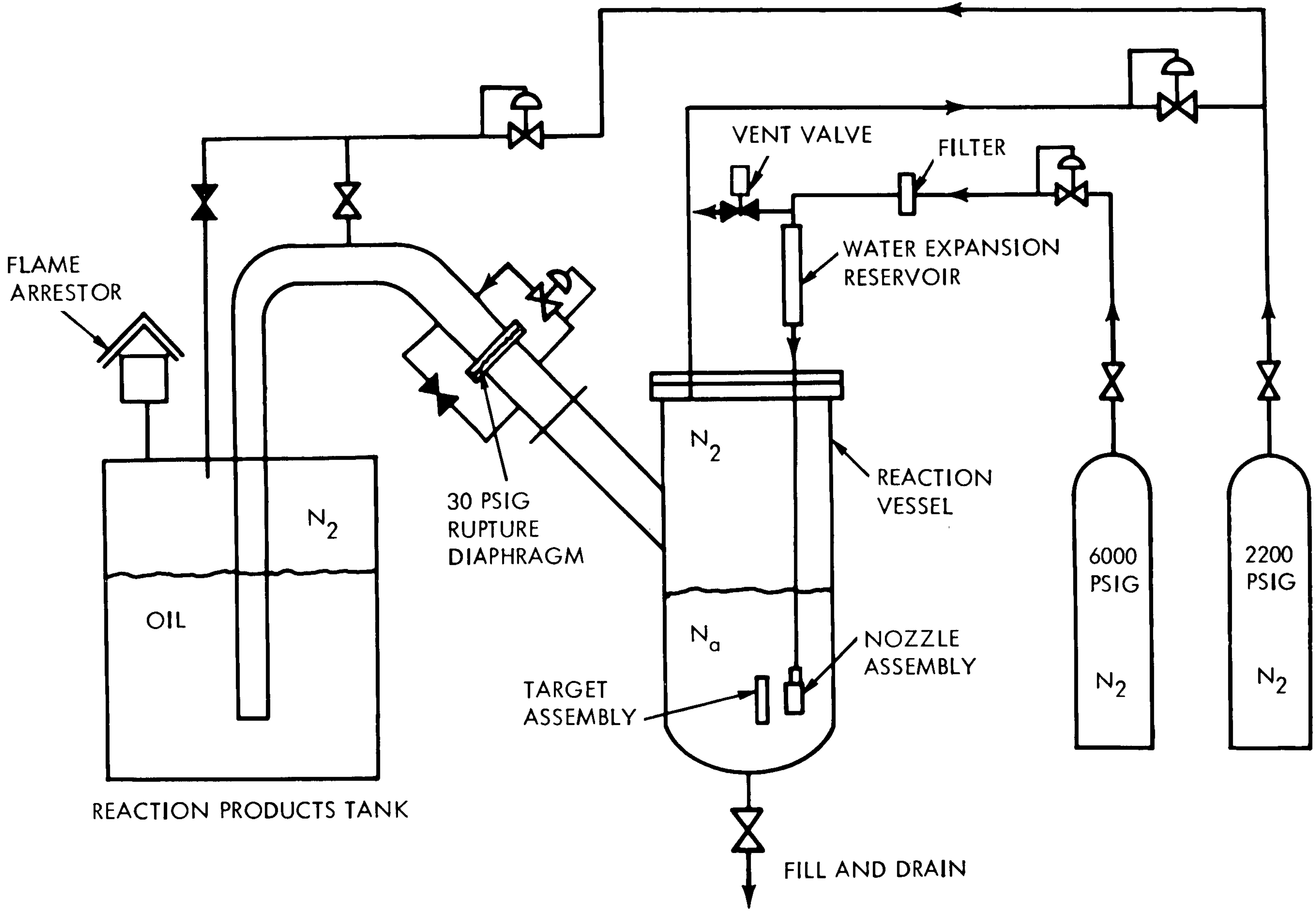

FIG. 15 APDA SMALL LEAK STUDIES - TEST RIG 43

TABLE 5

APDA SMALL LEAK STUDIES
SUMMARY OF RIG 43 WASTAGE TEST RESULTS

	Test No.		
	1	2	3
Orifice Size (nominal), in.	0.008	0.008	0.008
Water Added, lb.	0.24	0.24	0.24
Pressure of Injected Water, psig	2650	2650	2650
Bulk Sodium Temperature, °F	630	630	630
Injection Point-to-Target Spacing, in.	1/4	1/4	1/4
Target Material	← Croloy 2-1/4 →		
Injection Duration, sec	70	61	52
Injection Rate, lb/sec	0.0035	0.0040	0.0047
Maximum Measured Tube Temperature, °F	1440	1390	1610
Depth of Penetration, mils	← Negligible →		

To understand which of the differences between the two test rigs were responsible for the unexpected test results, it was decided to run some special tests in Rig 10 in which Rig 43 test conditions would be reproduced. The test conditions and test results are given in Table 6. Test Nos. 14 and 23 were run with stagnant nitrogen in the target tube instead of circulating water. Test Nos. 15 and 22A were run in static sodium i.e. the sodium pumps were turned off. Test No. 16 also was run in static sodium but with a reduced sodium level, and Test No. 17 was run with a very low sodium flow. The wastage rate vs. leak rate data for these tests are shown in Figure 16 for comparison with the data obtained under normal Rig 10 testing conditions. The substitution of stagnant nitrogen for circulating water does not appear to be significant. At first glance it appears that sodium velocity and head of sodium are significant but this may not be so -- the number of data are few and they may be within the range of the statistical variance of all the data. In conclusion, the absence of any wastage in the Rig 43 tests is not understood. A sodium circulation system has since been added to Rig 43 and testing will be initiated soon to investigate further the effect of sodium velocity and other variables.

The third phase of small leak testing in Rig 10 has consisted of comparing other candidate steam generator tube materials to 2-1/4 Cr-1 Mo. In selecting a list of candidate tube materials the following Companies and facilities were contacted: Argonne National Laboratory, Babcock & Wilcox, Westinghouse Atomic Power Department, Atomics International, and United Nuclear Corporation. The candidate materials ranked in the order of their estimated relative ability to withstand the effects of a sodium water reaction are Inconel, Incoloy, the stainless steels, the Croloys, and carbon steel. Thus far eight tests have been run with Incoloy 800 and two with 321 SS. The test conditions and results are given in Table 7. Figure 17 shows the wastage data for these materials compared to Croloy 2-1/4 - 1 Mo. These results are consistent with the expected relative resistances of these materials to the sodium-water reaction.

At this time the fourth phase of the small leak studies are in progress. This phase consists of testing 2-1/4 Cr-1 Mo and 304 SS at increasingly larger leak rates. It is expected that there is some higher leak rate at which the wastage rate will reach a maximum and that at still higher leak rates the wastage rate will decrease. The basis for this belief is that the reaction rate becomes so large that the hydrogen will force away the reactants thereby reducing the tube damage. It is known that in large leak tests there has been no evidence of tube material wastage.

<u>Causes of Tube Wastage</u> - The tube wastage tests are being conducted primarily on an empirical basis. Because of the large number of test variables and the interest in several tube materials the number of tests required is very large even to obtain a sketchy understanding of the interaction of the

TABLE 6

APDA SMALL LEAK STUDIES
RIG 10 TESTS SIMULATING RIG 43 CONDITIONS

	TEST NO.					
	14	15	16	17	22A	23
Sodium System						
Flowrate, gpm	400	0[a]	0[a]	20[b]	0[a]	400
Velocity past target tube, ft/sec	2	0	0	.1	0	2
Bulk Temperature, °F	610	610	620	610	600	602
Sodium level above target tube, ft	8.4	8.4	1.25	8.4	8.4	8.4
Injection Water System						
Water added, lbs	0.24	0.24	0.24	0.24	0.24	0.24
Temperature, °F	610	610	620	605	600	602
Pressure, psig	2650	2650	2650	2650	2650	2650
Orifice size, in. (nominal)	0.006	0.006	0.006	0.006	0.006	0.006
Injection point-to-target spacing, in.	1/4	1/4	1/4	1/4	1/4	1/4
Injection duration, sec	133	125	153	145	236	145
Injection rate, lbs/sec	0.0012	0.0019	0.0016	0.0017	0.0010	0.0017
Recirculating Water System						
Pressure, psig	No Water[c]	2650	No Water[c]	2650	2650	No Water[c]
Wastage						
Material	2-1/4 Cr-1 Mo Steel					
Depth of penetration, mils	38	12	27	35	21	41
Wastage rate, mils/sec	0.20	0.10	0.18	0.24	0.09	0.28
Wastage rate, mils/lb water	158	50	113	146	87.5	170
Maximum measured tube temperature, °F	1240	1380	1180	960	1780	940

(a) Sodium pump turned off.
(b) Nominal flow rate -- actual flow rate estimated to be between 0 and 40 gpm.
(c) Test was run with stagnant nitrogen in the target tube.

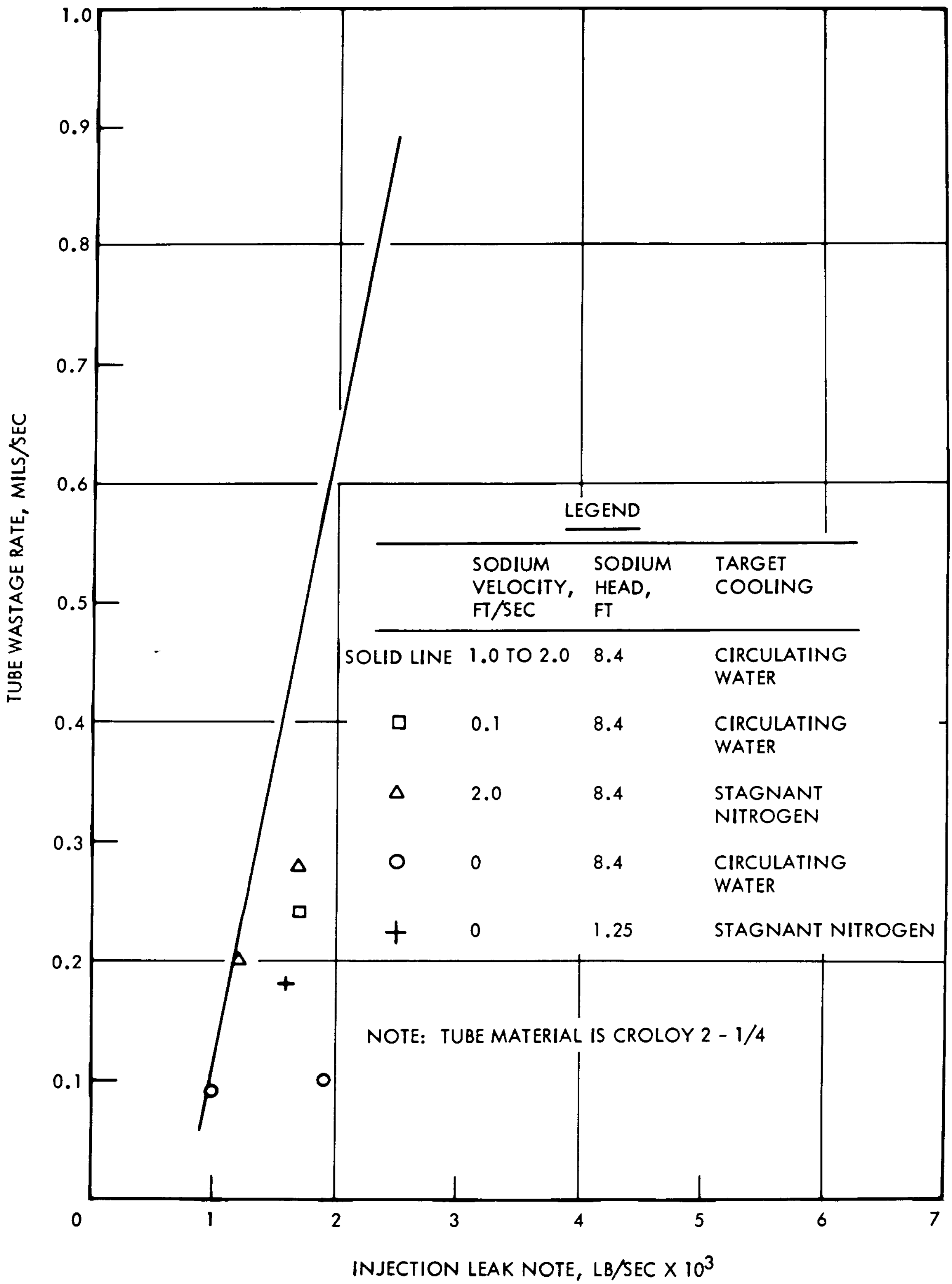

FIG. 16 EFFECT OF SODIUM VELOCITY, SODIUM HEAD, AND TARGET COOLING ON WASTAGE RATE

TABLE 7

APDA SMALL LEAK STUDIES TUBE WASTAGE TESTS - MATERIALS COMPARISON

	TEST NO.									
	18	19	19A	20	21	24	25	26	27	28
Sodium System										
Flowrate, gpm	400	400	400	400	400	400	400	400	400	400
Velocity past target tube, ft/sec	2	2	2	2	2	2	2	2	2	2
Bulk temperature, °F	610	610	604	600	400	625	602	610	605	622
Injection Water System										
Water Added, lbs	1.3	(a)	1.3	1.3	1.3	0.65	0.24	0.24	1.3	1.3
Temperature, °F	608	613	604	600	600	625	603	610	605	622
Pressure, psig	2650	2650	2650	2650	2650	2650	2650	2650	2650	2650
Orifice size, in. (nominal)	0.006	0.004	0.004	0.006	0.006	0.010	0.004	0.006	0.016	0.040
Injection point-to-target spacing in.	1	1/4	1/4	1/4	1/4	1/4	1/4	1/4	1/4	1/4
Injection duration, sec	799	856	861	691	570	103	208	618	100	23
Injection rate, lbs/sec	0.0016	(a)	0.0015	0.0019	0.0023	0.0063	0.0012	0.0021	0.0013	0.0565
Recirculating Water System										
Pressure, psig	2650	2650	2650	2650	2650	2650	2650	2650	2650	2650
Wastage										
Material	Incoloy 800	Incoloy 800	Incoloy 800	Incoloy 800	Incoloy 800	Incoloy 800	321 SS	321 SS	Incoloy 800	Incoloy 800
Depth of penetration, mils	0	0	0	0	12	4	0	17	0	0
Wastage, rate mils/sec	0	0	0	0	0.02	0.04	0	0.03	0	0
Wastage rate, mils/lb water	0	0	0	0	9.2	6.2	0	13.1	0	0
Maximum measured tube temperature, °F	1020	1190	1150	1186	1341	1621	1081	1526	1803	1793

(a) Test terminated due to plugged capillary.

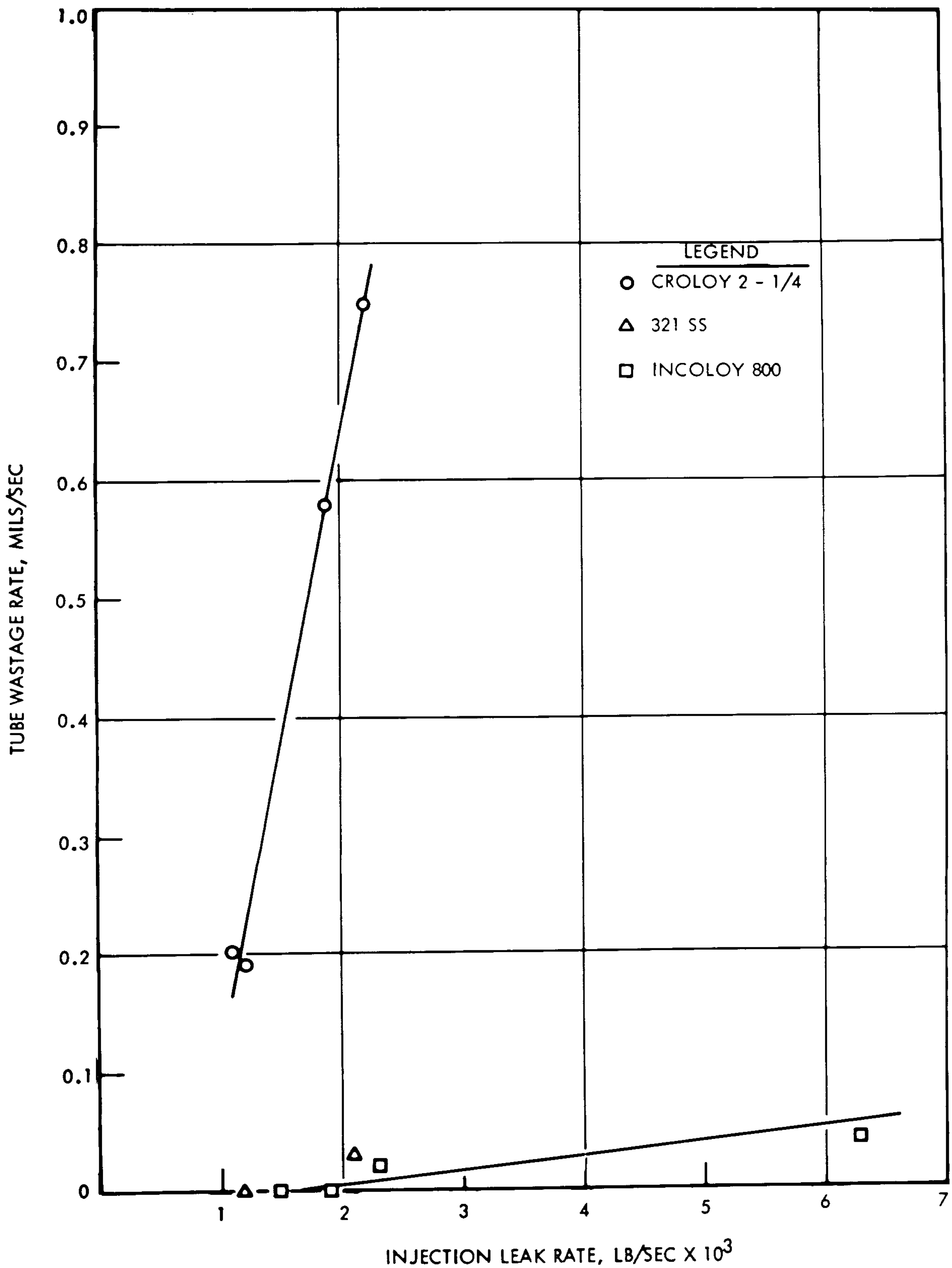

FIG. 17 APDA SMALL LEAK STUDIES, TUBE WASTAGE, MATERIALS COMPARISON

important variables. In an attempt to put the test program on a more fundamental basis and also to provide basic data for interpretation of test results, a parallel effort is being carried that is aimed at obtaining an understanding of the wastage phenomena. This effort consists of several parts namely: Sodium-water reaction jet characteristics, tube surface temperatures, metallurgical examination of target tubes in the area adjacent to metal wastage, erosion experiments, and corrosion experiments.

When high pressure water leaks through a small hole into high temperature sodium there occurs in the immediate vicinity a complex phenomena distinguished by very high fluid velocities, a vigorous exothermic chemical reaction, generation of large volumes of hydrogen gas, and generation of reaction products that are known to be extremely corrosive in sodium systems i.e. sodium hydroxide, sodium hydride, and sodium oxides. It is believed that tube wastage is caused either by erosion or corrosion or both, and that temperature is a most significant factor.

Under the conditions of steam generator temperatures and pressures, there will be some flashing of the water as it leaks through tubing and weld defects; and the jet issuing from the defect into the sodium will consist of a steam-liquid mixture. For a typical Rig 10 test, the jet as it discharges from the defect into the sodium is calculated to have a composition of 80% water and 20% steam, and the calculated velocities of the water and steam phases are 306 ft/sec and 1050 ft/sec respectively. Measurements of the temperatures, dimensions, and stability of the sodium-water reaction zone have been obtained with a nominal 0.008 inch diameter leak. The tests were conducted in Rig 43. The general test conditions were typical of the Rig 10 wastage tests and were as follows:

Sodium Temperature, °F	600
Injection Water Temperature, °F	600
Injection Water Pressure, psig	2650
Weight of Water Injected, lb	0.11
Injection Rate, lb/sec	3×10^{-3}
Injection Nozzle Capillary ID, in. (nominal)	0.008

Reaction zone temperatures were measured using thermocouple target assemblies which were positioned at various distances from the leak. One assembly, shown in Figure 18 consisted of sixteen 1/16 inch diameter, Type 304 stainless steel sheathed thermocouples swaged down to 0.040 inch at the hot junction. The thermocouples are mounted in a 3 inch diameter rim similar to spokes on a wheel, and the sixteen hot junctions form a plane target approximately 3/4 inch in diameter. In the tests, the spacing between the leak and the plane target assembly was varied over the range 1/4 inch to 1-1/2 inches. During a test, temperatures were recorded on a high speed oscillograph. The maximum temperatures measured as a function of distance from the leak are shown in Figure 19. They are much lower than

