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Argonne National Laboratory

AN EVALUATION OF REACTOR CONCEPTS FOR USE AS SEPARATE STEAM SUPERHEATERS

by

D. H. Lennox, D. R. MacFarlane,
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ARGONNE NATIONAL LABORATORY
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Argonne, Illinois

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Reactor Engineering Division

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TABLE OF CONTENTS

	<u>Page</u>
I. INTRODUCTION	7
II. SUMMARY	9
III. REVIEW OF CENTRAL STATION POWER PRODUCTION TRENDS	11
A. Steam Cycle Temperatures and Pressures	11
B. Steam Turbine Equipment Currently Available for Nuclear Superheat Plants	14
C. Future Trends in Central Station Steam Conditions	15
IV. REVIEW OF THE STATUS OF NUCLEAR SUPERHEAT	17
A. Incentives for Nuclear Superheat	17
B. Review of Superheat Reactor Concepts	17
V. CLASSIFICATION AND PERFORMANCE CRITERIA FOR SEPARATE SUPERHEATING REACTORS	22
A. Classification of Reactor Types	22
B. Selection of Reactor Steam Cycles	22
VI. SELECTION OF REPRESENTATIVE SUPERHEATING CONCEPTS FOR EVALUATION	25
A. General Discussion of the Basic Types of Superheating Reactors	25
1. Direct Cycle	25
2. Indirect Cycle	26
3. Pressure Tube Versus Pressure Vessel Designs	26
4. Solid Versus Liquid Moderators	26
5. Anticipated Results of Current Development Program	27
B. Discussion of Individual Reactor Concepts	27
1. Direct Cycle	27
a. Steam-cooled, Graphite-moderated	27
b. Steam-cooled, Zirconium Hydride-moderated	29
c. Steam-cooled Fast Reactor	29
d. Steam-cooled, Heavy Water-moderated	31
e. Steam-cooled, Light Water-moderated	31

TABLE OF CONTENTS

	<u>Page</u>
2. Indirect Cycle	32
a. Gas-cooled, Graphite-moderated Reactor	32
b. Homogeneous Molten Salt Reactor	33
c. Sodium-cooled, Indirect-cycle Reactors	34
(1) Sodium-cooled, Graphite-moderated Reactor	34
(2) Sodium-cooled Fast Reactor	35
VII. THE STEAM-COOLED, LIGHT WATER-MODERATED SUPERHEATER	36
A. Design Summary	36
B. Mechanical Description	37
C. Heat Transfer	39
D. Turbine Cycle	40
E. Reactor Physics	41
VIII. THE SODIUM-COOLED, GRAPHITE-MODERATED SUPERHEATER	42
A. Design Summary	42
B. Mechanical Description	43
C. Heat Transfer	44
D. Turbine Cycle	44
E. Reactor Physics	45
IX. ECONOMICS	46
A. Nuclear Plants	47
B. Coal Plant	47
C. All Plants	47
X. CONCLUSIONS	50
XI. RESEARCH AND DEVELOPMENT	51

TABLE OF CONTENTS

	<u>Page</u>
APPENDICES:	
A. REACTOR PHYSICS CALCULATIONS	53
B. COMPARISON OF DIRECT AND INDIRECT SUPERHEATER REACTOR COOLANT CYCLES	64
NOMENCLATURE	69
BIBLIOGRAPHY	70

LIST OF FIGURES

<u>No.</u>	<u>Title</u>	<u>Page</u>
1.	Increased Use of Higher Pressures and Reheat in Central Power Stations.	11
2.	Percentage of Reheat Units in Various Pressure Ranges	12
3.	The Maximum and Average Turbine Generator Kilowatt Ratings.	13
4.	Non-reheat Condensing Steam Turbine Generator Units.	13
5.	Reheat Condensing Steam Turbine Generator Units.	14
6.	Light Water-moderated Separate Superheater	38
7.	B^2 vs Enrichment for H_2O -Steam and C-Steam System	60
8.	B^2 vs Enrichment for Clustered H_2O -Steam System	60
9.	B^2 vs Enrichment for Na-C System	61
10.	Temperature Profiles for Direct and Indirect Reactor Coolant Cycles.	65
11.	Comparison of Direct and Indirect Reactor Coolant Cycles for Different Heat Transfer Properties of Coolant.	67

LIST OF TABLES

<u>No.</u>	<u>Title</u>	<u>Page</u>
1.	Superheating Steam Reactors Under Construction or Operating	18
2.	Indirect-cycle, High-temperature Reactor Concepts Planned for Construction.	19
3.	Direct-cycle Superheating Steam Reactor Concepts	19
4.	Reactor Concepts Considered for Superheating Steam to 1050°F	22
5.	Summary of Reactor Plant Steam Cycles	23
6.	Fuel Use Charge for 500-Mw Fast Superheater	30
7.	Design Summary of 500-Mw, Light Water-moderated Separate Superheater	36 & 37
8.	Design Summary of 500-Mw Sodium-Graphite Separate Steam Superheater	42 & 43
9.	Capital Cost Items for Superheat Plants	46
10.	Tabulation of Total Power Costs - 540 Mwe Plants.	48
11.	Thirteen-group Macroscopic Cross Sections (cm^{-1})	54
12.	Compositions of Systems Studied.	58
13.	Cell Compositions	58
14.	Fuel Cluster Geometry	58
15.	Comparison of I_{eff}^{28} , P^{28} and Thermal Disadvantage Factors at 2% Enrichment.	59

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I. INTRODUCTION

As part of the Civilian Power Reactor Program, several reactors for the production of superheated steam are in the design and construction stages. All of these are based on the integral concept whereby the boiler and superheater are one unit. The purpose of this study is to estimate the relative merits of various reactor concepts for use as external superheaters which would add on to an advanced nuclear plant producing saturated steam.

Since a comprehensive analysis of every concept feasible for separate superheating was not possible, the reactors were separated into two basic categories, and a promising example from each group was selected for detailed evaluation. The selection criteria used to screen these various reactor concepts for their potential as superheaters were, first, the current technical feasibility to produce a specified steam temperature; second, the economic promise of the concept; and, finally, the inherent characteristics of the concept favorable to long-term improvements in thermal efficiency.

Section III of this report contains the brief review of modern central station power plants which was made in order to guide the selection of target steam conditions for the superheater. Current limitations on steam turbine equipment are noted and an estimate of future trends is made. The ground rules then used to define the performance of the external nuclear superheater are listed in Section V. Background material on nuclear superheat is furnished in Section IV which contains a survey of current work in the field.

In Section VI, the choice of superheat concepts is narrowed to two fundamental types: (1) an indirect cycle using sodium as the coolant, and, (2) a direct-cycle, water-moderated system. The detailed analysis of these reactors is described in Sections VII, VIII, and IX.

Conclusions specific to this evaluation plus others pertinent to general superheat technology are given in Sections X and XI.

Throughout the report, liberal use was made of data reported in the literature. In some questionable areas, key parameters for the systems studied were checked independently. The cost estimates were made to conform with available AEC accounting procedures.

II. SUMMARY

Various reactor concepts were compared for use as a separate superheater which could be added on to an advanced 300-Mwe reactor producing saturated steam. Fossil steam plant superheat temperatures were used as a criterion for selecting nuclear superheat temperatures. Therefore, the performance specified for the superheater was a minimum exit steam temperature of 566°C (1050°F) when supplied with saturated steam at either 71 atm (1050 psia) or 167 atm (2450 psia). A preliminary screening of ten different reactor concepts resulted in the selection of two for a detailed evaluation. These are a direct-cycle, water-moderated reactor, and an indirect-cycle, sodium-cooled reactor. The steam-cooled, water-moderated system is judged to have the best chance for initially reaching 566°C (1050°F), whereas, the indirect-cycle, sodium-cooled system is considered best for subsequent advances to exploit the more efficient, high-pressure steam reheat cycles. A design concept was selected for each of the reactors to establish a basis for the detailed evaluations and comparisons. The sodium-graphite reactor of Atomics International is the reference design for the sodium-cooled, indirect-cycle concept. The concept for the direct-cycle, water-moderated superheater is a pressure vessel reactor.

The technical evaluation of the two concepts shows that the sodium-cooled, indirect-cycle superheater can realize a significant reduction in the gross plant heat rate [0.6097 to 0.5404 cal/watt-sec (8710 to 7720 Btu/kwh)] by use of the 164-atm (2400 psig) reheat steam cycle. This is not feasible with the water-moderated superheater, because of an assumed limitation of 71 atm (1050 psia) on large-size pressure vessels for reactors. This restricts the direct-cycle superheater to the use of 69-atm (1000-psig) turbines without reheat. On the other hand, to reach the specified steam exit temperature of 566°C (1050°F), the direct-cycle concept requires less extrapolation of current technology. Oxide fuel is suitable for the direct-cycle reactor but carbides are necessary to reach sufficiently high sodium temperatures with the indirect-cycle reactor. Both concepts need fuel cladding of high creep strength; however, the problems of corrosion and erosion are alleviated in a sodium-cooled system.

Cost estimates give an advantage to the water-moderated, direct-cycle system although both show a net reduction in power costs when added as separate superheaters to a boiling water reactor. The significance of a size factor for plants in the 500-Mwe range is illustrated by comparisons made in this study. Power costs for the direct-cycle superheater, boiling water reactor combination were almost identical with those of an integral-type water-moderated direct-cycle system. This is because two of the integral-type units are required to match the power (575 Mwe) of the separate superheater-boiler combination. Conversely, with the sodium system,

a separate sodium-cooled superheater, sodium-graphite boiler combination gives higher power costs than an integral-type sodium system because only one reactor is required for the latter in the 500-Mwe range.

This study has centered on an approach to cheaper nuclear power through higher overall thermal efficiency. In this context, it is clear that there is a significant gap between the existing performance ceiling for modern steam turbines and the presently attainable steam conditions from nuclear superheat. The difference represents an area for potential improvement in thermal efficiency that would lead to lower costs of nuclear power. To reach the ceiling now imposed by steam turbine technology, higher temperatures are indicated for the sodium-cooled superheater, since it is already amenable to the use of a high-pressure, reheat steam cycle. Alternatively, the direct-cycle, steam-cooled superheater (in the light of current development programs) is closer to the current limit to turbine temperature so that the major degree of freedom remaining is increased pressure for improved cycle efficiency.

III. REVIEW OF CENTRAL STATION POWER PRODUCTION TRENDS

In the process of defining ground rules for a nuclear superheat evaluation, a brief survey was made of central station power production. This was necessary because a significant factor in the projection of economic limits for steam conditions from nuclear plants is the state of the art in the steam turbine industry.

A. Steam Cycle Temperatures and Pressures

In a review of the status of central station power production facilities, it was found that over 80% of the electrical energy used in the United States is produced by burning coal, gas, or oil to produce steam to drive turbine-generator units. The position that has been reached by steam-powered electrical generation plants is a result of a steady development of larger unit capacities and higher thermal efficiencies. The increase in efficiency is the result of higher temperatures and pressures at the turbine inlet plus lower exhaust temperatures.

From Electrical World⁽¹⁾ surveys of steam station cost, it is apparent that higher pressures, temperatures, and reheat have found increased

FIG. 1
INCREASED USE OF HIGHER PRESSURES AND REHEAT
IN CENTRAL POWER STATIONS

use over the past ten years in attaining higher thermal efficiencies and lower power production costs. In the 1952 survey, the 96-atm (1400-psig), 538°C (1000°F) non-reheat steam cycle was predominant, while in the years 1954, 1956, and 1958, the 122-atm (1800-psig), 538°C/538°C (1000°F/1000°F) reheat steam cycle has been predominant. As shown in Fig. 1, the proportion of new steam plants utilizing pressures over 83 atm (1200 psig) has increased from 33% to 90% in the last ten years. Likewise, the fraction of new plants using reheat has increased by an average of 7% each year in the period from 1952 to 1958. Although there are discontinuities (1956 and 1958) due to changes in numbers and types of plants reporting, the curves do illustrate the trends.

A graph (Fig. 2) of the percentage of reheat units within various pressure ranges also shows the trend to higher pressure units.

FIG. 2
PERCENTAGE OF REHEAT UNITS IN VARIOUS PRESSURE RANGES

The turbine-generator unit nameplate ratings have also increased over the years. As shown in Fig. 3, ten years ago the maximum nameplate rating was 180 Mw, while today the maximum in service is 500 Mw. The available sizes of turbine-generator units in various pressure ranges is limited for non-reheat, condensing units. In reheat turbine-generator units, the sizes are even further restricted by pressure categories (see Figs. 4 and 5). The temperature limitation on non-reheat turbines is 538°C (1000°F) and on reheat turbines is 593°C (1100°F). The exceptions to the latter are the supercritical turbines which have been designed for temperatures up to 649°C (1200°F).⁽²⁾

FIG. 3
THE MAXIMUM AND AVERAGE TURBINE GENERATOR
KILOWATT RATINGS

NOTE:

ALL UNITS OFFERED IN A CERTAIN PRESSURE RANGE ARE NOT OFFERED IN ALL TEMPERATURE RANGES. THE TEMPERATURE RANGES ARE AS FOLLOWS:

1. SAT.-600°F 3. 751-825°F 5. 901-950°F
2. 601°-750°F 4. 826-900°F 6. 951-1000°F

FIG. 4
NON-REHEAT CONDENSING STEAM TURBINE GENERATOR UNITS

FIG. 5
REHEAT CONDENSING STEAM TURBINE GENERATOR UNITS

B. Steam Turbine Equipment Currently Available for Nuclear Superheat Plants

In establishing the inlet pressure range for the superheater, two basic reactor types were considered for the boiler supplying saturated steam. The first was an advanced boiling water reactor (BWR) operating at 71 atm (1050 psia), and the second was a sodium-cooled system capable of supplying inlet steam at 167 atm (2450 psia). A study of various vendor's turbine-generator catalogs showed that the 71-atm (1050-psia) case limited the maximum size of turbine-generator units available for purchase in the non-reheat category and eliminated the possibility of using standard reheat turbines. The lower limit of the pressure range for reheat turbines is 85 atm (1250 psig).

In the non-reheat turbine category, the maximum pressure range available for standard turbines is 59 atm to 100 atm (851 to 1450 psig). The operating pressure of the BWR is in the middle of this range; however, there is a size limitation for non-reheat turbines. The maximum nameplate rating available is a 250,000-kw, 1800-rpm tandem compound double flow unit. With the inlet conditions of 69 atm (1000 psig) and 538°C (1000°F), one turbine manufacturer stated that they could supply two 250,000-kw, 1800-rpm, non-reheat condensing units, whereas another manufacturer thought they could handle an output of 500 Mw in a single unit. The latter, based upon a brief review of the inlet conditions and their past

experience in large-size 1800-rpm units, believed they could furnish a 500,000-kw, 1800-rpm, tandem-compound, four flow unit, with 97-cm (38-in.) or 109-cm (43-in.) last-stage blades, although an extensive design study would be required to confirm this. A certain amount of savings would be incurred by a large single unit over two smaller half-size units. This savings would be of the order of \$3.70 per kilowatt.

C. Future Trends in Central Station Steam Conditions

At the present time, there is a general feeling in the power industry that the technical progress in steam power stations has slowed somewhat. The criterion used in judging technical progress has usually been reduction in fuel consumption. Higher pressure, higher temperatures, and maximum utilization of the steam cycle have produced roughly a 3% per year reduction in specific fuel consumption. A point is now at hand where the rate of return on increasing pressure, temperature, and other factors is diminishing, and reduction in fuel consumption will be less than 1% per year.