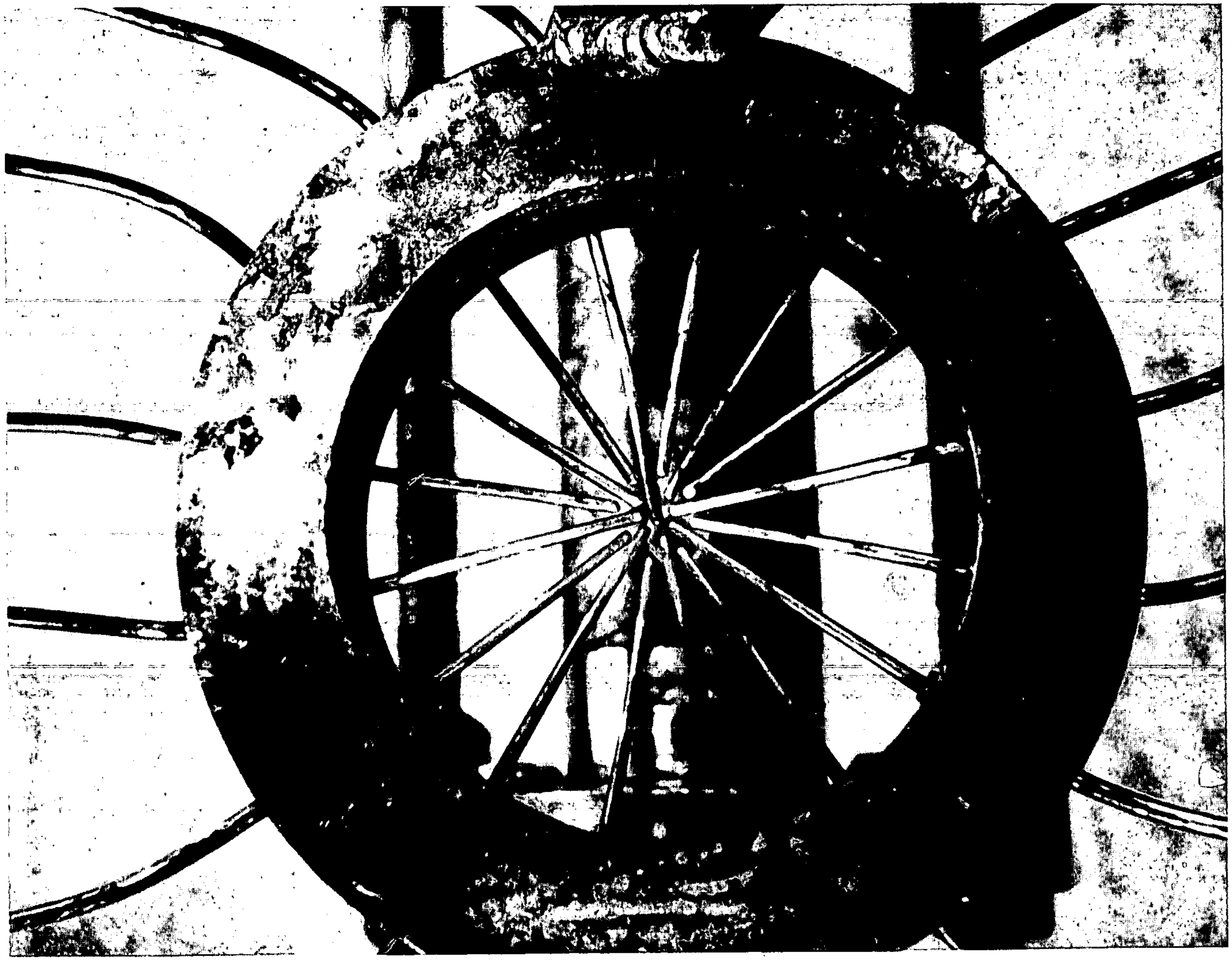

FIG. 18 APDA SMALL LEAK STUDIES - THERMOCOUPLE TARGET ASSEMBLY

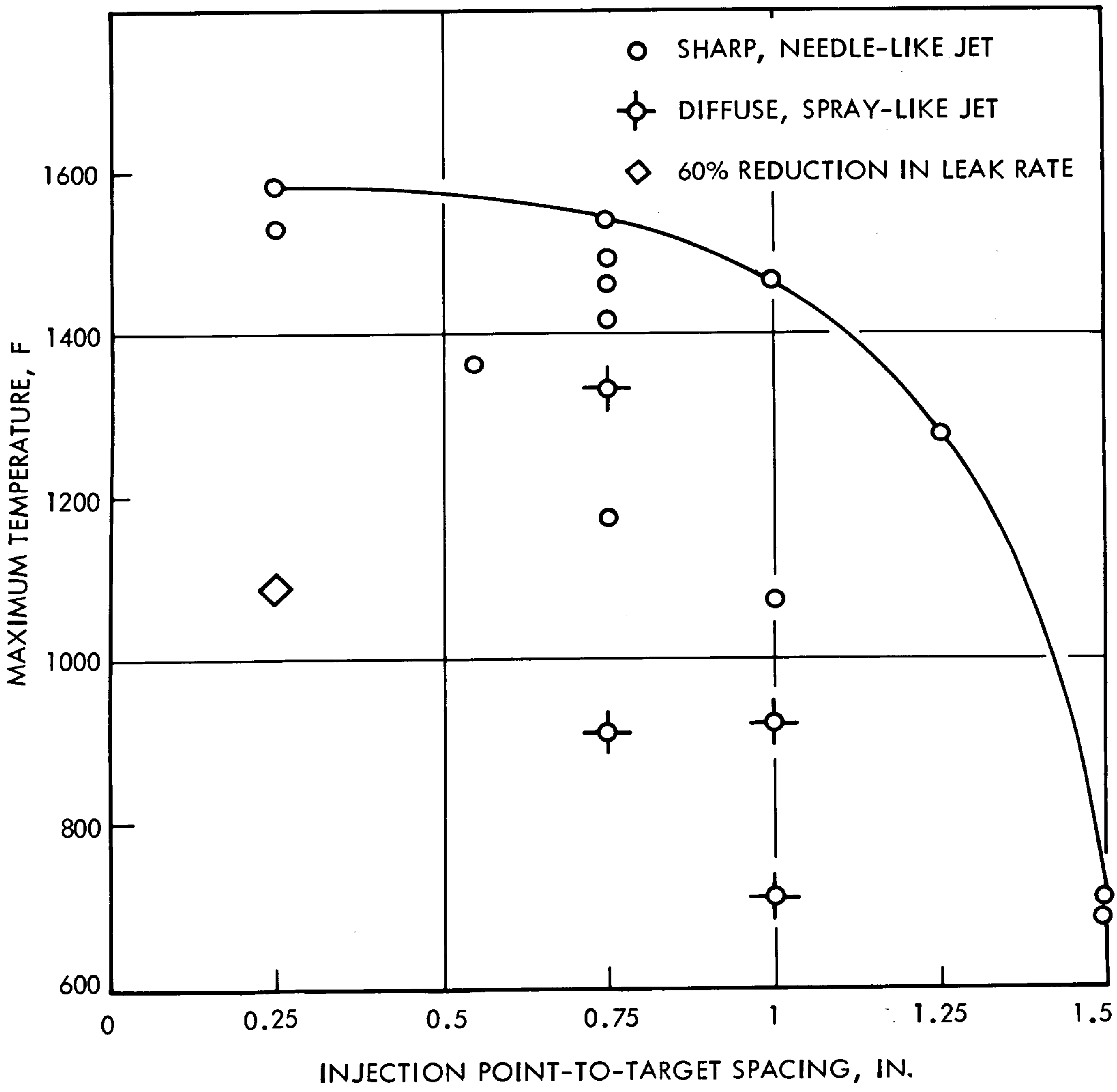

FIG. 19 APDA SMALL LEAK STUDIES, SODIUM-WATER REACTION TEMPERATURES

the calculated adiabatic reaction temperature of 3000°F. It is believed that the reaction temperatures are higher for larger leaks. The test results indicate that the spatial orientation of the jet was very stable and that the cross-section of the reaction zone was small -- being about 1/4 inch in diameter at a distance of 1-1/4 inches from the leak. These observations are corroborated by the Rig 10 tests in which the tube wastage generally was concentrated at one spot, about 1/8 inch to 1/4 inch in diameter.

Metallurgical examination has been made in the vicinity of the wastage of all target tubes from Rig 10 Test Nos. 3-13 in an attempt to understand the mechanism of tube wastage. A summary of the information obtained is as follows:

1. There is no evidence that during the test the material temperature was as high as the eutectoid temperature.

2. There is no microstructural evidence for the existence during the tests of any temperature significantly above the operating test temperature. The cementite present in the microstructure did not spheroidize, and there was no grain growth in the ferrite phase.

3. The lack of any detectable bulging of the material remaining in the bottom of the erosion pit of specimen No. 4, which had only an approximate 0.020 inch of wall thickness remaining, indicate that the maximum temperature of the wall could never have been much above the sodium temperature.

4. There is no evidence of any corrosive attack on the target tubes by the sodium-water reaction jet. The surface features observed, being bright and semimatte in appearance, are consistent with an erosion mechanism for the removal of the metal in the target areas. Cross-section microstructural examination in both the as-polished and polished-and etched conditions failed to show the presence of any type of surface film.

A test program is about to begin on investigating erosion and corrosion as tube wastage mechanisms. High velocity jets of sodium, sodium hydroxide, and water at steam generator temperatures and pressures will be impinged on candidate tubing materials. The test apparatus is essentially complete and testing will begin this spring.

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2. ANS-101, Fast Reactors National Topical Meeting, American Nuclear Society, Northern California Section, San Francisco, California, April 10-12, 1967.

3. H. Strahl, "A Device for Continuous Detection of Hydrogen in Sodium," NAA-SR-6986, May 1962.

4. J. H. Coleman, E. F. Hill, "In-Line Detector for Hydrogen in Sodium," APDA-222, to be released.

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6. L. Bogaert, "Modular Steam Generator Fabrication," NAA-SR-11739, February 8, 1966.

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DISCUSSION

D.P. White (Sargent & Lundy) - I wish to preface my remarks by saying that it has been a pleasure to be given the opportunity to review and submit comments on this paper. We at Sargent & Lundy feel that the work that the Authors have done in assessing the status of these projects has been invaluable in the overall evaluation of the LMFBR program.

Since this paper presents an updated status report on a continuing project, positive conclusions cannot be discussed at this time. The problems discovered in the manufacturing and design of the Enrico Fermi steam generators during its limited operation and the in-field solutions can be used to advantage in the design and manufacturing of future steam generators. It will be interesting to note on the performance in this unit after the modifications are completed.

The proper functioning of the sodium-water reactor relief system on number one system generator helps to establish faith in the steam generator protection systems. This reliability of the relief system is significant in an operating viewpoint. The ESADA sponsored sodium-water reaction tests can offer invaluable assistance to steam generator designers, primary in material selection and tube design. The correlation between the test results and the analytical model seem encouraging from a design standpoint and tend to help establish reliability of the model.

The development of the sodium to water steam generator is a major task in the overall development of a liquid metal reactor. It is apparent that considerable effort is being expended along these lines. The proof-testing of this unit and its safety system is of paramount importance to the LMFBR Program. The design and capability of the steam generators is quite significant in the overall layout of the plant. The steam generators required for a large liquid metal reactor power generation station constitute a major piece of equipment - comparable in magnitude to the reactor and turbine generator. The more known about the physical size, shape and operating characteristics of the steam generator early in the design of a plant, the easier and less expensive will be the layout of the plant buildings and other equipment. The sodium-water reaction relief system has a major bearing on the design of the facility and its influence on the reactor system must be known early in the design. Finally, a comparison of the operating characteristics and cost between the modular and single unit system generators would aid the designer considerably in performing plant evaluation, layout and design.

It is apparent that some problem areas still exist in the manufacturing and design of sodium heated steam generators; however, they do not seem monumental and with continuing effort, the remaining questions can be resolved.

P.A. Salmon (Public Service Electric & Gas Company) - This interesting paper will be useful to all those who are contemplating the generation of steam by heat exchange from hot sodium. The B&W Integral Superheat Steam Generator and the Atomics International Module concepts differ widely in design philosophy. The AI design appears to be aimed at high availability in spite of leaks, while the B&W design assumes that its features assure reliable operation. The proof testing of the two will be watched with great interest, as will the modified Fermi steam generators. Experience to date has taught us the importance of designing to avoid tube vibration, careful selection of materials, and proper care of the equipment after manufacture is completed.

The importance of an effective and rapid leak-detection system is evident after reading the data on tube wastage. It must have been disappointing to find the lack of correlation between the results of tests in APDA Rigs 10 and 43. In the paper No. 5B/1 "Sodium Technology and Equipment of the BN-350 Installation" by A.I. Leipunskii, et al., presented at the London Conference on Fast Breeder Reactors in May, 1966, they report "There was never any fracture of the adjacent tubes filled with water at a pressure of 50-60 kg/cm^2". This suggests agreement with the results in Rig 10.

In view of the need for higher coolant and steam temperatures, one is inclined to ask whether there should not be a back-up effort such as a binary cycle in which the heat of the primary sodium is used to boil mercury which, in condensing, boils water, or some other cycle in which the leakage of steam or water into sodium is less likely than it is in a sodium-heated steam generator.

K.D. Kuczen (ANL) - Each of the authors have been actively engaged in the development of sodium-heated steam generators. Therefore, it is appropriate that they discuss the current activities of their respective companies pertinent to LMFBR steam generator development.

Both Mr. Probert and Mr. McDonald have indicated that optimum steam temperatures are now lower than they were a few years ago. This comes as no surprise, but the impression one has is that the lower temperatures are related to the use of stainless steel, particularly with respect to its susceptibility to chloride stress corrosion.

Stainless steel for superheaters, valves, turbine nozzles, and main steam piping has been in use for several years. Experience with Type 347 has been less than desirable because of cracking adjacent to welds. However, several large stations have shown successful application of Type 316[1]. The steam generator for SRE has logged approximately 19,000 hours of operation at sodium temperatures varying from 750 to 1060°F. "Inspection of the internals on the steam side indicated no observable deposits, corrosion, or other untoward phenomena".[2] The unit employs Type 304 stainless steel and its excellent condition is attributed to careful control of the water purity. Certainly, stress corrosion of the stainless steels is an important consideration in the choice of materials. However, it appears that water purity can be controlled to minimize, to a large extent, the probability of failure due to stress corrosion. There are, of course, other considerations which may deter the selection of stainless steel, for instance, the formation of the sigma phase, carbide precipitation in the

[1]Baker, R.A. and Soldan, H.M., "Service Experiences at 1050°F and 1100°F of Piping Austenitic Steels," Proc. Inst. Mech. Engrs. 178, Pt 3A, 4-35 (1963-64).

[2]Budney, G.S., "Liquid-Metal-Heated Steam Generator Operating Experience," Liquid Metal Engineering Center, NAA-SR-12534, November 1, 1967.

unstabilized steels and the ability to resist thermal shock fatigue. How much of a deterrent each of these may be appears to be open to question. Test results on Type 316 show that "as carbide precipitation increases with time at temperature (1200°F) the hardness and ultimate strength values are raised and the impact values and ductility values are lowered. However, the very excellent values of the impact tests at the operating temperatures of 1200°F will be noted (in the reference) and the results of tests at room temperature are still quite good compared with other materials."[3]

If the ability to resist thermal shock fatigue is considered to be a function of thermal conductivity, coefficient of thermal expansion and the modulus of elasticity, the stainless steels can be shown to be less resistant than the chrome-moly alloys. However, there are other parameters which are important, such as ductility, fatigue strength, severity of the thermal shock and the geometry of the region in question. Only when these are considered can a quantitative comparison among materials be made.

On the other hand, the stainless steels have at least two desirable characteristics for LMFBR application. Its use in place of the low alloy ferritics practically eliminates the mass transfer of carbon. (There may be carbon transfer in a mono-material system because of temperature differences.) The amount of carbon depletion of the low alloy ferritics over a thirty year life will be significant and will adversely effect the mechanical properties. The degree to which the properties are effected is unknown but could be significant. The use of stabilized low alloy ferritics may be an alternative.

As indicated by Mr. Kovacic, Type 321 stainless steel appears to be much more resistant to wastage than 2-1/4 Cr-1Mo. The British, in their Small Scale Experiments, have found similar results for Type 316.[4] The total weight loss of 2-1/4 Cr-1Mo was found to be almost thirty times greater than the weight loss measured for Type 316.

The writer is not trying to develop a case for the use of stainless steel. Rather, he is attempting to point out that there may be serious limitations to the low alloy ferritic steels especially decarburization-carburization effects and perhaps tube wastage during a sodium-water reaction. These disadvantages may overshadow the ability of the ferritics to resist stress corrosion cracking. Perhaps Mr. Probert and/or Mr. McDonald would comment further upon their basis for material selection and also indicate whether the present choice is assumed to be applicable to the far term, 1000 MWe (or greater) commercial, LMFBR plants.

Present data on sodium-water reactions both in this country and abroad indicates that relief systems will prevent catastrophic failure of the shell. It is apparent, therefore, that the design of such systems must have a very high degree of reliability. Conditions which might interfere with proper operation, such as, sodium and/or its oxide build-up on the face of ruptured discs, must be prevented. Equally important is premature failure of the disc which would seriously effect the plant's availability. This is especially significant in systems which employ vent piping which is common to several units where disc failure could "constipate" the entire vent system. One might estimate the time required to replace or repair and cleanup a steam generator after a sodium-water

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[3]Mochel, N.L.; Ahlman, C.W.; Wiedersum; and Zong, R.H., "Performance of Type 316 Stainless Steel Piping at 500 Psi and 1200°F," Proceedings of the American Power Conference, Volume XXXII, 1966, pp. 556-568.

[4]Bray, J.A.; Bleazard, J.M.B.; Hargreaves, K.; and Ward, B., "Sodium-Water Reaction Experiments on Model P.F.R. Heat Exchanger - The NOAH Rig Tests," TRG Report 1519 (D), 1967, The Reactor Group HQ, Risley, Warrington, Lancs.

reaction, but the effects upon the contiguous system are largely unknown. The British, in their NOAH experiments, found it necessary to drill out the drain piping which became clogged with reaction products. It would appear, then that leak experiments should be performed on steam generators in an environment which simulates the contiguous system. Would Mr. Kovacic care to comment?

Obviously, leak detection methods must be sensitive enough to signal shutdown before the vent system is activated. Small leaks must be detected before tube failure is propagated. The designer has some flexibility in this respect. He can provide sacrificial barriers between tubes or groups of tubes; use a material which is relatively resistant to wastage; design the shell side as the water/steam side -- the modular concept appears amenable to this approach; or even provide double wall tubes. (Note: Exclusive of the SIR steam generator tests where thermal performance and not structural integrity was emphasized, the writer is not aware of failures in double wall units which resulted in sodium-water reactions.) In the writer's opinion, the economics of these approaches can only be evaluated after more is known about local and system effects due to sodium-water reactions.

The writer's last comment is directed to Mr. Kovacic. Current information shows that local high temperatures are developed during the reaction of water with sodium. Is it possible that enough free oxygen is produced by the dis-association of water at the high temperatures to account for rapid tube wastage?

FUEL HANDLING MECHANISMS
FOR LMFBR SYSTEMS

By

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ABSTRACT

Because of its effects on plant availability, fuel handling is the most important system in the LMFBR. Major scheduled outage is for refueling and for no other reason. A semiannual refueling outage of 10-days for a 1000-Mwe Plant would cost $480,000 based on a 2 mill/kwh reserve capacity incremental charge. A one-month decrease in the ex-core fuel inventory holdup time can reduce fuel cycle costs by 0.017 mill/kwh.

General requirements for fuel handling mechanisms are identified by examining interfaces with the other reactor plant systems. The impact of fuel handling on containment, core, vessel, fuel management, and decay storage are discussed. The preservation of fuel integrity during handling is emphasized.

The technology of refueling systems in existence today is explored in terms of advantages, disadvantages, and the technical basis for future systems. In-vessel vs ex-vessel storage and on-line vs off-line refueling schemes are compared and the economics of in-vessel storage for various refueling intervals is presented.

Conclusions are drawn from the LMFBR refueling concepts being considered by the major reactor plant suppliers. The cautious extrapolation of existing technology illustrates the uncertainty in mechanisms that operate in sodium. Development and quality assurance procedures for codes and standards are the key elements in achieving successful fuel handling mechanisms for industry use.

INTRODUCTION

At present, the fuel handling system of a liquid-metal cooled, fast-neutron flux, breeder reactor (LMFBR) is one of the most important systems of such a plant in terms of its effects on plant availability. In addition, the fuel handling system can affect ex-core fuel inventory costs. Sweek and

Amorosi rate fuel handling components second only to safety in their discussions of the U.S. program on the LMFBR.[1]

The major scheduled outage of an LMFBR is for refueling and for no other purpose. Turbine maintenance is, of course, required but may be as infrequent as once in five years. It is optimistically assumed that a refueling outage could be as short at 10 days (if accomplished semiannually) for a 1000-Mwe plant; based on a 2 mills/kwh reserve capacity incremental charge, such an outage would cost $480,000.

The refueling system also affects fuel cycle economics via the time spent by the fuel <u>outside</u> the core. The in-core residence time, and the shipping and reprocessing time are not affected by the refueling scheme; however, the time required before the fuel can be shipped is a variable affected by choice of refueling system. A one-month decrease in this time can reduce fuel cycle costs by 0.017 mill/kwh for a 1000-Mwe plant.[2]

The high cost of fissile inventory is further identified by J. R. Dietrich[3] as having a serious effect on the fuel cycle costs of the fast breeder, and the necessity of keeping this cost within acceptable bounds accounts for one of the most difficult aspects of the fast breeder development.

The preceding factors; i.e., plant availability and ex-core fuel inventory costs, are those which must be weighed against safety, capital cost, development cost, and risk in selecting optimum refueling systems for LMFBR's.

FUEL HANDLING MECHANISM REQUIREMENTS

The general requirements for fuel handling mechanisms can be identified by examining their interfaces with the reactor plant systems.

The requirements imposed by the fuel and core components to be handled determines the size of the fuel handling equipment. The extrapolated target burnup on fuel sets decay heat load requirements. The fuel may become extremely fragile at high burnups; however, the fuel handling designer must meet the requirements to preserve the physical and chemical characteristics of the fuel during handling from the core to the point of examination, to preserve the key data which will allow extending fuel burnup. Ultimately, the operator must limit burnup according to the observed condition of the fuel after it has been removed from the core by the handling system.

The interface with the core and vessel imposes requirements on the fuel handling designer to protect the core from damage and eliminate the possibility of melting or otherwise damaging the fuel. Where in-vessel decay storage is to be utilized, the design of the storage scheme must be optimized to add as little as possible to the vessel size. Capability to recover from off-normal conditions incorporates requirements: to routinely handle failed or distorted fuel, to recover from fuel stuck in the core or in the handling system, and to routinely manage plutonium and fission products associated with failed fuel.

The interface with the containment design requires close examination of equipment size. In some fuel handling schemes, the handling mechanisms set the size of the containment structure, and major tradeoff factors exist.

Decay heat removal requirements are principally functions of what handling operation is being performed and of the decay time since shutdown. Withdrawal from the core within a few hours after shutdown requires considering decay heat loads in excess of 100 kw per fuel subassembly. Present concepts require maintaining continuous submersion in liquid metal. After decay periods in the order of 6 months, this is down to about 5 kw. Withdrawal of the subassembly from the reactor in a sodium-filled container such as is used in the Enrico Fermi system is limited to below 50 kw in a stagnant gas atmosphere, depending on the length and surface area of the container. Handling a bare subassembly in a stagnant gas atmosphere is limited to 1 or 2 kw with 5 hours before temperature limits are exceeded.

PRESENT TECHNOLOGY

The design approach to fuel handling systems in existence today has been dictated by the design objectives of the individual plant. In all cases, there was less emphasis on refueling rates than that expected of future systems. The high cost of downtime for a large FBR is likely to make high refueling rates a principal tradeoff factor in the future.

Fuel handling mechanisms for sodium-cooled reactors in existence today have served the industry as well as could be expected, considering that all were built on limited project-oriented research with considerable in-system testing and rework. Without the existence of codes and standards, the fuel handling designer has had little choice but to proceed on this basis. No one project could, in the past, assume the costs of standards development.

Fuel handling mechanisms for liquid-metal systems in existence today are broadly characterized in two categories: under-the-shield handling, and through-the-shield handling. Examples of under-shield handling with in-vessel decay storage are EBR-II and Enrico Fermi. Through-the-shield handling examples are SRE, Hallam, Dounreay, and Rapsodie.

Under-the-shield handling, with in-vessel storage, shown schematically in Figure 1, has a potential for high refueling rates and a minimum of preparation time. The fuel is maintained under sodium until a sufficient decay period has transpired to permit gas cooling, thus minimizing the possibility of fuel overheating.

Disadvantages of this general approach are:

- The difficulty of maintaining the radioactive fuel handling mechanisms, should a malfunction occur during operation,
- Operation of some of the fuel handling mechanisms in liquid sodium,
- An increase in vessel size to make room for the in-vessel storage positions, and
- Possible safety problems in connection with removal of fuel from in-vessel storage with the reactor in operation. If this operation is ruled out for safety reasons, the fuel may have to be left in the reactor vessel until the next refueling shutdown, thereby incurring an ex-core inventory penalty.