It has been reported that the average and maximum temperature for which steam plants have been designed and built has increased at a rate of 7°C (12°F) per year for the past 50 years. However, a point has now been reached where the cost of further advances in temperature outweigh the value of the possible savings on fuel consumption that might result. Thus, present limitations on the initial temperature and reheat temperatures of the steam are 566°C to 593°C (1050°F to 1100°F). If any advance is to be made above these temperatures, a breakthrough will be necessary in finding new low-cost, high-temperature materials.

The pressure ratings have been increasing also, and have doubled approximately every 12 years for the past 50 years. The limitation of pressure is presently set at 164 atm (2400 psig) for boilers having separation drums because of the increased fabrication difficulties and costs of high-pressure drums. This plateau in pressure may be overcome if the new supercritical, once-through type of boilers, such as used in the Philo and Eddystone plants, prove the economics of high pressure in the range from 239 to 307 atm (3500 to 4500 psig). These plateaus have been evident before, and it might be said that it is necessary for the industry to pause at a certain level in order to consolidate its gains. This would allow a few years for the development of new materials and their fabrication to catch up before the power industry can move ahead to new improvements in thermal efficiency and reduction of fuel consumption.

The average size of turbine-generator units will probably increase for a period of years before any plateau is reached, if such a plateau becomes evident. A large number of the units today are around 200,000-kw

capacity, with a number of 500,000-kw capacity units on order. According to some experts in the field, there is no reason why units of 1,000,000-kw capacity could not be designed and built.* Nevertheless, this is a matter of system economics, as the larger a unit is, the greater are the reserves to be maintained against its outage and the less is the system operational flexibility.

*The December 27, 1960 issue of the WALL STREET JOURNAL reported that T.V.A. had awarded a contract for two 800,000-kw steam turbine-generators to the General Electric Company. Shortly after this announcement it was reported that the size of the units was increased to 900,000 kw.

IV. REVIEW OF THE STATUS OF NUCLEAR SUPERHEAT

A. Incentives for Nuclear Superheat

Most of the nuclear power plants built to date have an inherently low overall thermal efficiency because of the use of saturated steam turbine cycles. Accordingly, economic gains can result from the upgrading of the nuclear power plant steam to those conditions of temperature and pressure found in modern fossil-fired plants. The overall cost savings in mills/kw-hr of electric power generation at the bus bar associated with improved thermal efficiency fall in two areas: (1) capital charge savings, and (2) fuel cost savings. The capital charge savings result from a reduced investment in plant capital because of the reduced thermal power rating of the plant for a given electric output. The fuel cost savings result from an improved net plant heat rate.

The nuclear plant operating with a saturated steam cycle has two fundamental advantages over the fossil fuel plant: lower boiler heat losses and no stack losses. Even with these benefits, the current nuclear plants have a significantly higher net heat rate than the modern conventional power plants. The nuclear plant heat rate can be reduced by using high-temperature superheat, and even better performance can be expected if advanced nuclear superheat reactors can utilize the high-pressure, reheat steam cycles common to fossil fuel plants. The potential gain to the limit of current steam turbine equipment is of the order of 25% improvement in the net heat rate over that from the best saturated steam nuclear plants.

Estimates made during the course of this study illustrate the cost savings expected with nuclear superheat. The cost saving estimated for a separate superheater-BWR combination over a BWR having the same net electrical output is about 0.70 mill/kw-hr. This is a reduction of 0.70 mill/kw-hr in an all BWR system power cost of 8.8 mills/kw-hr. The breakdown of this saving is roughly 60% for capital charges and 40% for fuel cost. It is believed that the saving of 0.70 mill/kw-hr is typical of that which can be expected in large-size nuclear power plants due to the use of superheat. Further impetus to the use of superheat and higher pressures in nuclear systems is given by the current and past trend of conventional power plants toward higher temperature and steam pressure. It seems clear that nuclear power plants must keep abreast of improving steam conditions in order to compete with advances in fossil plants.

B. Review of Superheat Reactor Concepts

As background for this study, data for existing, planned, and conceptual nuclear power plants were compiled and are presented in Tables 1, 2, and 3. Table 1 is a summary of present-day nuclear plants employing superheat, that is, plants which are operating or are under construction and close to operational status.

Table I
SUPERHEATING STEAM REACTORS UNDER CONSTRUCTION OR OPERATING

												Fuel Temperatures						Fuel Elements					
					Power		Steam Exit Conditions				Boiler			Superheater			Boiler			Superheater			
Reactor	Status	Designer	Moderator	Coolant	Thermal (Mw)	Net Elect. (Mw)	Net Cycle Eff. (%)	Press. (psig)	Sat. Temp. (°F)	Temp. (°F)	Degrees S. H. (°F)	Surface (°F)	Center (°F)	Limited By	Surface (°F)	Center (°F)	Limited By	Geom.	Meat	Clad	Geo.	Fuel Alloy	Clad
A. <u>Boiling Water Direct-cycle Integral Superheat</u>																							
1) BONUS	UC	GNEC	H ₂ O	Boiling H ₂ O and Steam	50	16.3	32.6	900	534	900	366		4400	℄ melting	1140		clad	rod	UO ₂	Zr-2	rod	UO ₂	316 SS
2) BORAX-V	UC	ANL	H ₂ O	Boiling H ₂ O and Steam	35.7	3.5	-	600	489	850	361		2000		1115		clad	rod	UO ₂	304 SS	plate	UO ₂ - SS cermet	304 SS
3) Pathfinder	UC	AC	H ₂ O	Boiling H ₂ O and Steam	203	62	30.5	600	489	825	366		4400	℄ melting	1250		clad	rod	UO ₂	Zr-2	double tubular	UO ₂ - SS cermet	316 LSS
4) Ural Atomic Power Station	UC	USSR	Graphite	Boiling H ₂ O and Steam	285	94	33	1400	588	950	362	671	752		986	1022		hollow cylinder	sl. enrich. U alloy	SS	hollow cylinder	sl. enrich. U metal	SS
B. <u>Separate Fossil-fired Superheat</u>																							
1) Indian Point (Consolidated Edison)	UC	BW	H ₂ O (pressurized water)	H ₂ O	585(N) 215(FSH)	255	32	420 (Reactor)	449	1000 (Superheater)	551	605	4350	℄ melting		oil fired		rod	UO ₂ - ThO ₂	SS	-	-	-
2) Elk River	UC	AC	H ₂ O (boiling H ₂ O)	H ₂ O	58.2(N) 14.8(FSH)	22	30.2	955	538	825	287		4500	℄ melting		oil fired		rod	UO ₂ - ThO ₂	SS	-	-	-
C. <u>Indirect-cycle Gas-cooled</u>																							
1) Advanced Gas Cooled Reactor (UK-Winscale)	UC	UK	Graphite	CO ₂	100	27.3	27.3	650	497	850	353	-	-	-	1200		clad	-	-	-	rod	UO ₂ 20% 80%	SS Be
2) Bradwell Power Station	UC	UK	Graphite	CO ₂	1062 (2 Reactors)	300	28.3	730	510	705	195	-	-	-	817	1017		-	-	-	rod (finned)	Nat. U	Mg-alloy
3) Experimental Gas Cooled Reactor (ORNL)	UC	Kaiser Eng.	Graphite	He	84.3	24.1	28.5	1285	575	902	325	-	-	-	1150	3300		-	-	-	rod	UO ₂	SS
4) Heavy Water Gas Cooled Reactor of Czechoslovakia	UC	USSR & CSR	D ₂ O	CO ₂	590	150(G)	25.5 (G)	410	449	752	303	-	-	-	932			-	-	-	pins	Nat. U	Mg-Be alloy
D. <u>Indirect-cycle Liquid Metal-cooled</u>																							
1) EBR-II	UC	ANL	None	Sodium	62.5	21	34	1250	572	850	278	-	-	-	1050	1320	fuel alloy	-	-	-	pins	U-fissium alloy	SS
2) Enrico Fermi Fast Reactor	UC	APDA	None	Sodium	300	90	30	585	483	742	259	-	-	-	1055	1316	fuel alloy	-	-	-	pins	U-fissium	Zr alloy
3) Hallam Power Reactor	UC	AI	Graphite	Sodium	254	76	30	850	525	833	308	-	-	-	800	1260		-	-	-	rods	U-Mo alloy	SS
4) Sodium Reactor Experiment (SRE)	OP	AI	Graphite	Sodium	20	6	28.6	600	486	825	339	-	-	-	1000	1200		-	-	-	rods	Unalloyed U	SS
E. <u>Indirect-cycle Organic-cooled</u>																							
1) Piqua Organic Reactor	UC	AI	Terphenyls	Terphenyls	45	11	25	425	454	550	96	-	-	-	750	880		-	-	-	con-centric finned tubes	U-3.5% Mo alloy	AC
UC - Under Construction		OP - Operating		N - Reactor Heat		FSH - Fossil-fired Superheater																	

Table 2

INDIRECT-CYCLE HIGH-TEMPERATURE REACTOR CONCEPTS PLANNED FOR CONSTRUCTION

Reactor	Status	Designer	Moderator	Coolant	Power		Net Cycle Efficiency (%)	Steam Exit Conditions		Fuel Temperature		Fuel Element		
					Thermal (Mw)	Net Elect (Mw)		Press (psig)	Temp (°F)	Surface (°F)	Center (°F)	Geometry	Fuel	Cladding
1) DRAGON (UK)	UC	UK	Graphite	He	20	None	-	No steam - He outlet temp = 1380°F at 20 atm			4800		UC ₂ + ThC in Graphite	Graphite
2) High Temperature Gas Cooled Reactor	D	GA	Graphite	He	115	30-40	-	1450	1000				UC ₂ + ThC ₂ in Graphite	Graphite
3) Florida West Coast and East Central Nuclear Groups	D †	GNEC	D ₂ O	CO ₂	153	50	32.6	1465	950	1500	3800	tubular	UO ₂	SS
4) Experimental Molten Salt Fueled Power Reactor	To Start Construction	ORNL	Molten LiF and BeF ₂	Molten LiF and BeF ₂	30	10	33.3	1450	1000	1235 (Salt Outlet Temp.)		homogeneous	UF ₄ in molten LiF and BeF ₂	
5) Turret Reactor	D	Los Alamos	Graphite	He	3	-	-	No steam - He outlet temp = 2400°F		3355 (max)		tubular	U in graphite	None
6) Pebble Bed Reactor	UC	BBK (Germany)	Graphite	He and Neon	-	15	-	1030	941	-	-	spheres	UC ₂ in graphite	Graphite

UC - Under Construction

D - Design Phase

† - Project Recently Discontinued

Table 3

DIRECT-CYCLE SUPERHEATING STEAM REACTOR CONCEPTS

Concept Name	Designer	Pressure Tube or Pressure Vessel	Moderator	Coolant	Total Power		Net Cycle Eff (%)	Source of Steam	Steam Exit Conditions		Superheater Fuel Temperature		Superheater Fuel Elements		
					Thermal (Mw)	Net Elect (Mw)			Press (psig)	Temp (°F)	Surface (°F)	Center (°F)	Geometry	Fuel	Cladding
1) Boiling Integral Superheat Reactor (BISR)	GE	PV	H ₂ O	Boiling H ₂ O and steam	572	200(G)	35(G)	Integral Boiler	1000	940	1250	-	tubular	UO ₂	SS
2) Boiling Separate Superheat Reactor (BSSR)	GE	PV	H ₂ O	Boiling H ₂ O and steam	820	300	36.6	Separate BWR	965	900	1250	3800	tubular	UO ₂	304 SS
3) Canadian Steam Cooled Concepts	AECL	PT	D ₂ O	Steam	-	-	33	Loeffler Cycle	1500	750	900	3450	rods	UO ₂	Zr
4) Graphite Moderated Boiling Water Steam Superheat Reactor (GBSR)	AI	PT	Graphite	Boiling H ₂ O and steam	811	300	37	Boiling channels in separate region	1450	1000	2040	2400	rods	UC ₂ in Graphite	None
5) Graphite Moderated pressure tube, Integral Boiling Superheating Reactor (IBSHR)	West	PT	Graphite	Boiling H ₂ O and steam	820	310	37.8	Boiling channels in separate region	1450	1000	1250	3150	rods	UO ₂	304 SS
6) Mixed Spectrum Superheater (MISH)	GE	PV	H ₂ O Boiler Section	Boiling H ₂ O and steam	815	313	38.4	Integral Boiler	1400	950	1250	4500	rods	PuO ₂ - UO ₂	SS
7) Integral Nuclear Superheat Reactor (NUSU)	GENC	PV	H ₂ O	Boiling H ₂ O and steam	818	300	38.6	Integral Boiler	1200	1050	1250	3000	double tubular	UO ₂	347 SS and Inconel-X
8) Steam Cooled - D ₂ O Moderated Reactor	NDA	PT	D ₂ O	Fog and Steam	947	300	31.7	Fog cooled channels in separate region	750	800	960	2200	rods	UO ₂	SS
9) Steam Cooled - Fast Breeder Reactor	NDA	PV	None	Steam	854	300	35.1	Loeffler Cycle	1400	945	1350	4500	rods	UO ₂ - PuO ₂	Inconel
10) Zirconium Hydride Moderated - Integral Superheat Reactor	AI	PV	Zr H	Boiling H ₂ O and steam	784	300	38.3	Integral Boiler	1550	1000	1250	4000	rods	UO ₂	SS

(G) - Gross Electrical Output

This table includes information on reactor systems throughout the world, and it illustrates the current approaches to nuclear superheating. As can be seen from an examination of the column "Steam Exit Conditions," the steam conditions for nuclear plants currently under construction are intermediate between the saturated steam BWR and modern fossil-fired plants. The boiling water reactors with integral superheat, namely, BONUS, BORAX-V, and Pathfinder, are extensions of the direct-cycle boiling water reactor into the area of superheat. The steam temperature limitations on these reactors are imposed by current doubts of the ability of the fuel element cladding to withstand high-temperature steam. The maximum pressure is established by practical limits for the construction and transportation of large pressure vessels.

Likewise, the indirect-cycle, gas-cooled reactors listed in Table 1, especially those using CO_2 as coolant, are limited by compatibility problems of the coolant with the fuel element cladding and graphite moderator at elevated temperatures.

The indirect-cycle, liquid metal-cooled systems, because of high specific power, must use fuels with good thermal conductivity. The initial loadings planned for current reactors of this type will use metal fuels that probably cannot be operated above a centerline temperature of 649°C (1200°F). Building a high-temperature sodium-steam generator that will not leak after repeated thermal transients is difficult, but the steam generator does not set the maximum temperature limit in the current concepts.

The indirect-cycle, organic-cooled reactor is listed in Table 1 mainly for comparative purposes. This reactor concept has decomposition and deposition problems that prohibit its application to a high-temperature, high-efficiency, thermodynamic cycle.

The two fossil-fired superheat reactor systems listed in Table 1, namely, the Indian Point reactor and the Elk River reactor, are useful in that they provide operating experience with nuclear power systems that can utilize standard high-temperature turbines. This type of system probably has no potential beyond this interim function, since economically competitive nuclear power is premised on nuclear fuel that will be cheaper than fossil fuel. Therefore, when nuclear power is competitive, the expense of a fossil-fired superheater would restrict its use to unusual situations.

Some of the indirect-cycle reactors aimed at higher fuel and primary coolant operating temperatures are listed in Table 2. These concepts are more in the nature of reactor experiments designed to investigate areas such as operation at high temperature with ceramic fuel elements, and novel fueling schemes. Of the six concepts listed, only No. 3, the FWCECNG reactor, uses conventional uranium oxide, stainless-steel-clad fuel elements.