Through-the-shield handling, with ex-vessel storage, shown schematically in Figure 2, is a system which has been popular in the past on reactor designs in which the fuel elements have had relatively low decay heat generation rates after reactor shutdown. This permitted simple gas cooling of the fuel element as it was lifted directly from the core. The advantages of this method are that it is relatively simple, affords separation of the fuel handling system and the reactor design, permits shipment of fuel as soon as it has decayed sufficiently, is independent of reactor operation, and permits a vessel of minimum size, since no in-vessel storage is required. (It should be

UNDER-THE-PLUG HANDLING

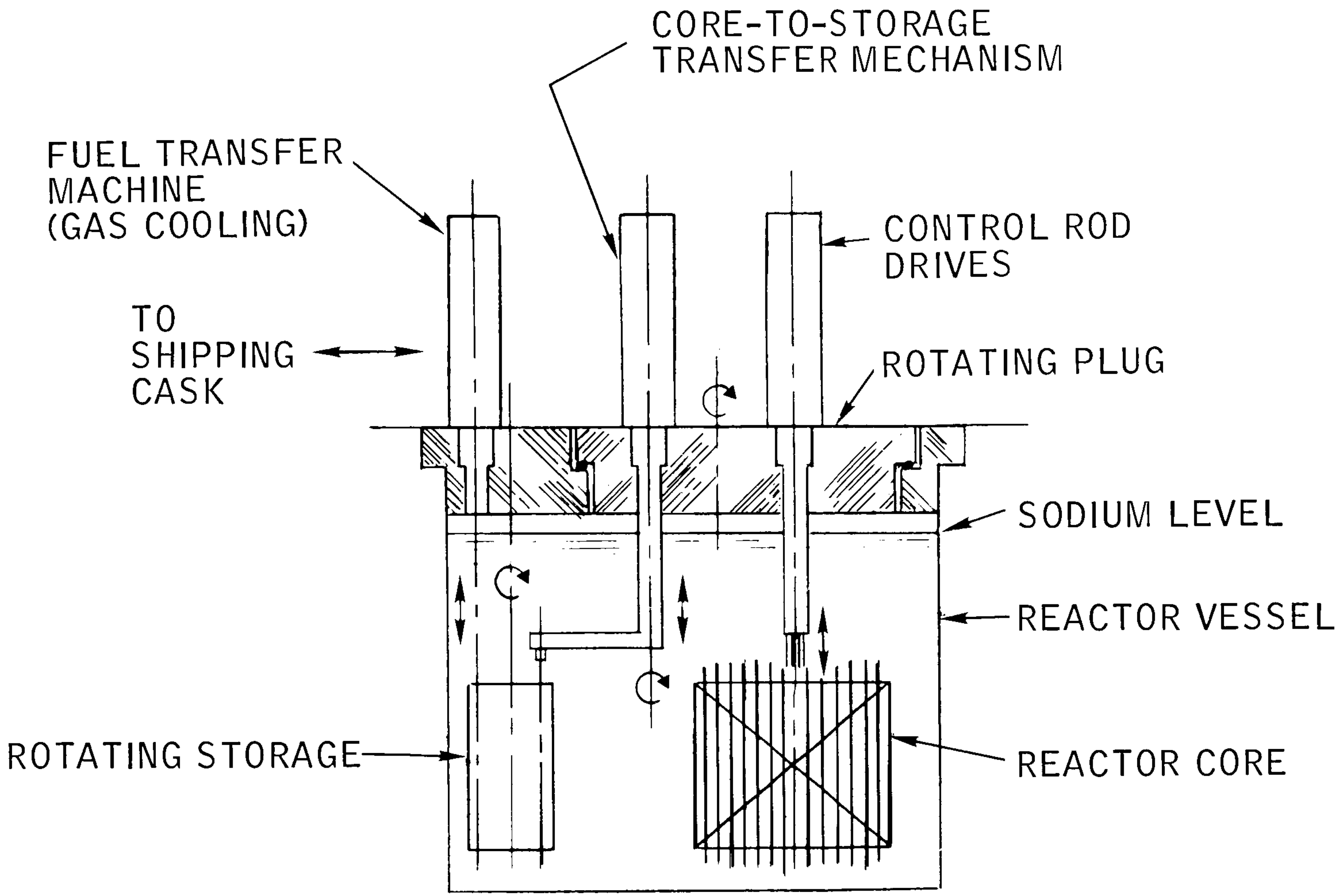

Figure 1

THROUGH-THE-PLUG HANDLING

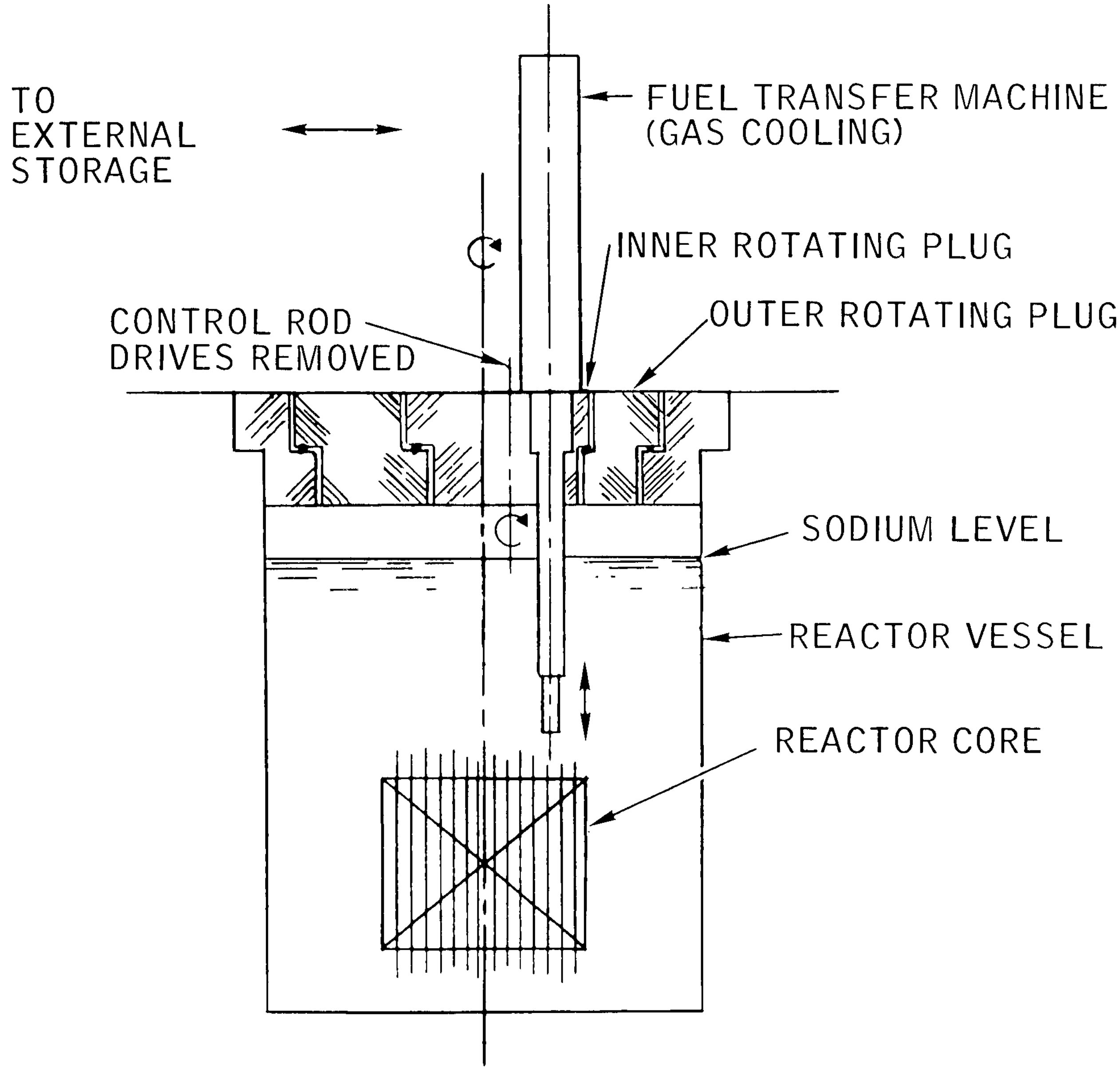

Figure 2

noted that this last advantage applies primarily to "loop-vessel" type reactor designs. The vessel of a "pool" type reactor is usually large enough, for other reasons, to accept in-vessel storage with no cost penalty.) This system, however, becomes more complex as fuel element decay heat loads increase to the 50 to 100 kw range expected for an LMFBR. The cooling system for the fuel handling machine must be extremely reliable. Liquid-metal cooling may be required for adequate heat transfer. In addition, time-and-motion studies performed to date indicate that this system is likely to be slower than the under-the-shield system.

Fuel handling mechanisms which have matured through service have demonstrated adequate safety. They have also demonstrated relatively high reliability of the under-shield equipment. EBR-II and Fermi systems have, for example, completed over 3000 in-core handling cycles. While this is encouraging, in terms of an estimated 7000 replacements of fuel, blanket reflector, and control elements in the life of a 1000-Mwe core, the performance of the ex-core mechanisms has not been satisfactory. Both plants have experienced less difficulty with the under-shield equipment than with the mechanisms external to the core.[4,5] This is probably due to the in-core equipment receiving more attention because of its critical nature.

Direct gas cooling of the fuel element, such as used in the EBR-II, has little potential for use in future systems. The limitations on gas cooling make it unsuitable for FBR fuel without prohibitively long decay periods which add to fuel inventory. The lack of thermal inertia of gas cooling provides little or no margin in the event of loss of circulation. The transition through the sodium/gas interface leaves the fuel uncooled during some period of time. If the gas coolant stream is started prior to lifting fuel clear of the sodium, gas bubbles and the potential for gas entrainment in the primary system is encountered. Sodium entrainment in the cooling-gas stream introduces operating and maintenance difficulties although these have solutions. If gas cooling is to be used in FBR fuel handling, it will probably utilize a sodium-filled container which builds on the Fermi experience.

Liquid-Metal Fast-Reactor fuel handling system experience has derived principally from EBR-II, Fermi, and Dounreay reactor systems. Some perimeter experience has been gained with the SRE and Hallam systems. Although there have been developmental problems with all of these machines, they have been minimal for this class of equipment. In general, the designers did a remarkable job in anticipating and avoiding potential problems associated with sodium and sodium vapor. Those problems that did exist have been overcome with rework.

FUEL HANDLING SYSTEMS AND MECHANISMS FOR FUTURE LMFBR PLANTS

Refueling systems for future LMFBR plants will be based in whole or in part on the proven concepts discussed in the preceding paragraphs. As can be seen in Table 1, a wide variety of systems and mechanisms for LMFBR refueling are being seriously pursued both domestically and abroad. (The Russian BN-350 refueling system is not shown in the table because of lack of recent published descriptions of the plant; however, earlier documents indicated a double rotating plug with a transfer tool limited to vertical motion.)

Two of the seven reactor designs listed utilize an inert hot-cell for fuel transfer. In both systems, the reactor top shield is removed to provide access to the fuel elements. In the hot-cell/in-vessel storage concept,[6] fuel is transferred under sodium to storage racks within the vessel, as shown schematically in Figure 3. Later, after a suitable decay period, spent fuel can be removed from the storage area into a fuel transfer cell, with the

TABLE 1

PROPOSED REFUELING SYSTEMS FOR LMFBR'S

Reactor	Refueling Method	Decay Storage	Removal From Decay Storage	Ref.
GE 300-Mw Demo.	Hot-cell and removable shield	In-vessel	On-line	6
AI 500-Mw Demo.	Double rotating plug and vertical transfer tool	In-vessel	Off-line	6
W 200-Mw Demo.	Hot-cell and removable shield	Ex-vessel	On-line	6
B&W 1000-Mw	Double rotating plugs and vertical transfer tool	In-vessel	On-line	7
British PFR 250-Mw	Single rotating plug and "pantograph" transfer arm	In-vessel	On-line	6-8
French Phenix 250-Mw	Single rotating plug and rotating, rigid, transfer arm	In-vessel	Off-line	6
German Na-2 300-Mw	Triple rotating plugs and vertical transfer tool	In-vessel	Off-line	6

REMOVABLE PLUG IN HOT-CELL WITH IN-VESSEL FUEL STORAGE

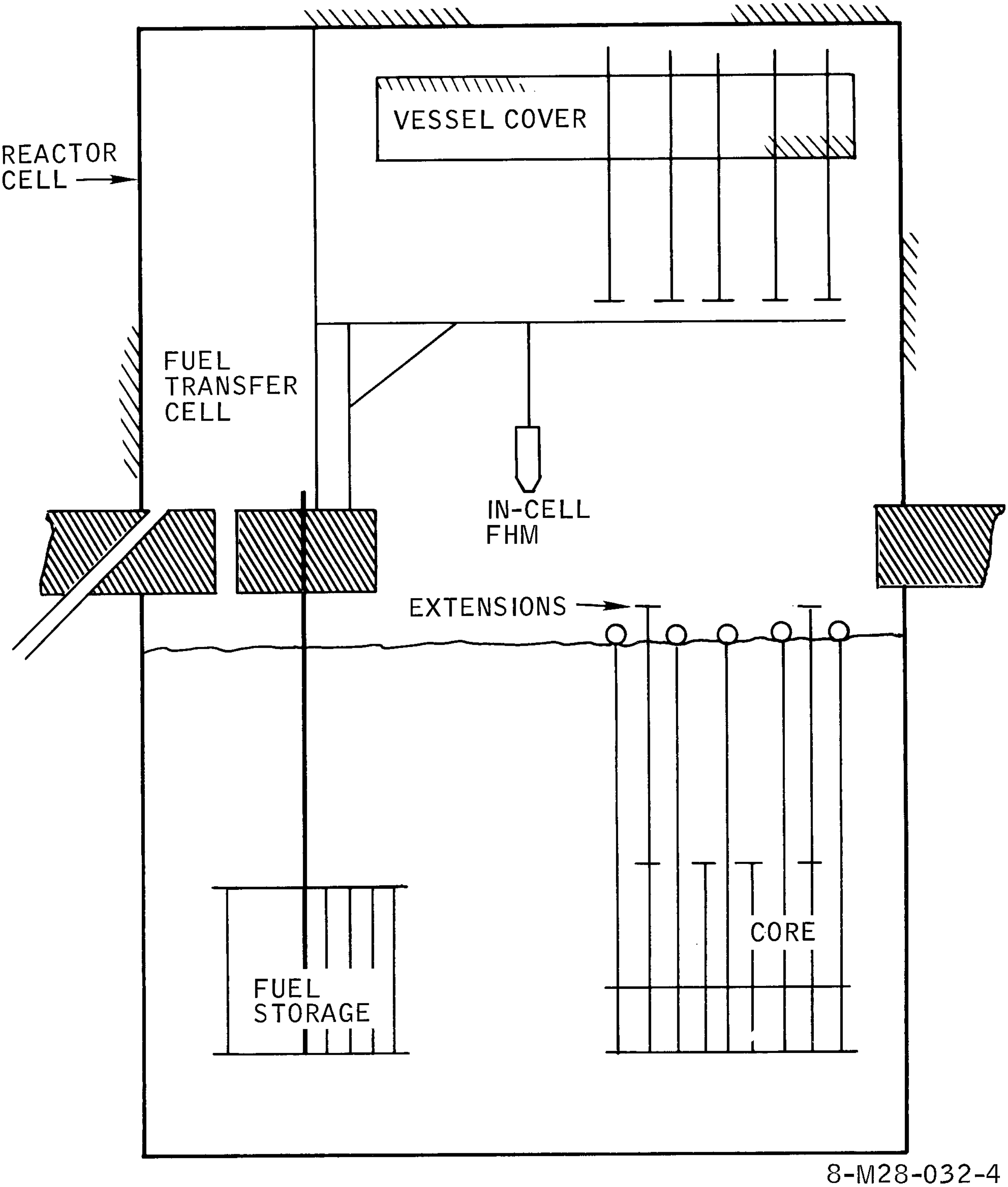

Figure 3

reactor on-line. In the hot-cell/ex-vessel storage concept, Figure 4, a similar inert hot-cell is used, but the fuel elements are withdrawn into a cooled fuel handling machine which transports the element to a decay storage area. After the shield is installed on the reactor and the reactor returned to power, fuel elements can be removed from decay storage for shipment as soon as they have decayed sufficiently.

Both hot-cell concepts have the following advantages.

- The shield is of simple design requiring a make-and-break static seal and no bearings.
- The open vessel results in potential visual fuel handling and direct access to every core component.
- The concept is simple. All mechanisms are accessible for hot-cell maintenance.
- Sodium frosting of refueling mechanisms should not be a problem.
- Unforeseen maintenance tasks on reactor and refueling components can be performed.

In addition, the hot-cell/in-vessel storage concept appears to require relatively little development, since a cooled fuel handling machine is not required. On the other hand, the hot-cell/ex-vessel storage concept minimizes vessel size, since no in-vessel storage is required.

The disadvantages of the hot-cell concept follow.

- A large shielded and heated, gas-tight cell must be provided to house the entire fuel handling system.
- Direct personnel access during fuel handling is not possible.
- Maintenance of control rod drives and in-core instrumentation will be difficult.
- The mechanism and surface areas that become contaminated are several orders of magnitude higher than that of closed vessel concepts.
- Remote emergency handling equipment and procedures must be developed.

Additional disadvantages, if the storage vessel is separate from the reactor vessel, can be charged to the fuel handling machine used to transfer fuel from the reactor vessel to the decay storage vessel. This machine must be capable of handling very high (50 to 100 kw) decay heat loads; hence, liquid metal cooling may be required.

It should be recognized, of course, that the hot-cell concept does not <u>require</u> a removable reactor shield plug. Under-the-shield or through-the-shield fuel handling can be combined with the hot-cell to provide the advantage of both types of systems at an additional cost penalty.

The remaining six of the seven reactor designs tabulated utilize various types of under-the-shield fuel handling systems. These are shown schematically in Figures 5, 6, and 7. The double rotating plug with in-vessel storage is the simplest of the three under-the-shield concepts since the in-vessel fuel

CELL-OVER-REACTOR HANDLING

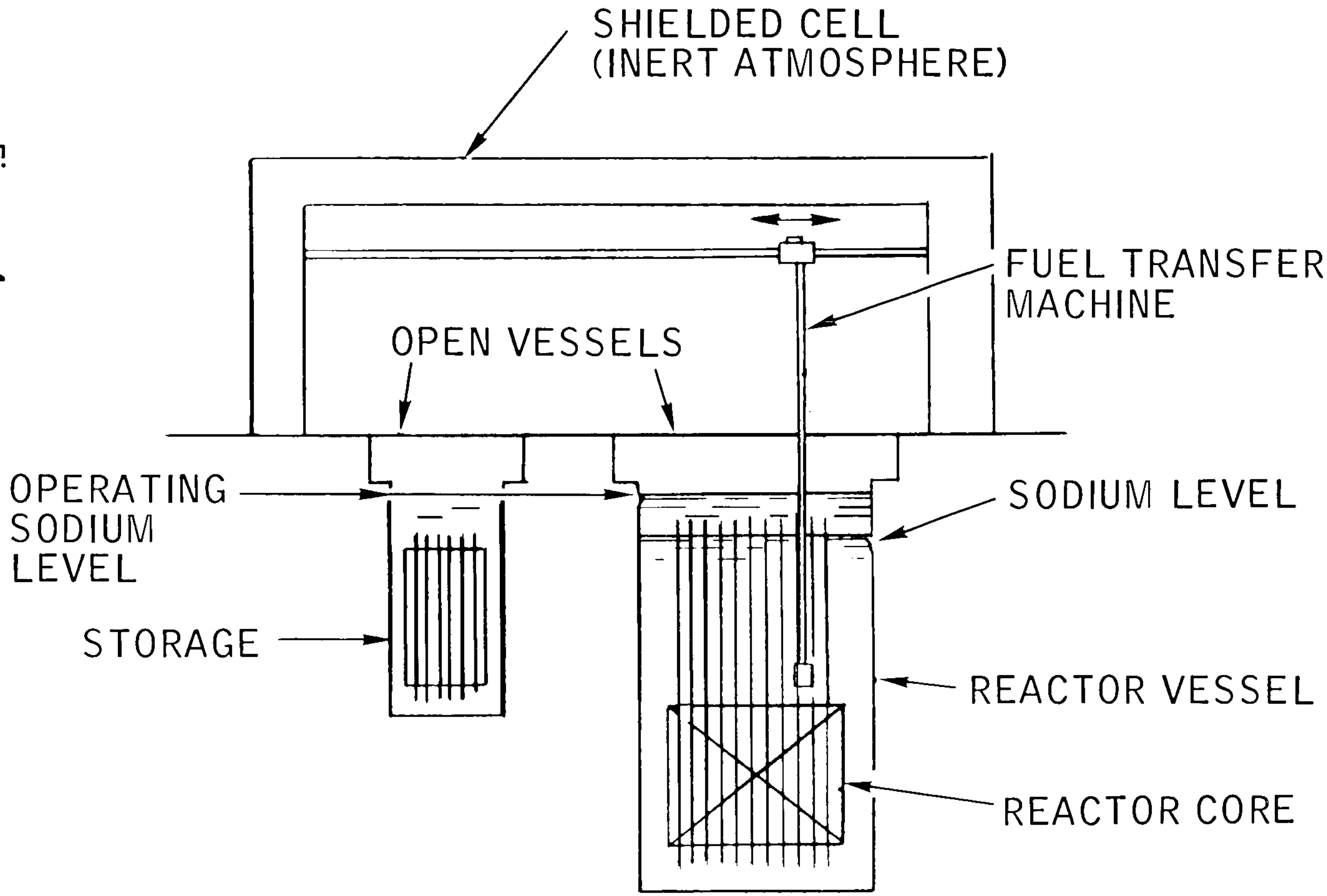

Figure 4

DOUBLE ROTATING PLUG WITH IN-VESSEL FUEL STORAGE

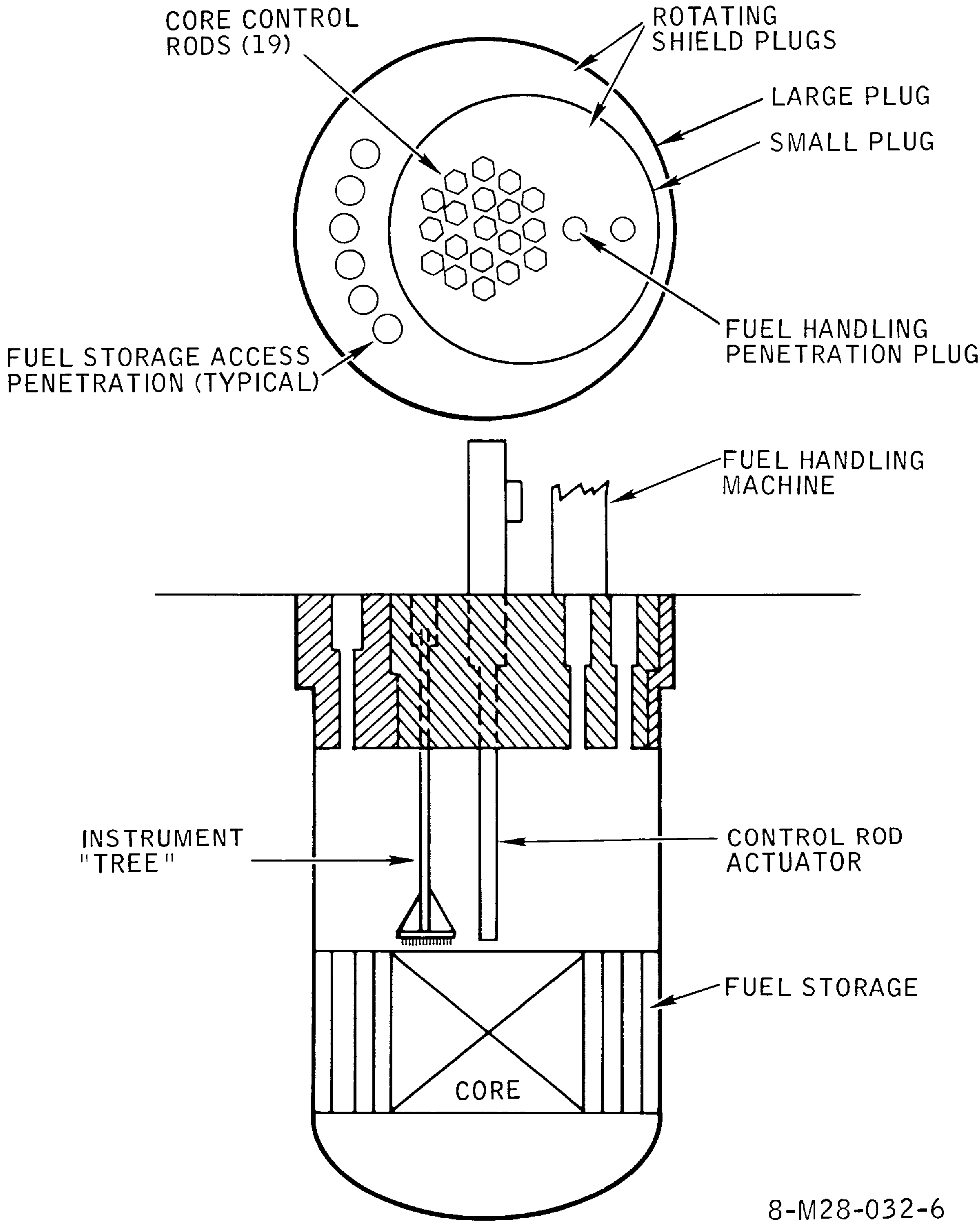

Figure 5

SINGLE ROTATING PLUG WITH ROTATING-ARM FUEL-HANDLING MACHINE AND IN-VESSEL FUEL STORAGE

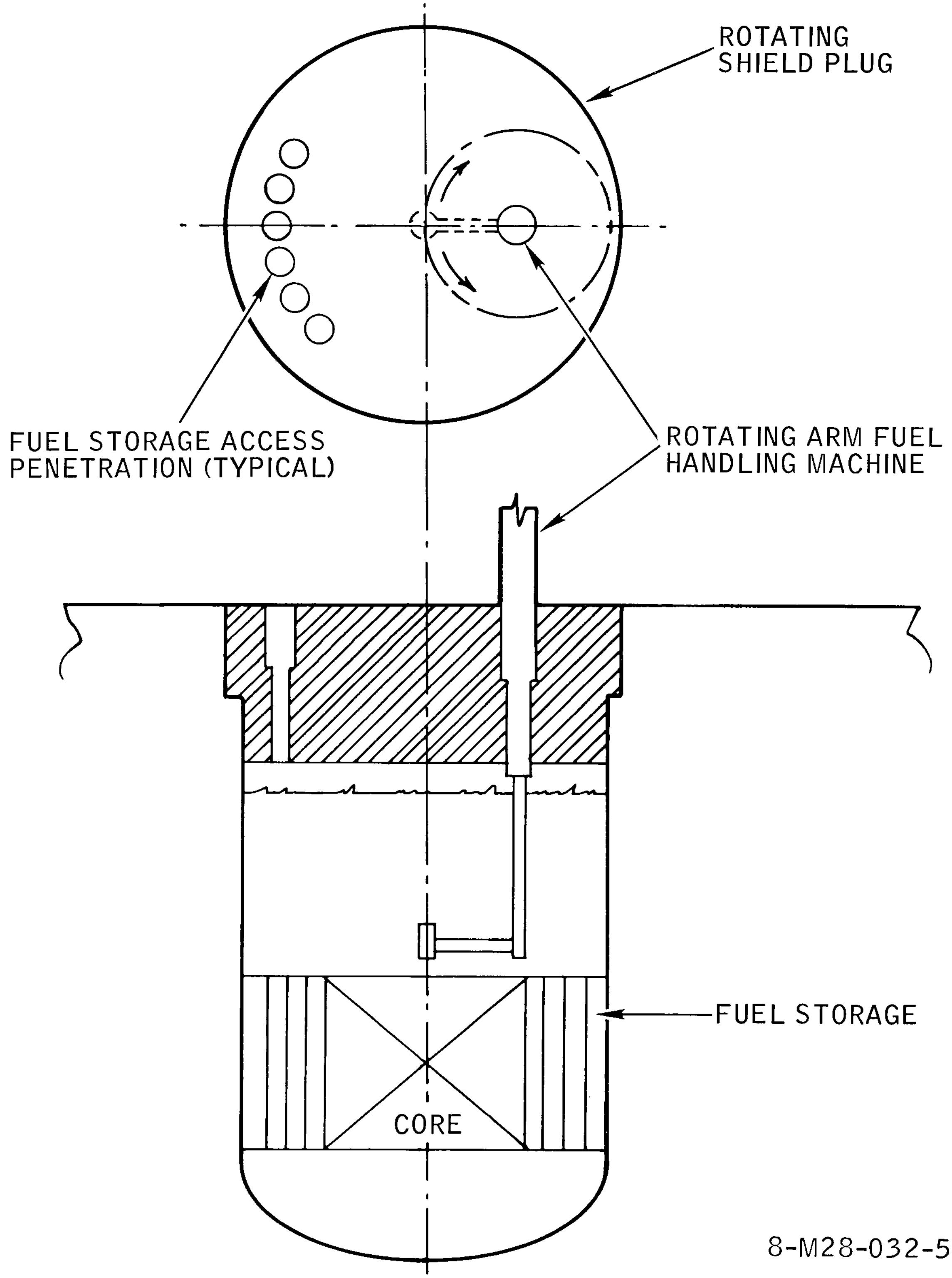

Figure 6

SINGLE ROTATING PLUG WITH A SINGLE NONROTATING ARTICULATING ARM FUEL-HANDLING MACHINE AND IN-VESSEL FUEL STORAGE

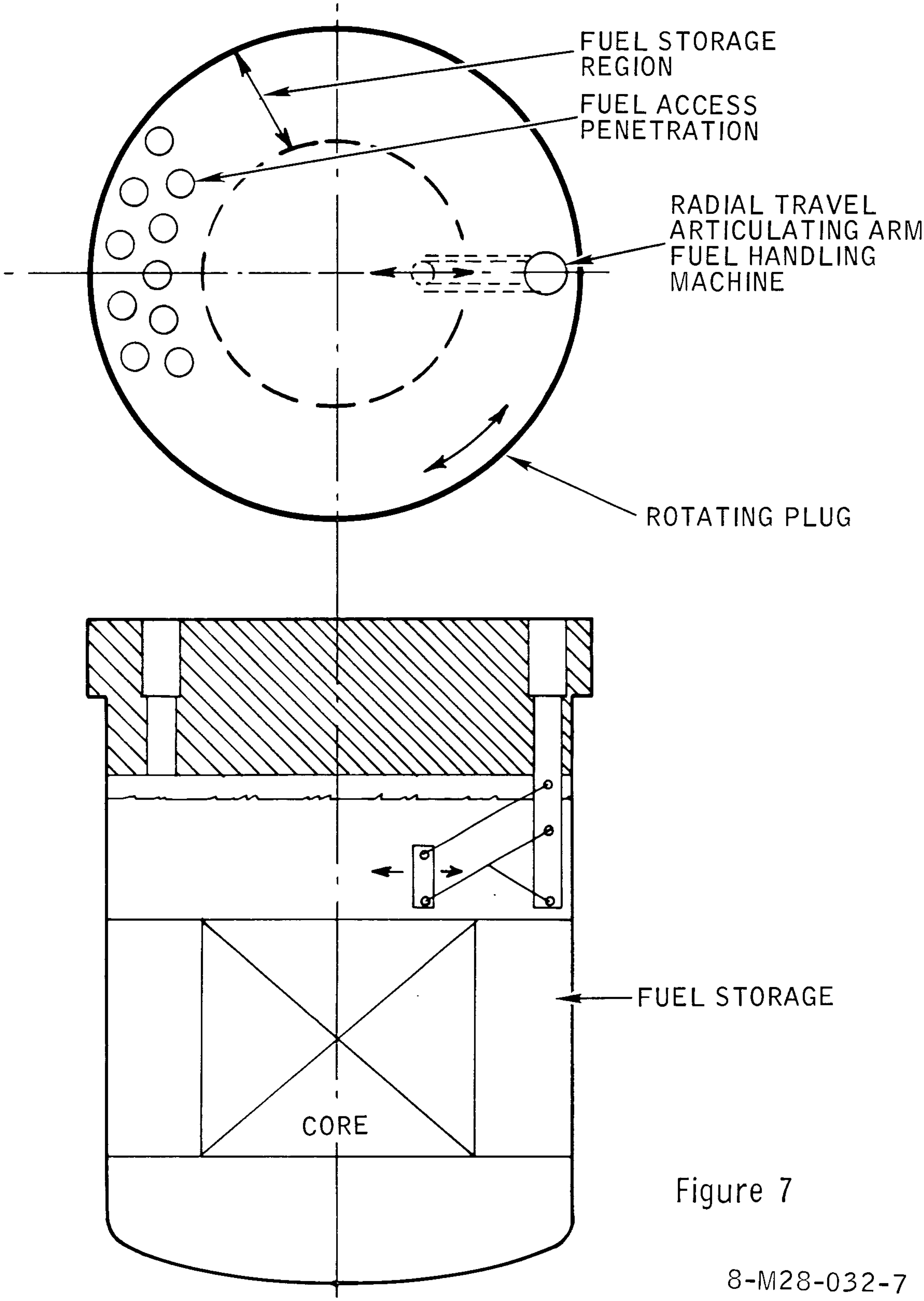

Figure 7

8-M28-032-7

handling mechanism operates only in the up-and-down direction. This concept is currently seriously being considered by AI, B&W, and the Germans. (The Germans add a third rotating plug to reduce vessel diameter.) The advantages of this concept follow.

- The concept is simple. All mechanisms are accessible for contact maintenance.
- Fuel handling is performed straight down through the top shield by using only push or pull movements.
- The in-core fuel handling machine requires minimum shielding.
- The control rod drives need not be removed from the reactor during fuel handling.
- Additional direct access to any core or storage position can be provided by additional ports in the smaller rotating plug.
- The indexing of the fuel handling machine to any core position can be verified by direct vision.
- There can be no loss of fuel element cooling during in-vessel transfer because the fuel element never leaves the sodium pool.
- Instrument "trees" need only be raised approximately 6 inches during reactor refueling. Instrument leads need not be disconnected.
- The rotating plug concept has been proven by the Dounreay, Fermi, and EBR-II reactors.

The disadvantages are:

- Fabrication and transportation of large rotatable plugs require special consideration,
- In-vessel storage increases the diameter of the reactor vessel (disadvantage to the "loop" concept only),
- Sodium frosting in the annuli of the rotating plugs may eventually cause plug binding, and
- A second fuel handling machine is required for ex-vessel fuel handling.

Figure 6 shows a single rotating plug with a rotating-arm, fuel-handling machine. This is the system used in the Fermi reactor and has been selected for the French Phenix reactor. The advantages of this concept resemble those for the double rotating plug concept.

- Only one rotating plug is required.
- There are no mechanical restrictions to the selection of a control rod pattern.
- There can be no loss of fuel element cooling during in-vessel transfer because an element never leaves the sodium pool.
- This concept has been successfully demonstrated by the Fermi reactor.

This concept has the following disadvantages in addition to those listed for the double-rotating plug:

- Direct through-the-plug access to each core position can not be provided,
- Control rod drives and some in-core instrumentation must be raised approximately 15 feet to the under side of the reactor top shield during fuel handling,
- Removal of the in-core handling machine from the reactor for maintenance is a major operation requiring reactor shutdown, and
- The azimuth position of the fuel handling machine cannot be verified by direct vision.

The third under-the-shield fuel handling system under consideration by reactor designers, Figure 7, is a single rotating plug with a nonrotating articulating arm. This also utilizes in-vessel storage of spent fuel. This concept is being incorporated into the British PFR, and although somewhat more complicated than either the double rotating plug or single rotating plug with rotating arm, has the following additional advantages over the latter.

- The control rod drives need not be removed from the reactor during fuel handling, only their actuating mechanisms need be raised approximately 6 inches.
- The in-vessel fuel handling machine is removed from the reactor after completing refueling, permitting inspection and maintenance between refuelings.

Particular disadvantages:

- Direct through-the-plug access to each core position cannot be provided,
- The radial position of the fuel handling machine grapple cannot be verified visually, and
- The articulating mechanism requires considerable development and testing.

Reference again to Table 1 shows that of the six designs which incorporate in-vessel storage for decay of spent fuel, three (AI, Phenix, and Na-2) plan to remove this fuel from storage for shipping only with the reactor shut down. This is basically a decision in the direction of safety, since removal of fuel from the vessel involves a penetration of a containment boundary. While this can be done in a manner which does not compromise safety significantly (e.g., the fuel transfer cell within the refueling hot-cell), the generalization can be made that a simpler design results if fuel is not removed from the reactor vessel while the reactor is in operation. This design simplification, however, can result in additional ex-core fuel inventory cost if the refueling interval is appreciably greater than the decay time required before fuel can be shipped. This effect is illustrated by the following example study for a vessel-loop type reactor with in-vessel storage of spent fuel and a nominal average burnup of 75 Mwd/kg at discharge after 18 months fuel residence in the core.

The refueling interval tradeoffs are listed in Table 2 for three refueling modes, one-sixth of the core every 3 months, one-third of the core every

TABLE 2

REFUELING INTERVAL TRADEOFFS[2]

Tradeoff Item	Refueling Interval Months		
	3	6	9
	Core Fraction Replaced		
	1/6	1/3	1/2
Capital Costs			
Vessel/Top Shield Size, ft	Base	+2	+4
Number Control Rods	Base	+2	+4
Fuel Cycle			
Fuel Burnup, Mwd/kg	Base	-4	-8
Breeding Gain	Base	-0.007	-0.014
Cooling Time, days (in storage)	Base	+90	+180
Processing Time, days	Base	+33	+66
Reprocessing Cleanup Time, days/yr	Base	-6	-8
Plant Availability			
Scheduled, %	Base	+1.3	+1.4
Unscheduled, %	Base	+1.0 (allowance)	+1.5 (allowance)
TG Maintenance	2 wk/yr	2 wk/yr	3 wk/18 mo
Refueling Time, days	3 to 5	6 to 8	9 to 11

6 months, and one-half of the core every 9 months. Capital cost is increased by the additional in-vessel storage space and by the additional control rod requirement for excess reactivity. Fuel cycle costs are affected by the reduction in average burnup as larger fractions of the core are replaced, loss in breeding ratio due to additional control rods, time lost due to longer in-vessel storage, increased reprocessing time, and saving of reprocessing plant cleanup time for larger batches. Plant availability is increased because of shutdowns and startups, and by reducing equipment difficulties which accrue, due to startup and shutdown cycles.

Assignment costs to the tradeoff factors as shown in Table 3, show that the magnitude of savings is in favor of the shorter refueling periods, the largest single factor being ex-core inventory costs. This trade study does not include the added cost of shipping casks for shipping the larger batches at less frequent intervals.

In the preceding study, it must be recognized that neither a 9-month nor a 3-month refueling interval is desirable from a utility standpoint; these intervals, therefore, are selected to show the cost trend, not to be proposed as actual shutdown intervals. Monetarily, either a 6-month or a 12-month refueling interval is acceptable. A 6-month refueling interval introduces an energy cost penalty of approximately 0.03 mill/kwh when compared with a 3-month refueling interval if the fuel is not removed from in-vessel decay storage during operation.

CONCLUSION

It is evident from the foregoing discussion that an optimum refueling system for LMFBR's will not result from design studies now underway, but from a combination of design, development, and operating experience in the demonstration plants and probably during the first round of power plants beyond these demonstrations. It is encouraging that so many apparently feasible fuel handling systems and mechanisms can be subjected to the stringent test of actual operation in a fast breeder reactor plant.

The refueling designs proposed for the first round of demonstration plants in the USA can be characterized as a cautious extrapolation of existing technology. These designs (hot-cell and double rotating plug concepts) utilize a minimum of mechanisms that operate submerged in sodium, to minimize development costs and risks.

Information on the performance of mechanisms in sodium will become available as a result of AEC sponsored development work and experience with operating liquid-metal cooled reactors. As a result, the designs of refueling systems for LMFBR's will progress to greater use of submerged mechanisms to speed up refueling, and hopefully cut plant capital cost.

The reliability of the various refueling systems discussed in this paper is thus far a matter of speculation; high reliability of the various mechanisms is necessary to permit refueling within the target time period. Mechanisms of both known and unknown performance require over-testing to establish limits of performance. When the limits are known, the design margins can be selected such that the reliability of the mechanism is predictable with a high level of confidence. Key items requiring development are the grippers, seals, bearings, guides, and the selection of materials of construction to withstand service in liquid-metal.

In parallel with development testing, product assurance methods must be developed which embrace the entire engineering, fabrication, and testing

TABLE 3

REFUELING INTERVAL vs COST,(2) 10^3 $

Cost Item	Interval 3 mo	6 mo	9 mo
Capital Cost			
Vessel/Top Shield	Base	500	1,100
Number of Control Rods	Base	100	200
Subtotal	Base	600	1,300
Fuel Cycle*			
Fuel Burnup	Base	1,300	2,500
Breeding Gain	Base	500	1,000
Ex-Core Inventory	Base	3,300	6,600
Reprocessing	Base	(1,600)	(2,200)
Subtotal	Base	3,500	7,900
Plant Availability			
Scheduled at 1 mill/kwh	Base	(830)	(900)
Unscheduled at 2 mills/kwh	Base	(1,270)	(1,900)
Subtotal	Base	(2,100)	(2,800)
Total	Base	2,000	6,400
Energy Cost mill/kwh	Base	+0.03	+0.09

*Present worth of 15 years at 7% interest process. These methods must develop and audit the use of disciplined procedures, codes, and standards necessary to establish reproducibility of successful components for industrial use.

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LMFBR SYSTEM AND COMPONENT TEST FACILITIES*

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ABSTRACT

The need for component testing in the LMFBR program is defined by the lack of evolutionary experience and the highly accelerated requirements for reliable components. Partial system testing is a useful and almost inevitable by-product of component testing for full-size components. The gross test conditions of maximum temperature, pressure, and rate of change and temperature transients have been tentatively established and are presented. Necessary engineering compromises are outlined, and those which cannot reasonably be compromised without derogating the value of the program are identified. It is concluded that component testing and the concomitant system testing in large LMFBR test facilities are necessary, and that the results of design and operation of these facilities are of value to the LMFBR community in addition to that gained by test and evaluation of the test articles themselves.

INTRODUCTION

To discuss facilities for testing of LMFBR components and systems, it is necessary to discuss:

1) What purposes are served by testing
2) The test conditions necessary to demonstrate performance
3) What test compromises are required and acceptable.

This paper will address itself to the general problems of LMFBR test facilities, rather than catalog the existing and proposed facilities. Existing facilities have been adequately described, albeit in scattered documents; proposed facilities are, with few exceptions, not sufficiently defined to make their public discussion of significant value now.

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Need and Purpose of Testing

Sodium component testing is necessary primarily because there is not a long evolutionary history of development, with rather small incremental changes to improve reliability and capability, as is the case in most conventional and engineering systems. A 1000-Mwe fossil-fired plant built today employs components which have been under development for literally hundreds of years in one form or another. Thus, there is quite a backlog of successful (and unsuccessful) experience on which to draw. While much of this recently developed technology (including industrial sodium usage dating back to the early 1900's) has been useful in the selection of development paths for sodium systems and components, it may be illuminating to recall that the prototype of the 1000-Mwe FBR of the 1970's was a 300-kwe system first operated in 1951. Sodium technology has thus required an accelerated growth in capacity and capability, as temperatures and flows have increased from EBR-I to the confidently projected commercial fast breeders, which must have "power plant availability." Some operating experience in moderately large systems has been obtained up to Hallam and Fermi sizes (75 to 100 Mwe); component capability is now required at 1000 Mwe and up to 200°F higher temperatures than those plants.

Added to the difficulties inherent in rapid expansion of technical capabilities is the fact that sodium is not "just another fluid." The temperature ranges over which components must operate both in startup and in casualty conditions induce engineering problems which stretch traditional approaches. The rapidity with which temperature and flow conditions may change, and the excellent heat transfer properties of sodium, introduce unfamiliar problems when extrapolating experience from similar components operating with other fluids. Some of these problems are simply not amenable to exact and optimized solutions; a designer must then fall back on safety margins and engineering conservatism in lieu of experience. Accelerated testing must provide this experience for the next generation of fast breeder reactors, which are to operate reliably and must have reasonable capital costs. Testing, therefore, has at least two purposes: provision of confidence in the (successful) designs tested, so that they may be immediately applied to the LMFBR Program, and the furnishing of design data and operational experience for the performance and cost optimization of these components in something less than the time which has been available for more conventional power plants.

The LMFBR systems are perhaps unique in that component development requirements are being set by overall plant requirements, rather than vice versa. The size and temperature capabilities of fossil plants followed developments in components and materials to a large degree, although the economic benefits of improvements perhaps provided the economic impetus for accomplishing such development. In the case of FBR's, the capacity and temperature capabilities of the components are set in advance, by the system designers and optimizers, so that the component developer and supplier can no longer proceed pretty much at his own pace in making incremental improvements over a span of decades. Thus, component development is being forced into unfamiliar technical territory, rather than leading the way gradually into these areas.

A good test bed for components might be a reactor system embodying all the parameters thought to be required, making changes and alterations as engineering and tests indicate, and accepting the probability that at least some of these high-investment plants may be complete failures. This is impractical because of the investment required and the time scale upon which it is now apparent FBR's must be developed to serve their purpose in the power and fuel economies of the U.S. Consequently, it will be necessary, in many cases, to compromise the full-scale integrated testing of a component by performing tests of individual components and utilizing engineering judgment to synthesize the results of these tests into a performance prediction of the total system. Typical of the compromises one must accept are the omission of radioactivity in primary system component testing and the necessity for modeling steam generators as the facility and operating costs of integrated tests approaches that of the eventual power plant itself. Each component to be tested must be examined for its critical unknowns, and the test program and facility design must be adjusted to satisfy the component requirements in order of priority derived from its eventual use in the power plant. For instance, a full-scale test of steam generator heat exchange surface may not be necessary, but a substantially full-scale test of critical tubesheet configurations may well be required. In all of these compromises, judgment and application of the relatively small amount of experience obtained to date must be utilized; in many cases the technical risks can only be minimized and cannot be completely eliminated. One salient point in applying judgment is to test in the largest scale practicable, and under the most prototypical conditions that can be engineered into test facilities within technical and financial constraints.

Test Conditions

To plan for required facilities, including modification of existing ones to meet today's demands, it is necessary that the conditions which will be mocked up by the test facilities be established. Current requirements have been surveyed, utilizing all the information available at present. The gross test conditions are tabulated in Table 1; it should be noted that these are tentative and subject to change as detailed design and optimization of LMFBR plants continues. They do, however, represent a "first cut" as a basis for planning, with the exception of instrumentation test facilities. Instrumentation has been deliberately omitted, as it should be the subject of a paper by itself. Each of the components listed will require additional, detailed, test conditions; for instance, valves must be tested for seat leakage; opening and closing torque requirements; steam seal performance; operability before, after, and during temperature changes; vibration; and flow characteristics. It is evident, however, from the tentative requirements identified so far, that the liquid metal component testing facilities required are unique, and with few exceptions, are unavailable today to perform the broad spectrum of testing and evaluation which is necessary to assure reliable operation of the LMFBR power generating plants. From this summary, which is considered indicative of the capabilities required of LMFBR test facilities, although certainly not definitive at this time, it is apparent that a substantial number of unique facilities must be made available to perform the testing and evaluation of components in the depth required to assure required performance and availability of the complete LMFBR power plant system.

Test Conditions and Compromises

Having set as a goal of the component development program the demonstration of reliable individual components and systems, and having at least tentatively identified the gross conditions under which these components and systems must operate, the means for accomplishing this goal in the absence of the complete assemblage of components and systems into a power plant must be examined.