Concept No. 4, the Experimental Molten Salt Fuel Power Reactor, is an extension of the aqueous homogeneous reactor concept to higher operating temperatures. The aim with this system is to achieve fuel costs lower than those for heterogeneous reactors.

Direct-cycle nuclear superheating is receiving a considerable amount of attention, as indicated by Table 3. Of the ten different types of reactors listed, types similar to numbers 1, 5, and 7 are now under construction and are also listed in Table 1. Numbers 1 and 7 are integral boiling water superheaters of which the BONUS, Pathfinder, and BORAX-V plants are examples. Number 5 is the graphite-moderated, pressure-tube, integral boiling superheater reactor of which the Russian Ural Atomic Power Plant is representative. Although this latter plant does use an indirect cycle in the boiling water region, it is considered to be in the category of a direct-cycle nuclear power plant since the superheated steam is in direct contact with fuel. Of the ten concepts in Table 3, four use pressure tubes, and six use pressure vessels. Most of the concepts use two-region boiling and superheating. Steam is generated in one region of the core and passed to another region where it is superheated. The exceptions to this method of steam generation are evident in the Canadian steam-cooled reactor concepts and the steam-cooled, fast breeder of Nuclear Development Corp. of America, both of which use all-steam cooling in conjunction with a Loeffler boiler for steam generation. All ten concepts are designed for cycle efficiencies higher than those for plants under construction listed in Table 1; however, they still are somewhat lower than conventional fossil-fired plants.

V. CLASSIFICATION AND PERFORMANCE CRITERIA FOR SEPARATE SUPERHEATING REACTORS

A. Classification of Reactor Types

As indicated in Section III, a wide variety of reactor concepts were initially considered for application as a separate steam superheater. These can be classified in two basic categories: (1) the direct-cycle system, where superheating is performed within the reactor core; and (2) the indirect-cycle reactor wherein steam is heated in an external heat exchanger. It was found useful to further classify the systems. In the former case, since steam is the common coolant, a classification is made on the basis of moderator material. In the latter category, a further breakdown between liquid metal and gas coolants is obviously required. The concepts considered in each category are limited to those which a superficial examination indicated potential for high operating temperature, and for which a certain amount of related information is available in the technical literature. A listing of the various types is given in Table 4.

Table 4

REACTOR CONCEPTS CONSIDERED FOR SUPERHEATING STEAM TO 566°C (1050°F)

Nuclear Superheater Classification	Reactor Coolant	Moderator
1. Direct steam superheating in the reactor core	A. Steam	Heavy water
	B. Steam	Light water
	C. Steam	Zirconium hydride
	D. Steam	Graphite
	E. Steam	Fast
2. Indirect steam superheat- ing in a heat exchanger	A. Sodium	Graphite
	B. Sodium	Fast
	C. Helium	Graphite
	D. Carbon dioxide	Heavy water
	E. Homogeneous	Molten salt

B. Selection of Reactor Steam Cycles

A turbine inlet temperature of 566°C (1050°F) is typical of modern steam cycles and therefore was specified as the superheater outlet temperature for the study. Since the supply of saturated steam is assumed to

come from an unspecified type of nuclear reactor, it is recognized that a variety of pressures might be available. For one case, the superheater is assumed to complement a water-moderated reactor (BWR or PWR) of an advanced design which could provide nearly competitive power costs without nuclear superheat. In consideration of the capabilities of such reactors, the saturated steam is assumed to be produced at 71 atm (1050 psia) and reactor thermal power is assumed to be 1000 Mw. At this power level both the superheater and the boiler are in large sizes favorable to reactor economics. The selection also allows the use of detailed cost analyses which are available for reactors of this general size. With these guidelines, the thermodynamics of the steam cycle leads to the conditions listed in column 1 of Table 5.

Table 5

SUMMARY OF REACTOR PLANT STEAM CYCLES

Case	1	2
Throttle pressure, atm	69 (1000 psig)	164 (2400 psig)
Turbine inlet temperature, °C	566°C (1050°F)	566°C/566°C (1050°F/1050°F)
Gross electrical output, Mwe	575	575
Gross turbine cycle efficiency, %	39.2	44.2
Superheating reactor power, Mwt		
Superheating	467	475
Reheating	-	190
Boiler power, Mwt	1000	635
Combined reactor power, Mwt	1467	1300
Gross plant heat rate, $\frac{\text{cal}}{\text{watt-sec}}$	0.6097 (8710 Btu/kw-hr)	0.5404 (7720 Btu/kw-hr)

A second pressure level considered is 167 atm (2450 psia), which might be available from an indirect-cycle reactor. For this pressure, the gross electrical output from the plant is the same as for the previous case, i.e., 575 Mw, and the plant parameters are adjusted accordingly as shown in column 2, Table 5. At 167 atm (2450 psia), reheat turbines are standard equipment. It is assumed that the reactor is capable of reheating to throttle temperature, thus making the turbine cycle comparable to that of modern fossil-fired plants in all major aspects.

The results of a generalized steam cycle study⁽³⁾ were used to determine turbine cycle efficiency for this study. A conventional steam cycle, using five stages of feedwater heating, is assumed for the plants. The final feedwater temperatures employed are optimized from the turbine cycle point of view and might require modification to meet special requirements of the particular reactor plant supplying the saturated steam. It is also recognized that the above approach leads to turbo-generator sizes that differed slightly from standard. This was ignored in estimating turbine plant costs since, with minor changes, the system power level could be adjusted to match a standard turbo-generator.

For the more detailed examination of the two selected superheating reactor representative concepts certain ground rules were imposed. For direct superheating, a limit on steady-state cladding temperature of 677°C (1250°F) is used, following the value selected for other steam-cooled reactor studies.⁽⁴⁾ Pressure vessel sizes are restricted to those capable of meeting existing limitations involving fabrication and transportation. Fuel burnups are limited to a value of 15,000 Mwd/tonne. For the sodium-cooled indirect system, a maximum coolant temperature 649°C (1200°F) was chosen, since it is the immediate temperature goal in the development of sodium systems components.

VI. SELECTION OF REPRESENTATIVE SUPERHEATING CONCEPTS FOR EVALUATION

At the present time there are several important questions concerning the use of both the direct- and indirect-cycle concepts for superheating that can be resolved only by experiment. The prototype and experimental reactors tabulated in Section III will provide many of the answers which are necessary. In this section, some of the general features and problem areas of the various concepts applicable to separate superheating are discussed. The criteria used to compare the various reactor concepts for their effectiveness as separate superheaters are:

1. Within the framework of current reactor technology, the reactor should be capable of superheating steam to a modern steam turbine throttle temperature of 566°C (1050°F).
2. The power costs from the nuclear boiler-superheater combination should be as nearly competitive with fossil-fueled plants as possible.
3. The reactor should have characteristics favorable to long-term improvements in thermal efficiency.

No single concept that was reviewed appeared to satisfy completely all three of the above objectives. In each case, some extrapolation from current technology is needed to meet the temperature and cost criteria. As a result, two reactor concepts were selected for additional study from the ten that were studied initially. The first has near-term feasibility for producing the desired steam temperature, and it is an example of the direct-cycle class of reactors. The second is an indirect-cycle reactor which, in the opinion of the authors, has better long-term potential for achieving a high thermal efficiency.

A. General Discussion of the Basic Types of Superheating Reactors

1. Direct Cycle

The direct-cycle concept has two fundamental advantages over the indirect cycle: a smaller average temperature differential between the fuel surface and the cycle working fluid, and lower pumping power losses due to the elimination of intermediate heat exchange systems. Also, maintenance and equipment costs for this intermediate loop are not present in the direct cycle. Some problem areas with the direct cycle are: (a) the stability of fuel and cladding materials in high-temperature, high-pressure steam; (b) the carryover of radioactive corrosion products from the reactor to the turbine plant; and (c) the low fuel power density inherent in the use of a gaseous coolant. It is in these first two areas that the direct-cycle superheat reactors under construction listed in Table 1 will give quantitative data.