It is evident that the effects of primary sodium gamma radiation cannot be demonstrated easily in the absence of a reactor. Therefore, those portions of components which might be expected to be affected by radiation must be tested for this effect separately, as well as under nonradioactive system conditions. Fortunately, there do not seem to be many of these subcomponents which are absolutely required, and the component designer must be made fully aware of their limitations before irrevocably including them. A complete design review of the component or system of components must be made to establish any potential

TABLE 1

TENTATIVE SODIUM COMPONENT TEST REQUIREMENTS
(except instrumentation)
(Sheet 1 of 3)

Temperature (°F)	Pressure (psig)	Flow (gpm)	Thermal Transients
1. VALVES			
Sodium to 1210 (design)	To 225 (design)	0 to 1000 (service systems) 0 to 20,000 (FFTF main loops, LMFBR service systems) 20,000 to 120,000 (LMFBR main heat transfer loops)	1) 10°F/sec for 30 sec from 800 to 1100°F 2) 10°F/sec for 30 sec from 1200 to 900°F 3) 4°F/sec for 120 sec from 600 to 1080°F 4) 4°F/sec for 120 sec from 1200 to 720°F
2. STEAM GENERATORS			
Sodium side — to 1200 Steam side — to 1050	Sodium side — to 225 Steam side — to 2500	1) Test steam generators (~30 Mwt) ~2700 sodium 2) LMFBR prototypes (30 to 50 Mwt) 3000 to 5000 sodium	Required thermal transients have not been established for all steam generators. Tentative requirements are: 1) -35°F/sec for 3 sec, change from 1175 to 775°F in 14 sec 2) +50°F/sec for 3 sec, change from 775°F to 1175°F in 9 sec 3) -35°F/sec for 3 sec, change from 1175 to 650°F in 16 sec
3. SODIUM SAMPLER			
To 1200	To 225 where applicable (in sodium) To 50 (in cover gas)	Applicable only to in-line samplers	Applicable only for in-line samplers with mechanical joints
4. SODIUM PIPING SYSTEMS			
To 1210 (design)	To 225 (design)	Fluid velocities to 40 ft/sec, sizes: 1) Greater than 30 in. (LMFBR main loop) 2) Less than 30 in. (LMFBR service loop)	1) 10°F/sec for 30 sec from 800 to 1100°F 2) 10°F/sec for 30 sec from 1200 to 900°F 3) 4°F/sec for 120 sec from 600 to 1080°F 4) 4°F/sec for 120 sec from 1200 to 720°F

TABLE 1

TENTATIVE SODIUM COMPONENT TEST REQUIREMENTS
(except instrumentation)
(Sheet 2 of 3)

Temperature (°F)	Pressure (psig)	Flow (gpm)	Thermal Transients
5. COOLER (sodium to air)			
To 1000	To 150 (FFTF module)	To 3000 (FFTF module)	Information not available
6. COOLANT MIXING TEE			
To 1200 ΔT up to 600	To 225	Unknown	Information not available
7. EXPANSION JOINTS			
Sodium to 1200	0 to 225	Not applicable	1) 10°F/sec for 30 sec from 800 to 1100°F 2) 10°F/sec for 30 sec from 1200 to 900°F 3) 4°F/sec for 120 sec from 600 to 1080°F 4) 4°F/sec for 120 sec from 1200 to 720°F
8. PIPE AND VESSEL PREHEAT SYSTEMS			
Ambient to 350	Not applicable	Not applicable	Not applicable
9. FREEZE TRAPS			
Sodium to 1200 Air to 150	To 225	Not applicable	Not applicable
10. VAPOR TRAPS			
To 1200 vapor	To 50 cover gas pressure	Not applicable (vapor only)	Not applicable
11. HOT TRAP (either carbon or oxygen)			
To 1300	To 225	To 100 at 1300°F To 250 at 1200°F	Applicable for traps utilizing mechanical joints
12. COLD TRAP			
Sodium to 1200	To 225	0 to 250	Applicable only if mechanical joints are used to connect the trap to the system

TABLE 1

TENTATIVE SODIUM COMPONENT TEST REQUIREMENTS
(except instrumentation)
(Sheet 3 of 3)

Temperature (°F)	Pressure (psig)	Flow (gpm)	Thermal Transients
13. HEATERS (electrical)			
To 1300 in sodium; to 1500 in air	To 225 for submersion type	~600 for FFTF closed-loop heater	Not available
14. SODIUM PUMP SHAFT SEALS			
Sodium vapor to 1200	To 50 (cover gas)	Not applicable	Not applicable
15. INTERMEDIATE HEAT EXCHANGERS (conventional or pot type)			
To 1200 in sodium; LMΔT to 200	To 225	1) 0 to 1000 (FFTF closed loop) 2) 0 to 13,000 (FFTF main loop and LMFBR model)	1) 10°F/sec for 30 sec from 800 to 1100°F 2) 10°F/sec for 30 sec from 1200 to 900°F 3) 4°F/sec for 120 sec from 600 to 1080°F 4) 4°F/sec for 120 sec from 1200 to 720°F 5) Lesser transients as required
16. PUMPS			
To 1200	To 225	1) 0 to 3000 (small service pumps) 2) 3000 to 20,000 (FFTF main loop or LMFBR service) 3) 20,000 to 120,000 (LMFBR main loop)	1) 10°F/sec for 30 sec from 800 to 1100°F 2) 10°F/sec for 30 sec from 1200 to 900°F 3) 4°F/sec for 120 sec from 600 to 1080°F 4) 4°F/sec for 120 sec from 1200 to 720°F 5) ±100°F/hr from 70 to 1200°F
17. SODIUM PUMP BEARINGS (sodium test)			
To 1200	To 120	0 to 7000 (tentative estimate)	1) 10°F/sec for 30 sec from 800 to 1100°F 2) 10°F/sec for 30 sec from 1200 to 900°F 3) 4°F/sec for 120 sec from 600 to 1080°F 4) 4°F/sec for 120 sec from 1200 to 720°F

sources of radiation effects, and a plan to test for these potential effects using radioactive facilities must be made. Engineering analysis is necessary to determine whether the synthesis of these separate tests is sufficient to provide the required assurance of reliability.

Physical size of components necessitates a compromise in some cases, primarily in heat exchange equipment. Mocking up an 800-Mwt steam generator system may well be possible, but its practicality in terms of schedule and money is certainly questionable even without exhaustive analysis. A full-size IHX is closer to the realm of practicality, as regenerative heating may be used, but the expense and time involved in even this facility (which should incorporate a steam generator to more closely duplicate the real transients and transfer functions of a steam generating system) appears almost prohibitive at this time. Compromises in the "full-scale testing" principle are clearly in order; under the foreseeable circumstances of time and money, it appears that model tests are the only practical solution. Scale models are, however, completely inadequate. Critical areas in the design, and those not amenable to reasonably exact design, must be identified in the detailed design of a full-scale component before embarking on a model design. This means that a very substantial design effort should be made on the component, as if it were the final article, if the model tests are to be meaningful. "Cartoons" will not suffice to identify the subtle engineering areas where accurate techniques and data do not exist to the degree necessary to provide confidence. Once these areas are defined as well as possible, a model can be constructed intelligently to provide the missing information or confirmation, and no one should be distressed because it looks, perhaps, grotesque compared to the full-scale design.

Thermal transients, and the features to control their effects on components and systems, admit little compromise in test facilities. Sodium system thermal transients are almost unique in their magnitude and in their effects on the system. Because of this singularity, there is a corresponding lack of data and experience from other engineering systems to apply to sodium components and system design. Therefore, testing for confirmation of this imperfectly understood design feature is vitally necessary and the test apparatus must reproduce predicted thermal transients as accurately as possible. An interesting corollary exists; as the test apparatus is presumably built to test more than one component or system as part of the development program, it must sustain more thermal transients than any one test article. This makes for interesting problems in the design of facilities, including recognition of the possibility of selective replacement of parts of the test system which may have deteriorated as a result of thermal abuse. In fact, a planned surveillance program over the test facilities may provide accelerated life test data on the behavior of materials and components in real LMFBR systems as a "fringe benefit" to test and evaluation operation.

Flow and pressure and temperature conditions do not generally lend themselves to compromise, as acceptance of the "full-size" principle for testing automatically provides a full flow and pressure requirement. The hydraulic and stress conditions must be thoroughly investigated to ensure that difficult-to-predict vibrations, cavitations, and flow disturbances do not jeopardize the system reliability. Temperature, too, should simulate that predicted, since in the ranges of interest (700 to 1200°F) temperature has a significant and increasing effect on the strength and elasticity of materials, and a satisfactory component at 900°F may well behave mechanically differently at 1200°F. This is particularly true if the indication of lifetime performance is desired, since corrosion and erosion effects may also be deceiving at lower than typical operating temperatures.

SUMMARY

To provide maximum assurance that LMFBR power plants have reliability and availability comparable to conventional plants, in the face of time schedules which do not permit incremental advances and a shortage of basic technology and experience at the system conditions required, it is required to test components under the most prototypical conditions possible. These conditions have been reasonably well identified; the facilities to test components and systems for LMFBR application do not, with a few major exceptions, exist. These facilities, which are costly both in construction and operation, must be chosen carefully, and their design pursued with minimum compromise so that the end result of a planned component development program will produce the goal of components and systems which will perform reliably in an LMFBR plant. These test facilities, in which the design solutions to novel LMFBR problems must be achieved in advance of tackling them in LMFBR plants, can provide design and performance data beyond that required for their basic function, if properly planned. Thus, the test facilities must be regarded as more than "rigs," but as pre-prototypes, engineering mockups, and learning tools in the rapid development of LMFBR plants.

DISCUSSION

D.A. Minner (Babcock & Wilcox) - Mr. Dickinson's paper is a clear exposition of what is now being done toward testing of components for sodium reactor systems. He points out that the LMEC installation is not intended to incorporate irradiation testing. He also makes the important point that some substantial parts of such testing as is to be done at LMEC are limited, in the practical sense, to model techniques.

It seems to me that it is from these two prime areas of restraint that we must take departure in assessing whether or not this approach is enough to give us the liquid metal fast breeder reactor in commercially saleable form.

Current designs for the LMFBR display both the pot-type and the piped-type primary system layout. There may be significant irradiation exposure service requirements for 30-year lifetime components in the two systems if one is to minimize initial system costs. Can we draw conclusions, for example, from component tests of IHX units and primary coolant pump units without irradiation exposure that will be truly valid for any version of the pot concept? Or are we to be forced, because of test capability limitations, to convergence on a particular system layout minimizing component irradiation? The penalty attached to such convergence would be initial denial to the reactor users of the singular advantages of a potentially less expensive system simply because our "proofing" capability cannot reach to its essentials.

I feel Mr. Dickinson's paper to be eminently sound in pointing out so forcefully that very superior engineering judgment will be required to tie together irradiation tests and the tests at LMEC. Will the regulatory and licensing people be prepared to accept such judgments, however, and to what degree? This could be a significant cost factor in early breeder plants tending to discourage utilities from accepting the risks in building these plants on a timely basis.

As a second point, it might well be appropriate to attempt to factor into component test planning a more concise evaluation of safety criteria and the potential attitudes of the AEC in its regulatory capacity. I would suggest that this should not be done simply by manufacturers' and LMEC staff's "crystal ball" techniques, but rather that it requires the deliberate and time consuming participation of experienced personnel in the AEC's regulatory staff. We are learning today, at some substantial cost, that what is perfectly satisfactory to us as designers and manufacturers is not necessarily so acceptable to the people who have to bear the burden of issuing licenses for power reactors. It is not always fair for us to say, "They ask too much!"; we, in the breeder business, must face up to the character of their responsibility. I think securing their help from the outset might be productive.

Thirdly, I think we have to consider most carefully whether the full scale techniques themselves, or the model techniques where used, will give us enough to have the requisite competitive design freedom. We all know that there are engineers who will permit neither interpolation nor extrapolation relative to test results. If we let ourselves be put into a non-extrapolation posture on LMFBR components, the utilities will not have a choice of designs nor will they see competition; they will have only a choice of name to put in the preamble to a contract. I submit that it is an objective of the LMFBR program to turn now to determinations of how much latitude a component test will secure for us in future designs. This means to me a deliberate application of statistical theory techniques so as to increase the range of usefulness of a given series of tests. Such statistical analysis ought to have the benefit of objective advice from the Commission's regulatory staff so as to achieve, insofar as possible, prior agreement upon the acceptability of the conclusions.

Lastly, it appears that we deem our mission to be primarily developmental testing of components. If all our capabilities in test facilities are to be concentrated in large government facilities, where are we to find capability for proof testing of actual reactor plant components? One has to agree that test facilities in this business are very expensive; but it does seem that we should be giving thought to the very practical business of quality and performance assurance requirements inherent in a plant construction contract.

F.A. Smith (Argonne National Laboratory) - Mr. Dickinson's paper is a clear, understandable assessment of the present requirement for sodium component testing. Two "cliche posters" located in my office state "DO SOMETHING - LEAD, FOLLOW, OR GET OUT OF THE WAY," and "DO SOMETHING - EVEN IF ITS WRONG." I believe in, and agree with, the compelling need to test liquid metals systems and components.

Mr. Dickinson's concern for active construction and testing of plant-sized components has been presented a little differently by Admiral Rickover. In a March, 1968, JCAE hearing publication, "Naval Nuclear Propulsion Program - 1967-1968," Admiral Rickover states, p. 61:

> "However, in the Navy, we now devote our time to making studies -- not building ships.
>
> All of these studies remind me of the story by Bret Harte in the poem, 'Caldwell of Springfield,' about the Reverend James Caldwell. On June 23, 1780, during the Battle of Springfield in the Revolutionary War, Reverend Caldwell's wife was killed by the British. When the local troops ran out of cannon wadding, the Reverend carried from his church an armful of hymnals by the theologian, Isaac Watts. He urged the troops: 'Now put Watts into 'em boys. Give 'em Watts!'
>
> In every war we have to fight with the weapons we have; I suppose in the next war, we'll be exhorting our sailors: 'Now put the studies into 'em boys. Give 'em studies!'
>
> This is one supply of ammunition that is inexhaustable!"

The possibility of the need for nuclear ships and the possibility of the need for civilian fast reactor power development focus on the need for sound applied engineering in both fields. Foreign governments have conducted fast reactor studies and are implementing their studies with both fast reactor projects and component test facilities. When the "battle" for fast reactor sales does materialize, the "winner" will be armed with data relative to power cost, plant performance, fuel performance, and maintenance costs of real plants. Sales of fast reactors will not be based on "studies" alone.

One fundamental problem area of component testing not discussed in Mr. Dickinson's paper is the subject of "time." Dickinson suggests that, "accelerated testing must provide this (safety) experience for the next generation of fast reactors." "Time" cannot be accelerated in engineering test.

Fuel and fuel clad materials may remain in a fast reactor for some time less than two years. Resident time is limited by fuel dimensional change, loss of reactivity, and fuel clad induced changes related to temperature, sodium, and the fission process. The reactor vessel and associated pipe must last considerably longer. I have seen much evidence of engineering mechanisms to refuel fast reactors; I have seen no evidence of machines to replace the primary vessel and piping.

I am much concerned about sodium reactor vessel and piping integrity. I do not see evidence of engineering experiments related to this problem. I do not know how to compress 15+ years of alloy fast reactor service (stress, irradiation, temperature, and sodium environment) into a time scale shorter than 15+ years. Fast reactor studies could extend sufficiently long to permit the development of 15+ years structural alloy data. This event demands that engineering environmental tests of a long time nature be established in the near future.

To conclude, I concur, with enthusiasm, with the last sentence of Dickinson's paper. This sentence says, "Thus, the test facilities must be regarded as more than "rigs" but as pre-prototypes, engineering mockups, and learning tools in the rapid development of LMFBR plants."

How do we implement Dickinson's excellent suggestion? How can maximum useful information be generated from R&D work? How do "engineers" obtain the required help of senior chemists, metallurgists, or solid state scientists? How, or what must be done to make recognizable the interrelated scientific aspects of complex structural alloys, the effects of sodium from 900°F-1400°F on alloys under engineering conditions, and the effects of time and mechanical and thermal stress? The real scientific complexity is even difficult to state, precisely, to other engineers. It is no wonder that there exists a lack of understanding of why a higher pirority needs to be established for "things" we call "engineering."

I propose that engineers concerned with the evolution of economic fast reactors "suggest," "push," "prod," "pry," "propose," "write," and "discuss" the need for project-oriented interdiscipline scientific research. Interdiscipline scientific research must be directed to engineering objectives if the sodium-cooled fast reactor is to successfully evolve.

I wish to express my appreciation to Mr. Dickinson and to the ANS, Southwestern Ohio Section, for the honor and opportunity to discuss this paper.

R.W. Dickinson - Mr. Minner has commented on radiation exposure of materials; I believe this may be a candidate for the "engineering analysis" part of testing, and that other papers this morning show increasing insight into radiation behavior which is most encouraging. Therefore, a synthesis of this understanding with non-radiation testing may be acceptable. His point regarding safety criteria and potential attitudes of regulatory bodies is well taken; it should be noted that both LMEC and other organizations are preparing standards and specifications for LMFBR's which should at least assist in the regulatory processes once they are agreed to and are adhered to. Mr. Minner's third point concerning testing of a multitude of designs is valid; every attempt will be made, certainly, by testing activities to derive engineering information through adequate instrumentation and test procedures for prototype components. As this body of information grows, departures from stereotyped components should be permissible. To test a wide variety of components of one function, simply for the sake of variety, strikes me as both expensive and unnecessary at this stage in the state-of-the art. Mr. Minner's last point, concerning prooftesting of actual components in other than large government facilities is a policy matter; my only comment is that the AEC's prototype program appears to offer the possibilities for the type of operational testing Mr. Minner recommends.

To Mr. Smith's comments concerning the necessity of continually bringing to management's awareness the necessity for testing and the difficulty of compressing time, I can only concur. While adequate analysis and study is certainly required, there is a point of diminishing returns in many programs, beyond which lack of information makes further study an exercise, and only acquisition of data, both good and bad, will serve to advance the state-of-the art.

SESSION V

April 4, 1968

REVIEW OF PROBLEM AREAS

Chairman: Paul F. Gast
LMFBR Program Office

Local Co-ordinator: James F. Weissenberg
U.S. Atomic Energy Commission

REVIEW OF FBR CORE DESIGN PROBLEM AREAS

by

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Introductory remarks prepared for the panel discussion: "Review of Problem Areas," American Nuclear Society National Topical Meeting on Fast Reactor Systems, Materials and Components, April 2-4, 1968, Cincinnati, Ohio

Since the focus of this meeting is on fast reactor materials, components, and systems, this discussion is not directly concerned with physics and safety aspects of FBR cores but rather is concerned with some of the other problems facing the core designer. Furthermore, since Dr. Gurinsky is discussing some of the particulars of materials problems and their solutions, it is not necessary to cover this aspect of the question. However, uncertainties about certain materials performance limitations are one of the important problems at the present time and the effect of these uncertainties on core design deserves some comment. In fact, there are important uncertainties about most of the materials and engineering limitations which are applied in the design of FBR cores. The following remarks are addressed principally to this problem area.

The early phases of core design are principally concerned with the establishment of the basic core configuration, namely its height and diameter, the size of the fuel pins and the fuel, coolant and structural materials volume fractions. There are several ways of going about this; one way that has proven convenient and effective is illustrated in simplified form in Table I.

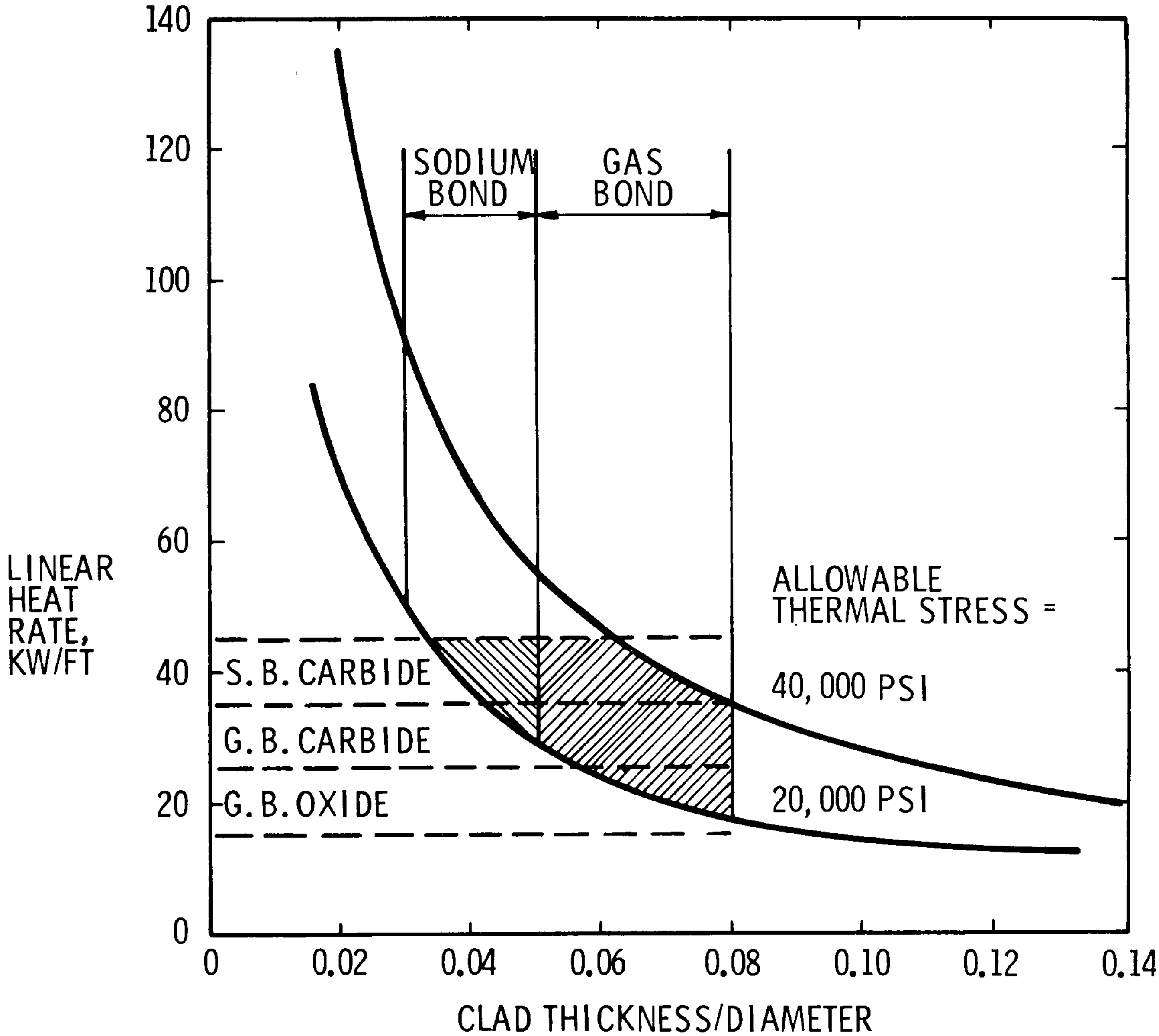
After establishing the type of FBR core to be studied (e.g., a 1000 Mwe sodium cooled, modular type with oxide fuel), an iterative procedure is followed in which a number of cores are evaluated in order to find the most favorable configuration. If total power is specified, a minimum of four additional free parameters must be fixed in order to characterize the core for the purpose of physics analysis. (For simplicity, blankets, control rods and moderating materials are omitted from this discussion; however, they obviously introduce additional degrees of freedom to the system. Their consideration complicates the procedure; however, the principle remains the same.) The basic free parameters may be considered to be the dimensions of the core and its volume fractions rather than the ones shown in Table I. This approach has its advantages; however, the alternate shown here is more powerful for several reasons. Within the context of this discussion, the most important of these reasons is that, right from the beginning, it forces consideration of the material and engineering limitations of the system as an integral part of the configuration selection procedure.