2. Indirect Cycle

The indirect cycle also suffers from problems of compatibility of coolant with fuels and cladding. However, in this case there is a wider selection of materials and coolants. Since the indirect cycle retains all radioactivity in the primary coolant, carryover from the superheater core to the steam circuit is not a problem. A major problem in the indirect cycle using high-performance coolants is the design of the superheater. In general, operating conditions impose lower steam temperatures for these boiler-superheaters than for conventional fossil-fired boiler-superheaters fabricated of equivalent strength materials. The compatibility of primary reactor coolant with water in the case of the gas-cooled indirect-cycle plant alleviates this problem, but at the expense of greater heat transfer area. The design of efficient high-temperature heat exchangers that can be built at a reasonable cost remains one of the major development areas for the indirect-cycle nuclear reactor systems.

A generalized comparison of the heat transfer parameters of the direct and indirect reactor coolant cycles is given in Appendix B.

3. Pressure Tube versus Pressure Vessel Designs

At the present state of development of the various reactor systems, particularly for small-size reactors, the pressure vessel is a cheaper method of containing the primary reactor coolant than is the more complicated calandria pressure tube design. However, currently, the limitations on pressure vessel fabrication for large-size reactors imposes an upper limit of about 1500 psi on the system operating pressure. Furthermore, a single pressure vessel design with a coolant of high vapor pressure has limited possibilities for using a reheat, steam cycle, or for fueling under full-power operation. All of these items, namely, higher pressure, steam reheating, and fueling under power, offer potentially significant gains in overall plant efficiency which cannot be neglected. The pressure tube design does not offer these advantages without some mitigating disadvantages. Perhaps the most important of the disadvantages associated with the pressure tube concepts is the problem of finding tube materials of low cross section which will withstand high temperatures and pressures. Also, the pressure tube design is more complicated mechanically, due to the multiplicity of manifolds and individual connections that must be leak tight.

4. Solid versus Liquid Moderators

Pressure vessel, liquid-moderated systems suffer a slight thermodynamic penalty in that heat lost to the moderator can only be transferred back to the power cycle at a temperature somewhat less than the saturation temperature of the water-moderator at the system pressure.

In the case of the pressure tube design, where the liquid-moderator must be kept at a fairly low temperature due to the absence of a pressure vessel, this disadvantage is greater yet, because, effectively, the heat generated in the moderator is discarded. The solid-moderated systems may recover this heat loss, but in general will have a lower power density than light water systems because of the large volume of moderator required. The low power density obtainable with a graphite moderator dictates the use of a relatively large, pressure tube reactor for the direct-cycle type of superheater. Large graphite-moderated reactors give some advantages in easier start-up because of the smaller reactivity effects associated with the coolant.

5. Anticipated Results of Current Development Programs

The experience gained in operating the various reactors listed in Tables 1 and 2 will yield pertinent information on design problems and materials. The operation of the direct-cycle reactors will give answers to the questions concerning the compatibility of fuels and cladding with high-temperature steam and carryover radioactivity to the turbine. The indirect-cycle reactors will give information on compatibility of various primary coolants with fuels and claddings and the problems associated with the operation of the radioactive primary-coolant loops. In this latter connection, it appears that the indirect cycle has an advantage in that it presents the possibility of using ceramic fuels and claddings wherein a certain amount of escape of fission products to the coolant is permitted.

B. Discussion of Individual Reactor Concepts

1. Direct Cycle

a. Steam-cooled, Graphite-moderated

A number of designs of graphite-moderated, pressure-tube-type reactors have been proposed for integral boiling and superheating. These concepts appear, with some modifications, to be practical for separate superheating. They differ in a number of ways, but most importantly in the choice of fuel element. As superheating reactors, they would have some common advantages in that internal insulation of the moderator is unnecessary and the moderator heat is available at a high temperature. It is possible to combine the functions of fuel cladding and pressure containment with a single barrier, and in-core steam reheating is possible. In the large-size reactors, a pressure tube design has potential for higher pressures than a pressure vessel design. This type of reactor has the inherent disadvantages of low power density and low specific power, and presents some difficult problems of piping complexity, dimensional stability of graphite, and pressure tube design.

A promising type of fuel element for this type of reactor is that used in the GBSR (Graphite Moderated Boiling and Superheating Reactor).⁽⁵⁾ This concept employs large unclad slugs composed of uranium carbide mixed with graphite as the fuel. The fuel slugs are located in penetrations in the graphite lattice which are isolated from the coolant pressure tubes. The design turbine throttle conditions are 538°C (1000°F) and 123 atm (1800 psig), with reheat to 538°C (1000°F). The pressure tubes are of Inconel-X. This design potentially offers other attractive features, such as refueling at power, possible high fuel burnup, and low fuel-fabrication costs. Operation is possible with failed tubes, and special seals for radioactive coolant on turbine plant equipment are eliminated. A degree of power flattening is afforded by the transfer of heat to the moderator and thence to the coolant. At the present time, however, technology does not provide answers for many questions associated with this concept. The fuel element is promising but remains to be proven in operation and experience is required with fission gas purge systems and the problem of fission gas release. Control rods for this high-temperature reactor have to be developed and many design problems must be solved.

A different type of fuel element is used in the Russian Integral Boiler-Superheater Reactor.⁽⁶⁾ In this reactor, annular fuel elements are positioned immediately around the pressure tubes. The tubes are thus in a region of relatively low thermal neutron flux. This configuration is favorable to neutron economy. However, this is achieved at the expense of a complex assembly of fuel, moderator, and pressure tube which is discarded at each fuel change. To produce 566°C (1050°F) steam with this design, the metallic fuel used in the Russian reactor should be replaced by a ceramic - probably UO_2 . It was not possible during this study to explore the ramifications of this change. The question of whether the neutron economy incentive with this fuel configuration compensates for its complexity and can offset the advantages of a GBSR type element also requires study.

A third type of direct-cycle fuel element design has been proposed by Westinghouse,⁽⁷⁾ and consists of more conventional clusters of UO_2 fuel rods placed in Zircaloy pressure tubes. A re-entrant type of assembly cools the pressure tube to relatively a low temperature with inlet steam. The peak temperature limit is thus set by the fuel cladding. The fuel element problems in general are similar to those of the direct-cycle water-moderated concept. A special problem is attachment of the Zircaloy pressure tube to stainless steel end fittings. The required fuel cladding probably handicaps this reactor when compared with the GBSR in the areas of potentially high fuel burnup and reduction in fuel-fabrication costs.

The conclusions of this survey of the graphite-moderated pressure tube reactor for use as a separate steam superheater are that the technology is not as advanced as it is for the water-moderated pressure

vessel design. As a developed concept using clad fuel in contact with steam, this type of reactor appears to provide only marginal technical advantage over the water-moderated superheater with similar fuel. However, the use of graphite permits some flexibility in fuel element design which, in the case of the GBSR concept, seems to potentially offer some economic advantage in fuel costs, if the associated problems can be solved. Although it is not meant to imply that these problems are unsolvable and it is recognized that applicable work is currently underway at various projects, the art is at a relatively early stage of development.

b. Steam-cooled Zirconium Hydride-moderated

A direct-cycle, zirconium hydride-moderated concept was studied to determine its economic incentive for the production of 566°C (1050°F) superheated steam. The use of zirconium hydride as the moderator in a reactor for superheating steam in direct contact with the fuel elements has been considered previously.⁽⁸⁾ The conclusions drawn from that study indicated that the capital cost penalty incurred by the use of zirconium hydride was not overcome by lower fuel costs. In addition, it was recognized that there were many unique fabrication problems associated with zirconium hydride but they were not evaluated. Since the zirconium hydride concept has the same performance limitations in the fuel zone as the other direct-cycle reactors and a higher projected power cost due to the use of an expensive, unproven moderator, it was eliminated as a possible separate superheater.

c. Steam-cooled Fast Reactor

A fast reactor used to superheat steam in a direct cycle is subject to the same material and power density limits imposed by heat transfer to steam as the other direct-cycle reactors. It has, also, an additional economic liability caused by the large inventory of fuel of high enrichment characteristic of the fast system. In the case of sodium-cooled fast reactors, this economic penalty vanishes if sufficiently high power densities can be reached. Likewise, to hold fuel cycle costs to a competitive level in a fast superheater, the same approach must be used.