As an example, consider the stress situation for stainless steel clad. As shown in Figure 1, the clad thermal stresses are substantial, especially at the upper limits of what appears to be possible from the viewpoint of fuel temperatures and what may be necessary in clad thicknesses. In addition to the radial thermal stresses shown, there may be stresses due to fission gas pressure, circumferential thermal stresses and stress due to fuel-clad interaction. These stresses are not necessarily all additive; however, in order to reach the full potential of any of the fuel systems, it probably will be necessary to operate the clad in the grey area between the one and two times the yield stress. Since the radiation environment rather severely limits the ability of the clad to relieve stresses by plastic deformation, the designer has difficulty deciding just how high the clad stresses can be allowed to go. This uncertainty together with the uncertainty of clad thickness requirements leads to the sort of design problem referred to above; namely, an inability to establish a power rating for the fuel pin, a steel fraction and a core diameter which are consistent with the material and engineering limitations of the clad. This sort of problem leads to difficulties in attempting to establish the relative performance potential of various fuel systems, especially the gas-bonded carbide system.

TABLE 1
CORE DESIGN PROCEDURE

FBR TYPE	FREE PARAMETERS	MATERIAL AND ENGINEERING LIMITATIONS	REACTOR CHARACTERIZATION	REACTOR ANALYSIS	REACTOR PERFORMANCE
Fuel	Core Height	Fuel Conductivity, Density, Max. Temp., Burnup	Fuel Power Rating	Physics	Fuel Cycle Cost
Clad	Fuel Pin Dia.		Core Dia.		
Coolant		Clad Conductivity, Max. Temp., Corrosion, Stress.	Volume Fractions (Fuel, Steel, Coolant)	Thermal	Doppler
Power	Coolant Velocity			Costs	Void Effect
Core Arrgmt	Core Temp. Rise	Fuel Assembly Stress,	Inlet and Outlet Temp.		Pu Doubling Time
		Corrosion, Vibration, Pressure Drop.			Specific Inventory
		Hot Channel Factors			

Figure 1 LIMITS ON LINEAR HEAT RATE DUE TO THERMAL STRESS FOR STAINLESS CLAD



Another sort of design difficulty connected with uncertain limits is illustrated in Figure 2, which shows the results of fuel cycle cost calculations for a 1000 Mwe sodium bonded, mixed carbide core. Certainly, the strong general influence on fuel cycle costs of achievable burnup levels is well known and does not need elaboration upon. The problem of significance here is the effect of burnup on optimum seed height. Short cores are attractive because of good breeding performance and a favorable void effect; however, reprocessing and fabrication costs tend to be relatively high for such cores unless high burnups can be achieved. Uncertainty about achievable burnups (and also about reprocessing and fabrication costs) creates a dilemma for the designer as to the best choice for one of the basic core configuration parameters, namely its height. It would seem most appropriate to pick an intermediate height in order to minimize the fuel cycle cost sensitivity of the design to these uncertainties. However, it is certain that the breeding performance of the taller core will be less favorable, and it is equally certain that it will have a substantially higher void effect. Ordinarily, problems of this type cannot be resolved by clever design; the designer must either take a calculated risk based on the best information available, or if the risk is too large, delay his decision until more information is available.

One further example will serve to pinpoint another superficially simple but rather difficult to resolve design problem for fast reactor cores. Simply stated, the problem is: How close is too close for spacing of the fuel pins? Tightly packed cores mean high velocities and/or high coolant temperature rises. The effect of these parameters on the performance of a typical 1000 Mwe sodium bonded carbide core is illustrated in Figure 3. The core in this case is designed such that its steel fraction is not dependent upon pressure drop; therefore, the effect of changes in velocity and core temperature rise are equivalent, i.e., a given percentage change in either parameter has the same effect on sodium, fuel and steel fractions. A third characterization of the abscissa, fuel pin pitch to diameter ratios, could be added to this plot.

There is little doubt that, both from the viewpoints of breeding performance and fuel cycle costs, a substantial incentive exists to go to tightly packed cores. The void effect may or may not have an influence in this respect, depending upon what safety limits are established and depending upon what methods are employed to control the void effect. Generally speaking, however, the use of open cores (high sodium fraction) for reduction of the void effect is less efficient than alternate methods such as the addition of moderating materials.

There are many factors that need consideration and resolution in determining just how tight a fuel bundle can be made. These include the direct effects of velocity and core ΔT such as pressure drop, mass transfer rates, effects of vibrations, and thermal bowing as well as less direct effects such as uneven clad temperature distributions and changes in hot channel factors. The core for which the calculations summarized in Figure 3 were made has a fuel pin pitch to diameter ratio of 1.1 in the center range of the calculations ($\Delta T = 300°F$, $V = 32$ ft/sec). Dwyer's[1] theoretical studies of temperature distributions in such a tightly packed bundle without spacers indicate fuel pin circumferential temperature variations of the order of 100°F may exist. There is no doubt that

(1) O.E. Dwyer, Analytical Study of Heat Transfer to Liquid Metals Flowing In-Line through Closely Packed Rod Bundles, Nuclear Science and Engineering: 25, 343-358 (1966).

Figure 2 EFFECT OF AVERAGE DISCHARGE BURNUP ON FUEL CYCLE COSTS

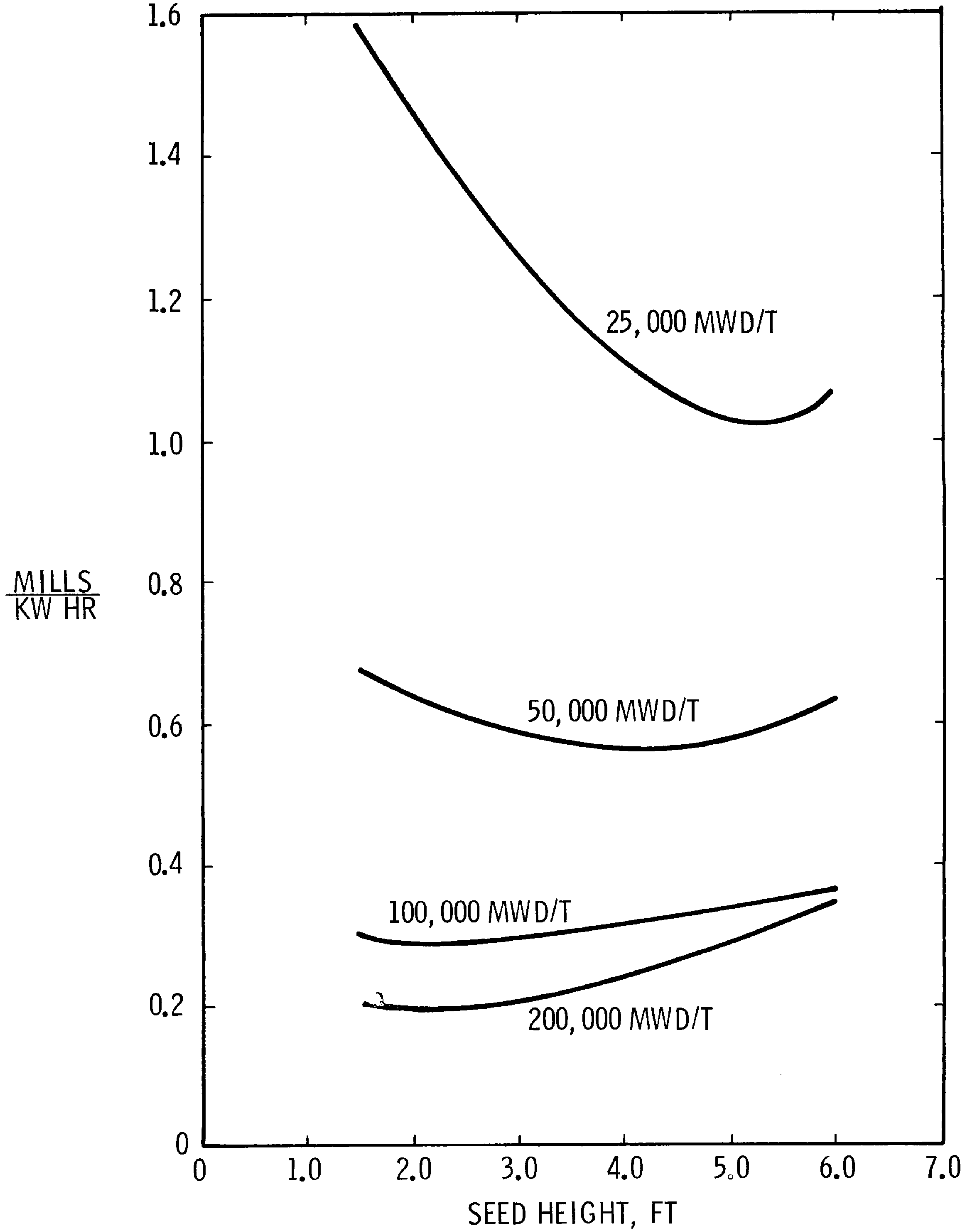
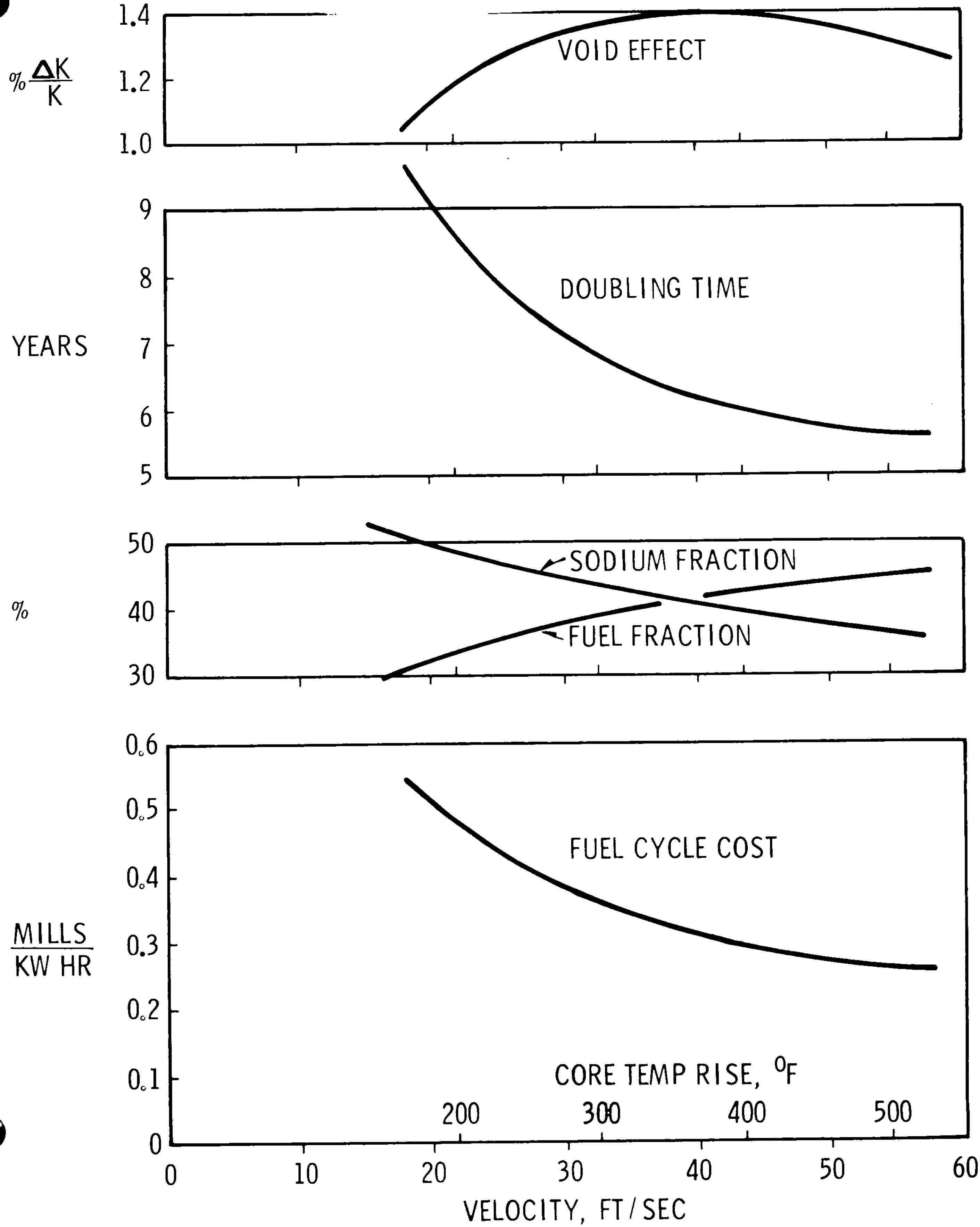


Figure 3 EFFECT OF PEAK DESIGN VELOCITY AND REACTOR ΔT ON REACTOR CHARACTERISTICS



properly designed spacers can reduce this effect; however, there is little information now available which indicates how such spacers should be designed. With respect to the problem of these temperature variations, there is a double uncertainty; i.e., considering the effect of spacers, how large a variation should be expected and, considering thermal stresses and other factors, how large a variation can be tolerated? Similar uncertainties exist with respect to other effects.

The material and engineering limitations needed for fast reactor cores must, of course, be related to potential failure mechanisms or, more exactly, to the prevention of failures. One reason that it has been difficult to establish a realistic set of limitations on these systems is the fact that it has been difficult to identify the kinds of failures which are likely to occur. In this respect, the fast reactor designer has a more difficult problem than the water reactor designer because there is no easy-to-identify failure mechanism analogous to burnout. Although it is not exactly a "failure", centerline melting is a clearly definable limit for oxide fueled reactors; however, carbide systems operate considerably below the melting point and apparently have rather "soft" limits on all the important parameters, i.e., maximum fuel temperature, maximum clad temperature, maximum coolant temperature, maximum heat flux, etc.

Clad failure resulting from fuel swelling is, of course, the most important performance limiting factor for FBR cores and the general character of the mechanisms involved has been pretty well defined. Appropriately, substantial development programs are being carried out to establish working limits to prevent failures of this type.

Unfortunately, however, development programs aimed at establishing failure mechanisms and appropriate working limits in the other areas indicated above are generally not so well advanced. Because of this, designers at the present time are often taking what may well be overly conservative positions which in turn may be resulting in unnecessary sacrifices of performance. This situation carries with it the danger that overly conservative designs will become standard for the industry and the realization of the true performance potential of FBRs will be unnecessarily delayed.

E

SOME MATERIALS PROBLEMS IN FAST REACTORS

by

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Introductory remarks prepared for the panel discussions: "Review of Problem Areas", American Nuclear Society National Topical Meeting on Fast Reactor Systems, Materials and Components, April 2-4, 1968, Cincinnati, Ohio.

SOME MATERIALS PROBLEMS IN FAST REACTORS

Experience in the operation of Na and NaK cooled reactors has shown that a number of materials problems have to be solved to implement the objectives of the Liquid Metal Fast Breeder Reactor Program. These problems and development tasks have been covered in the tomes of the ANL Program Office and the very excellent GE-NMPO Report entitled, "Evaluation of the Potential of Selected Alloys for Use as Fuel Cladding Material in an LMFBR."[1]

It is obvious that in the time available for this panel discussion that it will not be possible to cover many of the materials problems. What I plan to do, therefore, is to discuss a few very important problem areas which require work.

It is apparent again from experience that work has to be done on the materials for the steam generator. Decisions have to be made whether this component will be a ferritic steel, a Ni-base alloy or an 18-8 type stainless steel. If the choice is with the latter two alloys, the carbon transfer problem is minimized but mass transfer problems need further work. If the choice is with the low chrome ferritic steels, then more work needs to be done on the carbon transfer from ferritic to austenitic steels and specifically more work needs to be done on developing carbon stabilized alloys. In the above, the assumption is that stainless steels are used for vessel, piping, etc.

I do not foresee too many problems with respect to materials piping. Care will have to be used in the design to avoid thermal stresses; i.e., where cold and hot sodium streams meet.

This now brings us to the reactor proper, the core internals, the vessel, the fuel and the fuel cladding. Since the core internals and vessel will be subjected to essentially the same conditions as the fuel cladding, the comments which will be made on cladding are applicable to them.

It is my feeling that the problem of high burnup of 100,000 MWD/T for the fuel has to be solved by the "cut and try" method. The experience gained in the operation of the UO_2 fueled Light Water Reactors is the basis for selecting $(PuU)O_2$ as the reference fuel for FFTF. It seems reasonable to assume that with the acquisition of more radiation tests and the determination of some of the basic information on fuels such as swelling characteristics, plasticity, thermal conductivity, etc., it should be possible to engineer a fuel design which will approach the objective burnup. Selection of the fuel, whether it is $(PuU)O_2$ or (PuU)C or metallic fuel will depend on what type of fast reactor system is to be built; i.e., steam cooled, gas cooled, or Na cooled, the efficiency, and perhaps even the value of α. That not so constant ratio is again being questioned. Perhaps even the behavior of the cladding at high fluences may be a factor in the selection of the type of fuel.

[1] C.G. Collins, J. Moteff, and B.A. Chandler, "Evaluation of the Potential of Selected Alloys for use as a Fuel Cladding Material in an LMFBR", GEMP-573, General Electric Company, November, 1967.

Having disposed of all these "minor problems" I would like to discuss the really important problem - the cladding which is not really separable from the considerations for the selection and behavior of the fuel.

In LMFBR's the primary function of the cladding is to maintain the fuel in some preselected array so that heat can be safely generated and removed from the fuel. Safety implies that failure of a fuel cladding should not lead to catastrophic accidents such as meltdown and supercriticality. Since the economics for fast reactors require high burnup of the fuels, the cladding will have to withstand equally high exposure to fast neutrons, 5×10^{22} to 5×10^{23} ($E > 1$ Mev). A simple calculation shows that at these exposures each atom of the cladding will be knocked out of its equilibrium position at least 10-100 times. This is really a very rough treatment. In addition to the displacements, gaseous and solid transmutation products are formed as has been shown in the papers by Weir, Moteff and others. Concomitant with, or as a result of, the displacements and transmutation products, large voids are formed in materials irradiated to fluences $> 10^{22}$. These voids cannot be accounted for as being due to He or H production but must in fact be due to the absence of material.

The net result of subjecting materials to these fluences is that they suffer a serious loss in ductility and a reduction in density; i.e., the cladding swells. Since fuels at these high burnups are known to swell, stresses are produced in the cladding which if they cannot be relieved by plastic deformation or be taken up elastically will cause failure of the cladding. Are there solutions for this problem? I think the answer is yes. They are:

1. Minimize the stress imposed on the cladding by good design of the fuel element.
2. Vent the fission gases to a plenum or to the Na.
3. Provide reasonable cold clearance between clad and pellet.
4. Select proper density for pellet and for vibratory compacted fuel.
5. Improve the ductility of the cladding by alloying additions such as the Ti prescribed by ORNL.
6. Reduce the agglomeration of voids by adjusting the microstructure and second phase distribution.
7. Investigate and try to understand why the body centered cubic structures such as the V alloys appear to be less susceptible to loss of ductility on irradiation.

There may be other ways of minimizing the stress on the cladding, namely, by using sodium bonded fuels. In principle it may be possible to select dimensions of the fuel which swell to the inner dimensions of the clad as burnup progresses. This type of approach will require extensive testing of the fuel clad assembly to determine the feasibility of this concept.

There is one other idea that I think needs looking into. It may turn out to be feasible to radiation anneal the cladding. Such experiments have not been undertaken but seem to be at least interesting and may have some practicality.

What I am trying to say is that in addition to the cut and try engineering experiments which are so popular these days, we need some good searching basic work on these very difficult problems. This need has been recognized by the Program Office, the GEMP report, and nearly all the materials people. While I recognize that the solutions to engineering problems rarely come out of basic work directly, I also know that without the basic work "we" lack the background knowledge which is factored into intuitive, imaginative, empirical solutions to the problems.

I would also like to say something about the corrosion of materials by sodium. A good deal of work still needs to be done in this area to determine the effects of temperature, temperature difference, heat flux, oxygen, carbon, flow rate, Na chemistry, etc., on the corrosion process. I would agree with Mr. Goldmann that if reactors are run with maximum Na temperature of 800-900°F, the development work on materials would be minimal. The presently available data show that stainless steels, cobalt and the vanadium alloys in very low oxygen (< 5 ppm) display good resistance to corrosion by Na at 1200°F (~ 1 mil/yr). It will be necessary to maintain very low oxygen levels to maintain these satisfactory corrosion rates. Systems will therefore have to be well cold trapped, hot trapped, and tight. This is particularly true for the vanadium alloys. The oxygen level for safe use of these alloys may actually be much lower than the 5 ppm indicated. If the oxygen levels cannot be readily maintained below 10 ppm, it will be necessary to consider corrosion resistant coatings on vanadium alloys or to consider use of alloys which are more tolerant to higher oxygen. It certainly also seems desirable at this stage of development to study the effect of soluble deoxidants such as Mg which is a transmutation product of neutrons on sodium and to evaluate the usefulness of Li additions to reduce the oxygen activity.

These introductory statements are not meant to answer the many materials problems but rather to lay the bases for the discussion which will follow.

IRRADIATION TESTING FACILITIES: NEEDS, LIMITATIONS AND AVAILABILITY[a]

by

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IRRADIATION TESTING FACILITIES: NEEDS, LIMITATIONS AND AVAILABILITY

The needs for fast reactor irradiation testing facilities are reviewed and the availability of testing reactors to meet the needs are discussed. Limitations of available facilities are identified and space requirements are compared with availability. The need for continued and improved availability of EBR-II for near term fast reactor fuels and materials testing is emphasized and alternate testing facilities such as Fermi are briefly reviewed. The overall testing capabilities of the Fast Flux Test Facility are discussed.

Meaningful irradiation testing results are clearly an essential requirement for the successful development of the LMFBR. The establishment of a technology that permits the long term reliable operation of fuel cladding and structural materials at temperatures in excess of half their melting points in high energy irradiation fields to fluences significantly greater than those experienced to date should not be expected to be a straight forward development and testing task. Further, the behavior of fuel materials at the levels of burnup and temperatures required for economic power generation are subject to significant uncertainty. The characteristics and consequences of failure of fuels and materials in the high temperature sodium coolant, with its affinity for impurities and certain fission products, are poorly understood. The list of questions which must be answered to permit successful design and operation of an economic and reliable fast breeder reactor whether cooled by sodium or other fluids, is long and challenging.

With respect to the needs for irradiation testing facilities to provide means for developing answers to the many technical questions, it must first be stated that, in general, fast reactors are needed to test fast reactor fuels and materials. The current abundance of thermal test reactors can be used for some purposes such as screening tests of potential fuel materials, examining questions such as does fuel A swell more than fuel B, other things being equal. Such tests can also be used in a semi-quantitative way to cross compare results of the limited fast reactor irradiations with the much larger number of thermal flux irradiations of many of the fuels and materials of interest to fast reactors. In addition, some use is being made of thermal test reactors because of the closer approximation to fast reactor fuel column lengths which can be tested, compared with the available US fast testing facility, EBR-II. Also, some thought is being given to use of thermal test reactors as nuclear heat sources for investigation of things such as fuel rod failure and fission product release characteristics using specially designed integral sodium loops. By and large however, thermal test reactors are of interest for fast reactor testing because of their availability rather than because of known applicability of results.

The only operating US sodium cooled fast reactor, EBR-II, must clearly be the primary test facility for LMFBR irradiation testing for the immediate future. Use of this facility has grown in an exponential manner since 1965 when the first few experimental assemblies were installed. Today, the available irradiation space in EBR-II, at least in the higher flux positions, is essentially occupied to the extent of operational capability. At the present time, there are approximately 34 experiment containing assemblies being irradiated in the EBR-II. The makeup of these experimental assemblies range from full experimental fuel loadings in encapsulated 19 pin arrays, to 19 and 37 pin configurations containing materials specimens with relatively little fuel content. The number and composition of these experimental assemblies is such that the available reactivity is the primary restraint on further experimental irradiation test insertions in high flux locations.

A significant factor limiting the output of experimental results from EBR-II irradiations has been the operational availability of the facility. As a result of a variety of EBR-II operating problems, and a conservative operation philosophy, the effective plant factor has been limited to 30% in 1966 and only 20% in 1967. The operating problems encountered have included nuclear performance anomalies, difficulties in detection and isolation of failures in experimental and driver fuel pins and a larger number of non-reactor related systems difficulties. Plans for improvement in the overall plant factor of EBR-II are being developed in recognition of the need to provide greater on-stream time but these improvements can be expected to require as much as three to five years to develop an effective plant factor in the range of 40 to 60%. The low operational plant factor of EBR-II is a significant factor in the rate of progress of LMFBR irradiation testing as evidenced by the observation that 24 of the 34 experimental assemblies inserted into the reactor in the past three years are still under irradiation. It is also true that many of these tests have high exposure objectives which require long irradiation times.

Projections of EBR-II irradiation test space needs to support LMFBR development programs, including FFTF, over the next one to two years clearly indicate that the available space in the higher flux positions falls substantially short of the indicated needs. Current planning by EBR-II users calls for a minimum of twelve additional subassemblies over roughly the next year with some planning indicating that this estimate may be low by as much as a factor of two under certain conditions. While minor improvement in accommodating the backlog and planned experimental irradiations will result from the planned increase in EBR-II power to 50 MW, the increase in space with flux of interest for fuel irradiation is still small, rather like the visible part of the proverbial iceberg. It appears most likely that some form of priority system will be necessary until the EBR-II plant factor significantly improves and until unencapsulated experimental fuel irradiations become accepted practice.

With improved availability, EBR-II will make a more rapid contribution to the development of LMFBR irradiation testing results. However, this facility like any irradiation testing facility, has some limitations, partially inherent, which limit the direct applicability of results and restrict experimental flexibility. A primary limitation is the maximum flux level, approximately 2 to 3 x 10^{15} nv. Because the available flux is substantially below the design levels for FFTF and interests for LMFBRS, long irradiation times are required to achieve materials irradiation fluences which are representative of the expected design condition. While fuel burnup rates can be adjusted to more nearly represent prototypic conditions by the addition of U-235 enrichment, such a change results in a non-prototypic relationship between fuel burnup and cladding fluence, complicating the evaluation of test results. Near prototypic fuel pin irradiations with respect to length are not possible because of the relatively short core, 14 inches, compared with LMFBR and FFTF design core lengths ranging from two feet to over four feet. This parameter has assumed greater importance in recent months with the recognition that radial fuel swelling may be affected by fuel column length.

The inability to provide instrumentation for irradiation tests in EBR-II is a limitation which complicates interpretation of test results. In addition to the well recognized importance of characterized operating temperatures on cladding and structural materials with regard to affects of temperature on mechanical properties, there is recent evidence that irradiation damage effects in cladding materials may be particularly sensitive to narrow ranges of temperature. Thus, the need for quite precise knowledge of cladding temperature assumes new importance.

An additional limitation of EBR-II is the necessity to conduct all tests in the primary coolant, with primary coolant chemistry and, subject to some adjustment, primary coolant temperature. Thus, tests which might evaluate effects of coolant chemistry, particularly oxygen, on cladding mass transfer and mechanical properties are precluded, except at the EBR-II operating coolant purity levels. While some effort has been devoted to investigation of possible limited closed loop capability in EBR-II, the availability of such a capability, which might also permit somewhat more risky tests, appears to be several years away.

Some of the limitations of the EBR-II would be eased with the availability of the Enrico Fermi reactor for fuels and materials testing. The core height and potential flux are substantially more representative of LMFBR requirements, but the same limitations apply with respect to instrumentation capability and closed loops. There is also some probability that the SEFOR reactor might be convertible for irradiation testing some time in the early 1970's, after the currently planned physics studies have been completed. The capabilities of a modified SEFOR would not however be expected to be a major contribution to fuels and materials testing as a consequence of the relatively low power and flux, but it is not known if a detailed assessment of potential testing capabilities has been made.

The Fast Flux Test Facility is being designed, under the management of the Pacific Northwest Laboratory, as the primary fuels and materials testing facility for the AEC's LMFBR development program. This major testing reactor will provide a flexible capability for testing of fast reactor fuels and materials under well characterized and controlled conditions. The facility includes several sodium cooled, independent closed loops with a heat dissipation capability of up to 6 MW and bulk sodium temperatures up to 1200°F. These closed loops will provide controlled coolant chemistry with individual coolant purification systems as well as provide for instrumentation of experiment test conditions including temperature, flow, and other specialized instrumentation. Tests can be conducted in these closed loops to failure, a feature of major importance since characterized test failures frequently contribute the greatest yield of meaningful testing information. Substantial test space is provided within the driver core region which will operate initially with an outlet temperature of 900°F with similar instrumentation capability to the closed loops. Reflector region test space is also provided. With a core height near three feet, closely prototypic fuel pin cluster tests can be performed. In recognition of the limitations arising from significant departures in available neutron flux from LMFBR design values, the FFTF will provide an initial fast flux capability approaching 10^{16} nv. Additional planned FFTF features include facilities for short term, cyclic and limited transient testing, a closely coupled inert gas atmosphere non-destructive examination facility and a nuclear proof test facility. The FFTF is planned to be complete in 1973, making it available for use in proof testing of fuel for LMFBR demonstration plants, and in the confirmation of design margins for fuels for those plants. A major additional use of the FFTF will be in the development of improved fuels and materials for commercial LMFBRS, both in exploring the capabilities and behavior of candidate fuels and materials systems under prototypic conditions and in fuel system proof testing.

UTILITY REQUIREMENTS IN FAST BREEDERS

by

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Introductory remarks prepared for the panel discussion: "Review of Problem Areas", American Nuclear Society National Topical Meeting on Fast Reactor Systems, Materials and Components, April 2-4, 1968, Cincinnati, Ohio.

UTILITY REQUIREMENTS IN FAST BREEDERS

Mr. Wallace Behnke was scheduled to appear here today to discuss the utility's requirements for fast breeder power plants. Unfortunately, Mr. Behnke is attending a meeting in Washington today, and asked me to take his place.

We have heard much these past two days concerning the progress and problems of fast breeder reactor technology. My purpose is to comment on where we, as a prospective owner-operator, think this technology should be directed. The fast breeder demonstration plants, and later the first generation "commercial" sized plants will have to meet certain requirements with regard to performance and reliability, if they are to be accepted by the utilities.

This is not to say that we can see no incentives to buying fast breeders. On the contrary, we think fast breeders offer an exciting opportunity to continue the downward trend of electrical energy cost and to provide the nation with the abundant supply of energy it demands. The ability of the fast breeder to utilize a large proportion of the latent energy in uranium holds promise for low fuel cost and independence from the price of the raw material, U_3O_8. This will make the cost of electrical energy low and will stretch out the uranium reserves. In addition, fast breeders offer the best market for the large quantities of plutonium that will be created in the many thermal reactors which will be entering service over the next few years. Estimates place this quantity of plutonium at about 100,000 kg fissile recovered from United States reactors by 1980. While some of this plutonium can, and will, be recycled as thermal reactor fuel, its use in fast breeders will be much more efficient. This will increase the plutonium credit for thermal reactors and thereby enhance the overall advantages of nuclear power.

As exciting as technological developments such as fast breeders may be, our overriding consideration is that we are in the electric business. Our primary objective is to generate, distribute and sell electricity at the lowest possible cost. If fast breeders, or any other technological developments, are applied in our business, it will be to help us meet our primary objective. Therefore, in the future when we approach decisions involving the addition of generating capacity, we will compare all forms of power plants. Among the alternatives are fossil-fueled units, either at the mine-mouth or near the load center, several types of thermal reactors, as well as fast breeders. The capital cost, together with operating costs over the life of the plant, will determine which kind of plant will be built.

We realize that fast breeder demonstration plants may not be economically competitive with light water reactors. However, if the future fast breeders are to successfully compete with the other types of plants available, the capital cost of the plant will have to be carefully controlled. The lower fuel costs of fast breeders will then enable us to buy these plants on an economic basis, which is the only way we can do it.

I might add here that Dresden 1 was purchased as a useful addition to our generating capacity. It fulfilled a planned requirement for a unit in the year it went in service. While we expected operating costs to exceed those of a similar sized fossil-fueled plant initially, our part of the installation cost was equal to that of the fossil alternative. We attribute the success of Dresden 1 at least in part to this philosophy and hope to buy fast breeders on the same basis.

So far I have talked only about the need for low cost for fast breeders. Of equal importance is reliability. The need for reliability of generation cannot be stressed too highly. In a business such as ours where the product must be made at the instant it is required, a generating unit outage is always costly and sometimes it can be disastrous. Considering the fuel penalty alone for a 1000 MW unit outage, the cost might be $25,000 to $50,000 per day, depending upon the type of units, since generation must be provided by less efficient units while the outage lasts. This amounts to $750,000 to $1,500,000 per month for an outage extended to that duration. In addition to the fuel penalty, a utility which has made one or more bad decisions and has purchased generating units with poor reliability records will be forced to protect its load with a greater reserve margin, in effect a greater amount of installed capacity per kw of load. It is not difficult to see what this does to the company's carrying charges.

Reliability can be broken down into three categories. They are: Forced outages usually caused by unforeseen equipment malfunctions; scheduled outages for inspection, refueling, maintenance and so on; and capacity limitations in which the unit can continue to operate, but only at reduced load.

All three categories of outage are serious and costly. The way to minimize outages is through careful and conservative design. Remember that the first fast breeder demonstration plants will represent quite a departure in technology from anything we have done so far. As we visualize them today, the fast breeders will be very sophisticated pieces of equipment. Their design will mean exercising the upper limits of our knowledge of fuel capabilities and metallurgy. Past experience has shown that we have a long way to go in this area. Demonstration plants should be designed to operate at conservative outlet temperatures and pressures. There is no point in stacking the deck against fast breeders by trying to build a demonstration unit with high thermal efficiency, only to have its reliability impaired by metallurgical or fuel problems which could have been avoided if a more conservative design were used. Remember that the practicality of the automobile was proven by the crude Model T. The sleek, efficient 1968 models are the result of many years of evolution. So it must be with fast breeders.

Designers must be sensitive to the quality of fabrication available when the plant is built. These demonstration plants will be built on the factory floor and in the field, not in the laboratory. Welding techniques, for example, should be proven dependable before they are employed in building fast breeders. Mistakes in fabrication will occur and the designers should foresee and compensate for them as far as possible. Closer surveillance of fabrication procedures and greater emphasis on quality control will be required to prevent such errors.

Returning to our three categories of outage, some of the specific equipment, the failure of which may be a major contributor to forced outages or capacity limitations, are steam generators and instrumentation. Steam generators are a worrisome problem and from what we have seen, there is still a long way to go in their development. A major effort will be required to develop steam generators to a sufficiently high state of reliability. Instrumentation is of great importance and presents a strong challenge. The first question which must be answered is just what do we need to measure safe, reliable plant operation. At present we lack the ability to adequately monitor sodium flow at critical points in the reactor. The reliability of instrumentation will have to be improved to prevent unnecessary reactor "scrams" without jeopardizing safety.

Scheduled outages are a problem even in light water reactors due to long fueling times and time-consuming inspections. Fast breeders, because of their greater complexity, are even more susceptible to long scheduled outages. These must be held to a minimum. One way to approach the problem is through reduced fueling time. This means that the movement and handling of fuel must be kept simple. Scheduled outages can be reduced further by designing the plant to facilitate rapid inspection and to permit in-service inspections wherever possible. Scheduled outages due to high maintenance items, such as control rod drives, can be reduced by making these devices easily removable so they can be worked off-line.

These are just a few of the things which will have to be considered in increasing plant reliability. Related to reliability, but also to reducing operating cost, is the need to keep plant operation simple. At first there will be a shortage of trained operators. Therefore, there is strong incentive to simplify operation in order to keep the number of operators needed to a minimum and to reduce the time required to train them.

It will be only two or three years before utilities are faced with a decision to purchase demonstration fast breeders. These demonstration plants will be required to play a number of roles. They will provide design and equipment evaluation and operator training. But above all, they will have to prove to the utilities and to the world that the fast breeder concept is economically and operationally sound. The only way this can be proven is to generate kilowatthours more cheaply than can be done by any other means. This means that the lower fuel costs must more than offset the higher investment cost. Moreover, the utilities will quickly lose interest if plant operation is not reliable. These demonstration plants will have to be designed so they are buildable and built so they are operable.

The utility business is a capital intensive one. Therefore, the utilities have become astute buyers. It is necessary that the estimated capital and operating costs of equipment be close to the actual costs. An error of 10% in the estimated cost of an item as large as a generating unit is serious. Before the utilities will be able to justify the purchase of a fast breeder, the uncertainties of cost will have to be narrowed. This means that uncertainties in fast breeder physics, such as plutonium alpha value, will have to be resolved.

To my utility associates I say, "You must begin doing your homework now. You should participate in the manufacturers' design studies both to speed them up and to insure that your needs are adquately reflected in the final designs. You should become associated with the AEC advanced reactor program and learn all you can about fast breeders now. You have only two or three years in which to decide whether or not to participate in a fast breeder demonstration plant. Now is the time to begin thinking about it."

In buying a fast breeder demonstration plant, the purchasing utility takes on considerable risk. It cannot afford redundancy in generation. Operating costs, if excessive, will be a burden on the company and its customers for the life of the plant. Therefore, let me conclude my remarks by repeating that the installation and operating cost of these units must be kept as low as possible, but reliability must be foremost in the minds of the designers. We think this can best be achieved if simplicity and conservative design are made the goals in fast breeder development.

DISCUSSION - SESSION V

R.C. Noyes - I direct this question to Cliff Zitek, but perhaps some other people have ideas also. You emphasized a great deal of reliability and conservative design for the demonstration plant, and I would certainly agree with that on the first demonstration plant. It has to work and the thing it has to demonstrate is that a reasonably large fast breeder can be built and operated reliably. My question is this - I think that we all contemplate that there is going to be more than one demonstration plant. If they are all designed and built very conservatively, what are they going to demonstrate? It seems to me that all they are going to be demonstrating is the same thing over again, and if you are very conservative and sacrifice performance, you can make these things work; but I don't think they are going to demonstrate what really needs to be demonstrated and that is economic potential.

C.B. Zitek - I think the answer to this is that right now the upper limit on temperature of steam to the turbine is fixed. This is unofficial but our experience with generating units in the higher temperatures has not been too good. Because the expected fuel cycle cost of the fast breeders is so low, and we have been told by many people how low it is going to be, do you really have to push the upper thermal limits and try to push for the ultimate in thermal efficiency?

R.C. Noyes - It is true that projected fuel cycle costs are quite low; however, they are not going to be quite so low if we are conservative in other areas. For instance, in the area that I suggested on tight packing of the core. If we make very loose cores with low velocities and low ΔT, it is going to hurt fuel cycle costs quite a bit; but, I think perhaps just because fuel cycle costs are going to be low, the efficiency of the plant ought to be considered when you look at the problems of capital costs. I think that the projections people are making are somewhat varied but typically, the fuel cycle costs may be only 15% or less of the total power costs from a fast breeder, which means that the capital costs portion is really important and capital costs are very much affected by pushing temperatures and efficiency. One of the more effective ways to reduce capital costs is to raise outlet temperatures, increase the temperature drop across the intermediate heat exchanger and the steam generator so that surface areas can be smaller, increase the net efficiency of the plant and this means the whole plant can be smaller. I think the focus of attention on fuel cycle costs being low and then jumping from that to the conclusion that the performance of the plant in terms of temperatures, especially, can be rather conservative is not necessarily true. I think that perhaps we ought to spend more time looking at the effect of temperatures on capital costs and I believe we are going to find again that pushing hard in that area is what is really going to cut down on capital costs.

C.B. Zitek - If you will forgive me, I will refer to my talk that I gave Tuesday in which I stated that we expect that the costs will go down as they did with the light water reactors after we get some experience. I am not excluding the possibility of eventually trying higher temperatures. Right now, we think we are pretty close to the limit, and question why we should go to the ultimate in the early stage of the game?

D.H. Gurinsky - I will ask Cliff what is this steam temperature limit that he is talking about?

C.B. Zitek - 1050°F steam seems to be our limit.

P.F. Gast - This is from the standpoint of reliability in the turbines?

C.B. Zitek - Well, piping and turbine. Perhaps we have some other utility representatives out there. They may want to challenge this figure. As far as I know, this is about it.

S.O. Arneson - I would like to comment that the 1050°F number as a conservative approach is rather a higher number than I would have expected the utility to state as a limit, because that certainly implies reactor bulk temperatures in excess of 1100°F and probably cladding temperatures in the 1250-1300°F range, which I don't believe, on a concensus basis, would generally be considered to be a highly conservative condition. It certainly exceeds the bulk of current experience with high temperature sodium systems. I guess in summary, I think you are a tough taskmaster.

C.B. Zitek - We stress the point that the demonstration plants will have to be built on a very conservative basis. This is not excluding the possibility of eventually attaining these higher temperatures.

S.O. Arneson - You are not stating this as a requirement for demonstration plant level but rather as an estimate of what you consider to be a desirable target level for the ultimate application of the FBR.

R.C. Noyes - I was going to essentially agree with what Steve said and I think that perhaps there is something of a misunderstanding when you are talking about conservatism. Pushing the steam temperature to 1050°F is exactly what I am talking about. That is really pushing things. If we build a conservative demonstration plant now, the steam temperatures may be 800 or 900°F and sodium temperatures may not be much above 1000°F and I think that is appropriate perhaps for the first plant, so I would just like to come back to the original point I raised. What are the subsequent demonstration plants for - what are they going to demonstrate? I think probably one of the more important things they must demonstrate is this considerably higher temperature condition and other movements away from the very conservative positions.

C.B. Zitek - I don't want to run this subject into the ground, but maybe we should. I have read a number of these studies and they are all pushing 1100° sodium, immediately. I think our only point here is that the demonstration plants should not be pushing the upper limits of temperature on fuel.

I have a question for Dick Noyes. It sounded as though your cladding thicknesses that you had illustrated were quite thin. Are you really thinking about using cladding down to 4 mils?

R.C. Noyes - The numbers that were on that graph were the ratios of the cladding thickness to the diameter of the pin. In other words, it is a non-dimensional number. It is not an absolute number, and the ranges there represent clads in the order of 10 mils for large pins up to perhaps 20 mils on small pins. We weren't talking of 4 mils in any case.

C.B. Zitek - I have another question for Steve. Yesterday, we heard a comment that the Fermi plant was being lined up to do these irradiations. Is there an AEC contract with Fermi for these at the present time?

S.O. Arneson - I don't know that I can answer in real detail. My understanding is that the current contract between Fermi and the Commission expires sometime in the next several months. Perhaps there is someone in the audience that has some more detail on that.

J.P. Lagowski (APDA) - Perhaps it is inappropriate for me to comment on a matter for which I don't have all the background, but I believe the present contract between PRDC and the Commission expires in May of this year. There has been some discussion, I believe, between the parties but as far as I am aware, there is no formal program to proceed pending the recovery from the fuel damage incident.

C.B. Zitek - I have a question for Dave. You did not mention hot trapping to clean the sodium, or was this magnesium in solution equivalent to hot trapping?

D.H. Gurinsky - Magnesium and lithium could be the equivalent. I didn't mention hot trapping because we have some evidence that in the case of stainless steels in very tight systems, cold trapping would be adequate. My feeling is that if we were to go to vanadium alloys, we would certainly have to go to hot trapping.

S.O. Arneson - Dave, do you have any feel for what kind of an oxygen level might be required for operation either at the higher temperatures with austenitics or with vanadium as a primary cladding material? It is kind of a loaded question because there have been some estimates that talked about numbers as low as 2, 3, or 4 ppm which certainly exceeds today's state of the art and I think there may be some major questions as to whether or not they are achievable in large operating sodium systems.

D.H. Gurinsky - I noticed that the trend in the objectives seems to be to operate below 10 ppm and, nowadays, I get the feeling that people think they can operate below 5 ppm. Unfortunately, in some of our test work, where we did cold trapping but we found out afterwards that the cold trapping, as well as the system, did some trapping, our cold trapping actually brought the oxygen levels down below, well below, 5 ppm. My feeling is that in the case of the stainless steels up to about 1200°F, 5 ppm would be adequate, as of this moment. In the case of vanadium, I really can't answer this question because I don't have the information. My feeling is that it is lower than this.

K. Goldmann (UNC) - Maybe I should preface my statement that I liked Gurinsky's discussion of the sodium materials problems and that I generally agree with everything that he has said. The subject which we are talking about right now, though, I think that I would like to take a couple of exceptions to. To start with, let me agree that as soon as we use refractory metals as fuel clads, we do have to reduce the oxygen level in the sodium way below those levels that we normally use in stainless steel systems. Now, how low is a very good question. It appears to us from some tests which we have made, where an oxygen activity meter that I mentioned yesterday is used, that as soon as you put refractory metals into sodium, for instance, zirconium into sodium at 1000°F, and if you have a large zirconium surface, the oxygen activity goes down to levels that are equivalent to ppb rather than ppm. Now, very unfortunately, the chemical analysis methods don't allow us to go that low, so whenever a chemist with his chemical analysis looks for the oxygen level in zirconium-gettered or any refractory metal-gettered system, he ends up with a number like a few ppm, but the few ppm are really the lower limits of his detection and he doesn't really know how low the oxygen is. Now, the oxygen meter seems to indicate that you are down to the ppb range.

The area in which I want to disagree a little bit with Dave is the way, perhaps the better approach, on how to get the oxygen down to those levels if you are going to use fuel clads that are made out of refractory metals such as niobium, or vanadium, which are very definitely very, very sensitive to oxygen. You do have to get the oxygen out. I would propose that the oxygen be gotten out by stationary getters rather than by soluble getters, and I have two reasons for this: (1) If you have a stationary getter, you do know where the oxygen ends up. It ends up in a part in the zirconium or whatever you are going to use and you don't need to worry about the oxygen being someplace else. (2) Many of the soluble getters which have been suggested are also not very good from other points of view. For instance, calcium (or Dave mentioned lithium) happen to form nitrides also, and in the very early days of sodium technology, it was discovered that calcium was a rather bad actor in sodium. As a matter of fact, when we talk today about the reactor grade sodium, the major thing that differentiates the reactor grade sodium from ordinary sodium is the elimination of calcium. You shouldn't just put calcium back in again as an oxygen getter because you are really defeating some other purpose.

D.H. Gurinsky - I can't disagree with what Kurt has said; these are viewpoints of people who have worked with sodium systems. I specifically avoided mentioning calcium; however, I don't see how we can avoid the presence of magnesium in the system. I don't know of any work that has been done on eliminating the bred-in or the transmutation product magnesium which is going to be generated as a result of neutrons on sodium. This concentration is going to build up with time, unless you make a stout effort to get it out. It is going to be there and I was simply recommending that we look at the effect of magnesium which, I understand, is one of the few soluble getters that hasn't been tried. Also, I suggested lithium for the reason that it is very effective and the way we determine its effectiveness is not by normal chemical analysis. Our analytical methods are as limited as yours and we use the oxygen meter, which I might say is built by UNC, to determine oxygen activity. It is oxygen activity that is very important. We are finding some very interesting results comparing the oxygen activity as opposed to the oxygen composition. Now, as to whether one should use soluble getters as opposed to hot traps, I would certainly again agree that I think this was a serious ommission on my part to have left it out of my talk; I shall insert it before it goes into the record.

K. Goldmann - I would like to suggest that if you insert it before it goes into the record, please take out my comments.*

C.B. Zitek - I have another question of Steve. You mentioned three to five years before EBR-II would be coming up with better availability numbers. How did you come up with the three to five years?

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*Editor's Note: Dr. Gurinsky had an opportunity to revise his paper and inserted hot trapping as a method of oxygen removal from the system, however, in spite of Dr. Goldmann's request to remove his comments, the discussion was felt to be of sufficient interest to include the comments.