To study the potential of a fast reactor for superheat, a core model was picked to maximize power density within the acceptable limits of clad temperature and steam velocity. The smallest diameter fuel pins considered to be mechanically realistic were used in a tightly packed lattice to optimize the surface-to-volume ratio. The heat transfer parameters of the core are as follows:

Metal-to-steam ratio	1.6
Maximum steam velocity	76.3 m/sec (250 ft/sec)
Maximum clad temperature	677°C (1250°F)
Fuel pin diameter	0.318 cm (0.125 in.)
Fuel material	UO ₂
Steam exit temperature	566°C (1050°F) average
Fuel clad	Type 316 stainless steel

By means of the calculation methods described in Appendix B, the average power density obtainable for the optimized core model was found to be about 80 kwt/liter. Thus, for a 500-Mwt superheater, a total core volume of 6250 liters is needed. The critical enrichments of U²³⁵ required for a range of large, oxide-fueled cores were estimated by using 16-group diffusion theory (RE-122 Code).^{*} The corresponding fuel use charge was computed for an annual rate of 4.75% and a thermal efficiency of 40%. The results are tabulated in Table 6.

Table 6

FUEL USE CHARGE FOR 500-Mw FAST SUPERHEATER

Average Power Density, kwt/liter	Core Volume, liters	Critical Enrichment, %	kg U ²³⁵ per liter	Specific Power, kwt/kg	Use Charge, mills/kwhr
62.5	8000	0.129	0.5	125	2.17
100.0	5000	0.14	0.54	185	1.49
167.0	3000	0.16	0.619	270	1.04
500.0	1000	0.23	0.89	562	0.53

Without further consideration of the remainder of the fuel cycle costs, it is clear that the use charges alone severely penalize the fast superheater. In addition, a premium would be paid in fabrication costs to realize the fuel subdivision required to achieve a power density of 80 kw/liter. Consequently, the fast system was eliminated in favor of other superheat concepts.

^{*}Spherical geometry, 50-cm U²³⁸ reflector

Core volume fractions, 0.43 fuel, 0.18 stainless steel
0.39 void (steam).

d. Steam-cooled, Heavy Water-moderated

Since both the direct- and indirect-cycle heavy water-moderated reactors have similar economic limitations, each is discussed in this section. A common characteristic of the heavy water systems is the use of low-enrichment or natural uranium fuel. However, the gains in fuel cost are somewhat balanced by the cost penalty for use of heavy water. In general, the heavy water moderator is generally maintained at a low temperature so that heat losses to the moderator tend to reduce the overall efficiency of the system. Two reactors were considered for separate superheaters, namely, the steam-cooled, heavy water-moderated reactor, and the CO₂-cooled, heavy water-moderated reactor. Representative of the first category are the Canadian steam-cooled reactor concept⁽⁹⁾ and the steam-cooled, heavy water-moderated concept of Nuclear Development Corp. of America.⁽¹⁰⁾ Both are listed in Table 3. In the second category is the Florida West Coast and East Central Nuclear Groups reactor⁽¹¹⁾ by General Nuclear Engineering Co., listed in Table 2. The Canadian concept is an all steam-cooled reactor that uses a Loeffler boiler to generate steam. The performance of this reactor depends upon the development of a zirconium alloy which will permit cladding temperatures of 482°C (900°F) in contact with steam. Even if the development program is successful, the exit steam temperatures will be no higher than about 399°C (750°F). For this reason, this concept has poor potential for use in the high-temperature steam cycles considered in this study. The NDA concept uses uranium oxide fuel clad with stainless steel and, hence, is limited to about the same steam exit temperature as the other direct-cycle concepts. The GNEC CO₂-cooled concept uses stainless steel cladding with CO₂ coolant for generation of high-pressure, high-temperature steam in the indirect cycle.

The inventory of heavy water required is about 1 ton per electrical megawatt of capacity. With a price of heavy water of \$28 per pound, a 14% capital use charge, and an 80% plant load factor, the capital charge for this heavy water, not considering losses, is 1.2 mills/kw-hr. Since a significant fraction of the fuel charge is in fabrication costs, it does not appear that the use of natural uranium or slightly enriched fuel can save enough to overcome the cost penalty for use of heavy water. Under the current price structure in the United States, the heavy water reactor is not an economic competitor with other superheat reactors, and it is therefore ruled out for further consideration in this study.

e. Steam-cooled, Light Water-moderated

At the present time, the direct-cycle, water-moderated superheating reactor is receiving a great deal of attention in this country. Three of this type listed in Table 1 are under construction. Four more reactor concepts of this type are listed in Table 3. Of these seven reactors,

three basic types are represented, namely, the boiling integral superheater,^(12,13,14,15) the boiling separate-superheater,⁽¹⁶⁾ and the boiling water-steam, mixed spectrum integral superheater.⁽¹⁷⁾ All of these reactor systems have a reasonably good foundation in the established technology of boiling water reactors. The boiling integral superheaters are categorically ruled out for consideration as separate superheating reactors. Since a light water-moderated separate superheating steam-cooled reactor does have a good technological foundation in the boiling water reactor systems and has feasibility for producing steam at temperatures comparable to modern fossil-fired plants, it is one of the prominent candidates for a direct-cycle nuclear superheater. A list of some of the advantages of this concept for superheating are:

- (1) Water reactor technology is established; hence development costs are lower.
- (2) The moderator is inexpensive.
- (3) The moderator and coolant are compatible.
- (4) Higher power density is possible than with steam-cooled systems using other moderators.

Each of these items favors lower capital costs for the reactor plant. This capital cost advantage for the light water-moderated, direct-cycle system is somewhat offset by a higher fuel cost as a result of the higher enrichment required for light water-moderated systems. However, in general, the reduced capital costs associated with this system more than overcome the increase in fuel costs due to enrichment.

In summary, the steam-cooled, light water-moderated reactor was selected as the concept for superheat with the best current feasibility for competitive power costs. Accordingly, a conceptual design for a water-moderated superheater was prepared to aid in further evaluation of its potential (see Section VII).

2. Indirect Cycle

a. Gas-cooled, Graphite-moderated Reactor

The field of gas-cooled, graphite-moderated reactor designs with potential for superheating steam to 566°C (1050°F) is represented by designs such as the Pebble Bed Reactor (PBR),⁽¹⁸⁾ the Experimental Gas-cooled Reactor (ECGR),⁽¹⁹⁾ and the High Temperature Gas-cooled Graphite-moderated Reactor (HTGR)⁽²⁰⁾ (see Tables 1 and 2). With some modifications in the reactor and the heat exchangers, these concepts could be considered for use as separate superheating reactors. The possibility of achieving gas temperatures required for producing 566°C (1050°F) steam with stainless steel or beryllium fuel cladding is questionable,

and a common problem in these designs is the lack of a suitable metallic cladding for high gas temperatures. It was not possible in this work to evaluate the potential of alternative claddings, such as of niobium. In general, work in this direction appears to have been bypassed in favor of the development of graphite cladding and techniques for control of escaped fission products. The results of the fuel element development programs now in progress will establish performance limits for these gas-cooled reactors. At this time, however, none of the designs reviewed are considered to be developed to the point where a realistic evaluation of their economic possibilities for superheating can be made.