S.O. Arneson - Prediction of EBR-II plant availability is fraught with much uncertainty as any of us who have been associated with the program are aware. The estimate was rather a personal appraisal of what appeared to be the Argonne plan for improvement of the facility and taking into account the experiences to-date in being able to carry out plans for improvement of the plant. I believe that the formal planning by Argonne is aimed at an earlier target.

W.R. Gall (ORNL) - I have two questions or comments. One is that perhaps I wasn't listening carefully to Arneson's discussion but I didn't hear any mention of critical facilities, and I haven't heard any mention of physics problems areas from any of the panel, so I wonder if all the physic problems have been solved already?

P.F. Gast - Well, I think that perhaps this is one point for the Chairman to stick in his oar. It was one of the ground rules of the meeting that we would de-emphasize physics and safety problems since these had been discussed at some length in previous Argonne conferences. For this reason, we attempted to steer away from both the physics and safety. It is true that since these rules were laid down, we have a problem of alpha popping up and I believe that Dave Gurinsky did mention this.

W.R. Gall - I was interested to hear that the piping problems are all solved also. I am Chairman of the Nuclear Piping Code Committee and I would be very happy to hear about these solutions, because at the present time, we regard the piping for sodium systems at 1200°F where the diameters are large and the wall thicknesses small to be a very difficult design problem. As yet, we don't really know how to establish the ground rules in a code for designing these systems, particularly for flexibility.

D.H. Gurinsky - I don't know where engineering starts and materials science ends. What I was trying to say was that the materials problems, per se, in piping, the materials that go into making up the piping, is not an insoluble problem. I did hope that I stressed several times that the engineering of many of the components would be a difficult problem, but occasionally the materials people would like to feel that they can throw a problem to somebody else as the physicist normally does to the materials people.

L.R. Zumwalt (North Carolina State University) - I have a couple of questions for Dave Gurinsky. I was wondering if he has any comment on what the values of alpha or the new uncertainty might have in way of an implication on fuel materials; whether there should be new thoughts on fuel materials, such as even going to metals or something like that.

D.H. Gurinsky - I am really in no position to answer, but it seems to me that if the alpha value is high then it would seem to me it would give a much greater impetus to the work on the carbides which have a higher uranium density. I think the Program Office is well aware of this, and I think that from the Program Office, pressures will have to be constantly applied to certain areas so that the shift in emphasis is done rapidly enough so that the right materials end up being used to obtain the objectives desired.

L.R. Zumwalt - Then the second question. If I remember correctly, Stanley Goldsmith mentioned that he wasn't quite sure whether there was any great compatibility problem with carbide at least with respect to certain claddings, and I wondered if you would have anything to say on that. I am very interested to see what your experience and opinion is.

D.H. Gurinsky - All I can do is repeat what I have read. My feeling is that up to about 800°C, even with sodium bonding, the compatibility problem does not appear too serious. I would suspect that Dick might be able to comment on that just as well as I could.

R.C. Noyes - I think that you know we have been looking at sodium-bonded carbide designs. The most recent encouragement in that respect was the result of some work that Al Strasser has done at United Nuclear. We were down there several months ago talking to him about it, and I think he has just completed a series of compatibility tests and he has found at least two different kinds of carbide material which are apparently completely compatible in the temperature ranges that we are interested in with stainless steel clads. So far as I can tell, it seems to be mainly a matter of avoiding dicarbide phases and there are apparently a number of feasible, reasonably economical ways of doing that. So, we feel quite encouraged that this whole problem of compatibility with sodium bond is going to be solved. As a matter of fact, there probably will be several solutions developed in the very near future.

R.A. Langley (Bechtel Corporation) - I'd like to make one comment on a point that was raised. You (Dr. Gurinsky) mentioned that the piping problems all seemed solvable, and I presume that means within present technology. I get the feeling that we wouldn't be in this business if all the problems weren't solvable in some manner or other.

What I really want to direct my comments to was a seeming lack, as I sat here through the last three days, of very much comment on an area that I think is extremely significant and that is instrumentation. We heard Cliff mention it just a bit. We heard a little mention of it with relation to sodium chemistry, but I am thinking in particular of the major problems of in-core instrumentation which, of course, gets right down to failure mechanisms. How do we detect failures; how do we detect leakers and temperature problems within the core? These seem to be major problem areas. It doesn't do you any good to solve the basic problem of how long something will last until you also have the instrumentation to determine what you do when you find a problem developing. I would like to hear some comments from the members of the panel on the instrumentation problems.

C.B. Zitek - I will say that both my talk and Mr. Behnke's talk did not list all of the problem areas. I mentioned in my talk on Tuesday that in-core instrumentation is a serious problem to consider.

D.H. Gurinsky - I certainly can't answer this question, but it seems to me that in the last few days I heard Jim Schumar worrying about just how to instrument a failure study and I suspect that if he is able to get these kind of studies going and others too that somehow or other you will have to put some instrumentation there to determine when and if the failure has occurred. Perhaps Jim would like to comment on that.

J.F. Schumar (ANL) - Of course, being uninhibited, I have about ten comments I would like to make. Let me direct one to the instrumentation. A little background: we are trying to establish the propagation of failed fuel elements. We are not interested in how they fail; we assume they will fail. There will be of the order of 10^5 or 10^6 fuel pins in a large reactor. Some will have failed. Now, we ask ourselves a question: how do we really know the rate at which that failure might propagate? Being an engineer, I am only interested in rates. I would like to know how fast it propagates, and hope to give a margin of safety to the reactor operator that would define for him that he can operate at some downgraded power level or he can schedule a shutdown. Now, in going through an analysis, what we would need to have is to establish this propagation rate very quickly, in the two second range or even in the two milisecond range, in changing the heat generation rate to the heat removal rate by say plugging up the channel. Then we ask about such things as pressure surges or shock waves and there is no instrumentation right now that can give you the kind of handle you want. There is just no way of detecting these kinds of things. If you try to use the visible spectrum and thus need to have a transparent coolant, as was suggested yesterday, I think you had better forget about it. You can see things without really having to see them with your eyes. There are ways of doing that, although it is a tough problem. I think that even the temperature sensing, when you talk in the two miliseconds range as the TREAT experiments of Charlie Dickerman have shown in trying to get data on meltdowns, is no easy problem and I don't think there is any answer as yet. I am sure there isn't. There is also the problem of where you put your sensing devices; so the first approach is to say well let's just put a pressure instrument of some kind inside a bundle of seven pins, so arrange it that now we breach the jacket, and see if we get an overall pressure surge that might drive all the coolant out of the channels so that the adjacent pins will fail. Again, you are talking numbers like less than seconds before that adjacent pin would fail under the same power generation rate. It is not going to be simple.

May I make another comment? I think if you talk about mills per kilowatt-hour and bring the price of electrical power down, and Paul has heard me preach on this, as soon as the Wall Street Bankers find out that you can take $10 a pound uranium out of the earth and make $5,000 a pound plutonium, the business will go.

C.B. Zitek - This talking about propagation of fuels failures reminds me of a question I wanted to ask. Right now, people are thinking of using sodium-bonding in the d emonstration plant fuel and because the AEC Licensing people are so concerned about loss of sodium bond, perhaps we had better reconsider whether we should use sodium bond for the first fuel loadings.

R.C. Noyes - I would like to make some remarks on the question of instrumentation and maybe I can say something about sodium bonding, too. At least for a commercial type reactor, it seems to me that it is going to be essential to have three different kinds of instruments. The first type is outlet temperature thermocouples, and at least two on each subassembly. I am convinced that that is absolutely necessary. The other type of instrumentation is something to measure fission products in the cover gases and in the sodium. Whether or not that needs to be a local measurement, in other words a sample from each subassembly, or a more gross measurement, I don't think we are quite sure yet. The third type of instrument that I think is essential is a reactivity meter. I think that with the combination of those three sets of systems we will be able to detect the kind of failures that we need to protect against from the point of view of public safety and we will also be able to operate the reactor with a sufficiently low probability that there will not be local, singular, one or two subassembly meltdowns like Fermi. Obviously, with thermocouples in the outlet of each subassembly, there are going to be a certain number of failures and we have to operate with some subassemblies uninstrumented. That is not serious so far as public hazards are concerned, and I think it is also not serious so far as the risk to the utility. I think that we can show that if we have 95, or something like that, percent of the subassemblies instrumented and the reliability of the systems is at adequate levels, the risks are acceptable. One further comment with respect to those thermocouples. How to get those thermocouples in there and how to maintain them creates another real tough design problem for the core designer. They do not have an extremely long life time, so they will have to be replaceable on a regular basis and quite easily so they don't require excessive down time. That is another one of those tough mechanical problems that is going to have to be worked out. I might add also that I think it would be worth studying what the British are doing with outlet thermocouples. They are finding that you can get a lot of very useful information out of them other than just the bulk mixed-mean temperature from that subassembly. By doing various kinds of noise analysis on the thermocouple signals and a cross correlation analysis they feel they can essentially get a pretty good measurement of velocity by that technique. They can also detect local plugging within the subassembly around the fuel pins. I think that that area ought to be hit pretty hard with some fundamental kind of research and development to maximize the amount of useful information that can be gotten out of these thermocouples.

On the question of sodium bond - I would like to ask anyone who has been involved with operating, building, and examining sodium bonded fuel pins, of which hundreds of thousands have been built and operated, if they have ever had any evidence that loss of the bond actually did occur. This question is raised from time to time and the fear is created, but so far as I know, it has never happened or it has never happened in a way which causes some kinds of failures. There has been an awful lot of experience with sodium bonded fuel pins.

C.B. Zitek - Yes, but all of the pins that you are talking about, say, that were in EBR-II, have not gone to anywhere near the burnup that we are talking about for even the demonstration plants. They are limited to 10 or 12,000 megawatt days per ton.

S.O. Arneson - On the question of sodium bonding, it may not be apparent but there have been two sodium cooled reactors built, both of which use sodium bonded fuel. I am referring now to SRE and Hallam, and in neither case am I aware of any operating difficulty or unusual hazard situation developed because of the sodium bond.

C.B. Zitek - Yes, but the AEC had not brought up the question to those licensees. We are dealing now with a fast reactor where the results of the loss of a sodium bond are being considered very critically.

S.O. Arneson - I recognize that the environment is certainly somewhat different but there is certainly some applicability in that experience of Hallam and SRE. On Dick's question of has loss of bond actually occurred? There were some irradiation capsule tests performed in uranium carbide programs where failures occurred and the manifestation was such as to provide a very strong suggestion that in fact, the cladding integrity had been breached and sodium expelled from the primary cladding. In fact, based on swelling measurements of the fuel, there was some indication that the level at which the sodium had been expelled could be determined. In the cases that I can recall, certainly fuel growth did occur at a substantially greater rate in those areas where the bond was thought to have been expelled. There was some interaction with the cladding but I do not recall evidences of major damage to the cladding. Interactions, yes, but not of a catastrophic nature. I realize that it is of somewhat limited applicability because the conditions, the component sizes and the operating levels are not directly applicable to the fast reactor case but I certainly don't read into that limited experience discouragement for the prospects for the loss of sodium bond problem.

R.C. Noyes - I believe that venting fuel pins is probably important in this respect. Certainly if there is a gas plenum enclosed which is charged with high pressure fission gas, and a hole occurs in the clad somewhere down near the bottom, it is quite likely to expel the bond. I think for that reason and for a number of other good reasons, venting sodium bonded pins is the way to go. I think it eliminates that possibility. In the vented case, the situation is reversed. If there is a hole in the clad, the sodium from the primary coolant tends to be forced into that region.

The other question that has been raised from time to time about loss of integrity of the bond is about the release of fission gases and fission gas bubbles accumulating in a high power region. The usual technique, or one of the most effective techniques, for eliminating gas bubbles in bonding when bonds are made as the fuel pins are manufactured, is to heat the fuel pin and vibrate it until the bubbles rise to the top and you get a good bond. This is exactly the situation which exists in the reactor. We don't like those vibrations due to the high velocity coolant that we are going to have but they are going to be there. I think it is going to be one of the side effects which is beneficial and those fission gas bubbles are going to move right out of the bond very quickly because of the high temperature and the vibrations which will always be present which will make the bubbles mobile.

P.R. Heubotter (ANL) - Before we leave the subject of failure effects, I think it would be very useful if Cliff would comment on the consequences of fuel element failure in the light water reactors - what the utilities experience has been here, and what, if any, conclusions can be drawn from this in terms of an estimate of the required reliability of fast reactor fuel elements? I know some reactor manufacturers have been quoting in their studies the design provisions for 1% failure, that is, 99% reliability. Is that at all comparable to the experience in light water reactors at the present time and in fact, is it applicable?

C.B. Zitek - I am afraid that I don't have any actual number to quote but we have operated with quite a few failures. I do not think that you can take this information and relate it to having cladding failures in a fast reactor because of the spacings, the temperatures, and the heat fluxes you are working at. The fact that we can operate Dresden with numerous failures cannot be related to what we will do in a fast reactor. All of our studies are being based on the fact that we will operate with failed fuel, but this may have to go by the board if the AEC puts into your license that as soon as you detect a failure, you have to shut down. I would say we should design for operating with failed fuel but until we get in with the AEC and consummate the technical specifications, we don't know that we can do it.

A. Allgeier (EURATOM, Belgium) - I would like to amend a question that has been asked before concerning the plutonium alphas. It seems evident that this tends to point in a direction to carbides but how does this affect the steam cooled fast reactor?

A. Kare Hannerz (ASEA, Sweden) - We have gone through this question of how the alpha affects the breeding and we found that using the old values and the new also, we have lost about one-half of the breeding gain for the new alpha. So, it is a very serious question for the steam cooled reactor. That is one reason why we are trying to look at other alternatives.

One of the most significant things that came up here was the confirmation of the swelling of the cladding or structural materials. It was shown in Mr. Weir's paper, I think, and I would like to hear if anybody has any comments on how to apply this information in the core design where you have a close tolerance. Can you accommodate swelling of the order of 1 or 2%? I think it is a very critical question. I think Mr. Noyes also mentioned that he would like to go to very narrow spacing, very little coolant area in the spacing with P/D 1.12 or so. I think that if you have such a core, this is a very critical question. Also, the fuel boxes or shrouds may interact or they may jam in the core with a couple percent swelling.

R.C. Noyes - Dave mentioned that there are probably a number of solutions to this problem. The solutions that he suggested seem to indicate that they don't prevent the swelling from occurring but somehow accommodate it. I just wondered if there is some way to prevent the swelling from occurring? If not, I would agree with the comment just made. It is going to make that design problem of deciding how tightly to pack these cores all the more difficult.

D.H. Gurinsky - At the moment I don't know of any solution that has been tried for prevention of swelling. Many of us hope that by proper treatment of the microstructure of these cladding materials that it will be possible to introduce certain types of defect structures which will allow the vacancies to move out more rapidly. You will also recall that I mentioned the possibility of radiation annealing and this was mentioned in connection with this problem of minimizing the swelling phenomenon. But at the present time, I don't know that anyone has tried and succeeded in getting a material which will not swell. Certainly there have not been very many irradiations up to this level either.

C.B. Zitek - May I ask for a little more information on the radiation treatment? You say it is gamma treatment.

D.H. Gurinsky - No.

C.B. Zitek - Not gamma?

D.H. Gurinsky - What we had in mind trying was to see whether or not by reducing the flux and operating a reactor - of course this will not be done with a reactor at first but simply by inserting specimens - whether or not we can in fact reduce the number of voids and the size of the voids by operating at elevated temperatures so as to stimulate the vacancies and hopefully they will tend to move out to grain boundaries and move out of the fuel cladding. If you will recall Weir's comments yesterday, he pointed out that these voids occur over a fairly narrow temperature range somewhere between 350 and about 625°C in the case of stainless steels. Our notion is that if you could operate somewhere around 550-600°C with a lower flux, possibly the bombardment received by the material would allow some of the vacancies to move out because these are apparently what are causing the voids. I am not sure that there are many people who will agree with this point of view, and I see Jim's hand is up right now.

J.R. Weir (ORNL) - I think that there are two approaches to this. One of which Dave is mentioning and in fact there are some data on this, Dave, that we recently obtained for one set of conditions. We have taken some cladding off the EBR-II driver fuel that was taken to about 1.5 x 10^{22} at a temperature of something like about 470°C, and the voids have a size distribution that ranges from about 70 Å or smaller perhaps to 150 to 200 Å in diameter. An hour at 600°C, which is a feasible temperature for the process like what you are talking about, I think, anneals half of those that are around 100 Å in diameter. It leaves those in the structure that are larger. Now what this means is that if you are to postulate this kind of process, you have to do it earlier than at intervals of 1.5 x 10^{22}. So there are some programs looking at that. The second approach that we are taking is to decrease the stability of these voids by alloying (either alloying or putting in the materials sinks for the vacancies) so that the formation of the void is inhibited. And, I think there is a reasonable chance here to improve upon the materials significantly.

G.L. Weil (Consultant) - This question is perhaps directed to Mr. Zitek. I am not sure that the conflict was resolved as to the purpose and the significance of the demonstration of breeders. In other words, the design engineers seem to have a conservative approach to the first plants whereas the utilities would like to get performance data which would indicate reliability and economics which would be better than that which they now obtain. I know that Mr. Zitek's company was one of the first into the boiling water reactor field and I wonder whether he thinks there might be two stages of demonstration plants- one which would satisfy the designers and manufacturers that they can design fast breeders without paying too much attention to the economics performance and then perhaps a second stage which would interest the utilities. In other words, would the utilities be a little more conservative in picking up a fast breeder than they apparently were in picking up the light water reactors?

C.B. Zitek - I thought that I had answered that in the paper by saying that we feel quite sure that the first demonstration plant will not be competitive. The performance data that the designers would get from it would then be fed back in. We would then expect the manufacturers to develop the technology so that it would not be long before the economics would prove themselves out, both in capital cost decrease and fuel costs.

P.R. Heubotter (ANL) - I didn't know if anyone wanted to resume this discussion of cladding voidage but I had a couple of comments to make. I think it is a very serious discovery, but I think it is a little early to push the panic button on it. The British certainly haven't and they are the people who discovered the effect. Three things I should mention. First of all, while they have seen this 7% volumetric increase at something like 7 or 8 x 10^{22}, as I heard them say when they were here, they have only seen this once. They have had other examples of cladding that have gone to that fluence without seeing it. So we may be lucky and just find that this is a non-representative effect for some peculiar treatment that that cladding saw. Secondly, they find that they do get grossly larger amounts of voids in cladding that was solution treated when it went into the reactor and they selected for the DFR 20% cold work in which, they say, the voidage that they see at comparable burnup is much, much lower. The compliancy of their grids are such that they can accommodate very easily for the amount of total swelling that they see in that type material which is, say, 1 to 1.5% diametral increase of the cladding from all causes, cladding swelling as well as fuel pressure and fuel growth. The other thing they mentioned is that a state of stress on the cladding appears to be a requirement to get anywhere near this large amount of voidage. They don't see it, for example, on unstressed specimens irradiated to these kinds of fluences. So they postulate that swelling occurs in the cladding as a result of the stress applied by the growth of fuel on it, which would then seem to be an argument in favor of something like a sodium bonded carbide where you deliberately stand the fuel away from the cladding with the sodium bond and you remove the fuel element when that annulus is consumed. So, that is a possible design solution to the problem as well.

K.A. Trickett (AEC) - One of the things that has caused an EBR-II to shutdown and the lack of availability that Steve Arneson referred to was the loss of sodium bond in the X-011 capsule. So, if this can happen in static capsules, I presume it can happen in fuel elements pushed to long burnups.

I would like to ask Mr. Zitek's reaction on the possibility of designing plants that recognize the cores are consumable and could perhaps first of all have oxide cores with very low rating and then increase the rating on the core as you learn you can do this with the reliability and plant factor you are talking about, and maybe later on, even consider putting in a carbide, nitride, or some rather advanced core. I would like your reaction to that from the capital costs and problem viewpoints, and I would like Dick Noyes's from the practicality of doing it.

C.B. Zitek - I think you are asking for a lot. I would say that our studies are going along on the basis that the designer would use the fuel which had the most testing done on it and I think our point, which we stressed, was that we should not be pushing limits. This goes along with what you said that you would operate the core, shall we say, below expected rates and then gradually increase.

Coming back to the fact of operating with leaks, we certainly would expect that we would operate with leaks but until it is tried out at EBR-II, for instance, I am sure that the AEC is really going to clamp down on our expecting to operate with leaks.

R.C. Noyes - This question of convertibility is one that I have been thinking about for some time. I have not seen published anywhere, although there may be some studies in progress, a good in-depth study of convertibility, such as taking a reactor that is designed for an oxide core and converting it to a carbide core or perhaps preferably a reactor that is designed for convertibility. It is not obvious just exactly what has to be done to convert. For instance, we find that for a sodium bonded carbide design the optimum pin size is something like 0.4 inch outside diameter on the clad. Gas-bonded carbide and oxide pins usually, I think, optimize out quite a bit smaller. There have been some studies in which people have taken a core which has been optimized for oxide and simply substituted carbide in the same pins and when you do that, you find that you have a worse core. You are not demonstrating anything in the way of improved performance by putting in the carbide although you may perhaps prove some performance of the carbide. I think that there needs to be a detailed study on that question of convertibility. I am not saying that it is something that is difficult although it may be very expensive, and if it is very expensive, it may be more logical to design the second demonstration plant as a carbide plant rather than counting on starting with an oxide core and then converting it later. I really don't know which is the best way to do it. Certainly it would be nice if you could take a demonstration plant, start up with oxide conservatively, and then very easily stick in carbide. I suspect, however, that you can't do it easily, and you may have to shut down for a year or two in order to do that, or you may have to compromise the performance of either the first core or the second core so much that the whole results of the experiment are open to question. In other words, I think it gets back again to a question I asked earlier - What are you proving? You may end up demonstrating not much of anything if you follow that sort of course.

H.K. Oppenheim (Bache & Company) - We are very much aware of the profits to be made in financing the fuel cycle. But the question I would like to ask goes back perhaps as a corollary to what Dr. Weil just asked and what I think Bradshaw asked yesterday; what is the time table of breeder introduction? Two years ago when optimism seemed to abound in the industry, the prototypes were going to be built starting in 1970-71, be on stream by the FFTF, be operated for two years, and then orders were to be taken on the commercial breeders. I think, in this more realistic atmosphere, obviously this isn't going to be the time schedule. It is being pushed back. Perhaps 70-71 will be the days for deciding when prototype construction will take place and perhaps the demonstration plants won't be run for two years, four or perhaps even more. There may be subsequent prototype plants and then target plants. I would like to ask the panel just what it thinks the breeder time table is?

R.C. Noyes - I can make a couple of comments which seem to indicate rather gross differences. It seems to me that the studies that are now being done on a national scale about the problem of long term fuel supplies indicate two things. First of all, that the important development is not a nominal sort of breeder but the development of a high gain breeder which has a breeding ratio of 1.5 and doubling times which are 5 to 8 years. Secondly, it is not too important when that high gain breeder is introduced. It is not too important whether it is introduced in 1975 or 1980, or 1985. If you project the total requirements for ore until the year 2020, the way these things are done, even though it seems to be a rather hazardous thing to do, these projections indicate that it is not too important to get in with a breeder quickly - it is much more important to get in with a high gain, high performance breeder. That is one side of the question. The other side of the question, however, I think, is also very important and that is the question of foreign competition. The British are certainly moving along very rapidly and the Russians are also, although I don't really know if they are competitors of ours. But the French and the Germans also seem to be ahead of us in many areas and I don't think we can discount that lightly if they prove to be successful in these early plants that they are building. It seems to me that it will build up an awful lot of pressure to get going quickly and essentially to catch up.