The design having the best potential for producing high-temperature steam is probably the HTGR. A review of this design indicates some of its potential advantages and disadvantages. The fuel is a mixture of highly enriched uranium and thorium carbides dispersed in graphite and clad with a layer of relatively impervious graphite. Each fuel element is connected to a fission gas purge system. The economic attractiveness of the design is to a large degree dependent on obtaining very high fuel burnups with exposures beyond 55,000 Mwd/ton.⁽²¹⁾ The reactor operates at low pressure and uses low-cost carbon steel vessels and piping. In common with other homogeneous graphite reactors, the moderator heat sink serves to dampen thermal transients in the fuel zone. Use of the HTGR concept as a separate superheater receiving saturated steam at 164 atm (2400 psig) would require higher helium return temperatures from the gas-to-steam heat exchanger than contemplated for the existing design. The feasibility of retaining low-cost carbon steel construction in the helium loop and the effects on blower design need further study.

In comparison with a liquid metal-heated indirect superheater, the gas-cooled system avoids (and could not tolerate for this application) an intermediate heat exchanger and, also, eliminates problems of coolant freezing. Thermal transient stresses would be less severe. In case of leaks the steam is not reactive with the gas coolant. However, the heat exchanger will be larger than a liquid metal-to-steam exchanger and, because of the adverse effect of water vapor on the graphite reactor core, a unit of high integrity is required. In conclusion, the gas-cooled concepts appear to have excellent potential for high-temperature operation, but they require major extrapolations of technology to equal more proven reactor types, such as high-temperature steam superheaters.

b. Homogeneous Molten Salt Reactor

The homogeneous molten salt reactor concept is an extension of the water-moderated homogeneous reactor into the area of high operating temperatures. Like other liquid homogeneous reactor concepts, its greatest potential lies in the possibility of attaining low fuel-cycle costs which more than compensate for the construction and maintenance problems

believed to be associated with this concept. At the present time, there is only one reactor of this type seriously under consideration, namely, the ORNL Experimental Molten Salt Reactor⁽²²⁾ due for construction soon at Oak Ridge. This reactor concept has the requisite characteristics for production of high-pressure, high-temperature steam with a good overall thermal efficiency. However, the concept is in the very preliminary stages of development and its basic feasibility as a practical nuclear reactor is yet to be demonstrated. For this reason, the molten salt concept was eliminated from consideration as a separate nuclear superheater.

c. Sodium-cooled, Indirect-cycle Reactors

In the light of current technology, sodium-cooled reactors are considered to offer promise for economic nuclear power; therefore this type was reviewed for use as a separate superheater. The basic sodium-steam cycle uses an intermediate heat exchanger to transfer heat from the radioactive primary reactor coolant to nonradioactive sodium in a secondary coolant loop. This system is attractive because it offers the potential for producing high steam temperatures with a reactor coolant of very low vapor pressure. In addition, incorporation of a steam reheat loop close to the steam turbine is relatively easy with a sodium-cooled system. Both the high temperature and reheat features give improved cycle efficiency. Flexibility in the choice of the reactor is possible with the sodium-steam cycle. Either a fast or thermal reactor may be used. Both are considered in the following discussion.

(1) Sodium-cooled, Graphite-moderated Reactor

The sodium-graphite superheat concept consists of a sodium-cooled, graphite-moderated reactor similar to the Advanced Sodium Graphite Reactor.⁽²³⁾ A calandria containing the graphite moderator is penetrated by coolant tubes. The fuel assemblies are rod bundles which are inserted into the tubes. To produce steam at 566°C (1050°F), the required bulk sodium temperature leaving the core is about 649°C (1200°F) (a 28°C drop is allowed in the intermediate system). At this temperature the use of metal fuels is questionable because of possible eutectic formation with the cladding and phase changes in the metal. Thus, an oxide or carbide fuel element is required. The higher thermal conductivity of uranium carbide permits the use of fuel rods of larger diameter; hence it is favored over the oxide. Materials of high creep strength are necessary for fuel cladding and structural components; however, corrosion is not a problem with sodium systems at temperatures up to 816°C (1500°F).⁽²⁴⁾ Also, erosion is not considered to be a limiting factor at the sodium velocities required.

The potential of the sodium-graphite concept for economic production of high-temperature steam is dependent upon the successful development of uranium carbide fuels and sodium-to-steam

superheaters. The Atomic Energy Commission Sodium Components Test Program is directed toward development of low-cost, dependable sodium heat exchangers and the design of a 566°C (1050°F) steam generator for testing in 1962 is nearly complete. The exposure of large quantities of uranium carbide fuel to a reactor environment will be accomplished by concepts now under construction. Coincident with the development of uranium carbide fuel and sodium-to-steam superheaters, improved materials are needed to extend the feasible temperature limits. In comparison with the direct-cycle systems, a wider selection of materials is available because steam erosion and corrosion problems are absent.

(2) Sodium-cooled Fast Reactor

The sodium-cooled fast reactor for the production of 566°C (1050°F) steam has fuel and heat exchanger problems in common with the sodium-graphite reactor. In addition, the high power density of a fast reactor imposes fuel design requirements that lead to high fabrication costs. This penalty, plus a required high fuel enrichment, is expected to be overcome by breeding to give an overall fuel cycle that is economical. The use of UC or PuC in place of metal fuels in a fast reactor to produce 566°C (1050°F) steam makes possible an increased steam cycle efficiency but at the expense of breeding gain. High-temperature claddings for fast fuels can be used more liberally than in the thermal reactors because of their reduced parasitic capture in the fast spectrum. For the same reason, the fast reactor can utilize a variety of high-temperature coolants.

The conclusions reached after a preliminary evaluation of the graphite-moderated and fast breeder types of sodium-cooled reactors are:

1. Neither reactor type is capable of producing 1050°F steam under what is considered current technology.
2. Both reactors have the potential for exceeding modern fossil power plant steam conditions with the successful development of ceramic fuels and high-temperature sodium-to-steam superheaters.

To further explore the possibilities of the indirect-cycle sodium-cooled concept for external superheating, the sodium-graphite reactor was selected for a more detailed analysis (see Section VIII). The thermal system was chosen to avoid compounding the difficulties of making a cost estimate with a consideration of the various fuel costs for possible breeding cycles. The potential gains in thermal efficiency that can result from the high-temperature sodium cycle apply to both concepts as do the various development problems.

VII. THE STEAM-COOLED, LIGHT WATER-MODERATED SUPERHEATER

A. Design Summary

For reasons outlined in Section VI, the direct steam-cooled, light water-moderated reactor system was selected as the concept with the greatest probability of producing the economic nuclear superheat power in the immediate future. Consequently, in order to evaluate this concept in more detail, a conceptual design was prepared.

Steam for the superheater is assumed to be generated by a 1000-Mwt, direct-cycle boiling water reactor operating at a pressure of 71 atm (1050 psia). The steam temperature leaving the superheater is assumed to be 566°C (1050°F). These conditions fix the power of the separate superheater at 467 Mwt and the steam flow rate at 583 kg/sec (4.63×10^6 lb/hr). The total thermal output of the BWR and separate superheater combination is then 1467 thermal Mw. The net cycle thermal efficiency for 69-atm (1000-psig) non-reheat turbine cycle is 36.8%. The net electrical output is 540 Mwe.

The superheater utilizes UO_2 fuel rods clad with stainless steel. The rods are clumped together in process-tube steam channels which are surrounded by the light water moderator. There are nineteen 1.02-cm (0.40-in.) dia fuel rods to a bundle. The steam flow through the core is a two-pass arrangement, with the entering saturated steam from the BWR passing up through the center region of the core and exiting through a peripheral second pass. The core is 311 cm (10.2 ft) in dia by 311 cm (10.2 ft) high. The maximum fuel cladding temperature is taken as 677°C (1250°F). With the two-pass steam flow, it is possible to obtain 566°C (1050°F) steam and a power density of ~22 kw/liter, with a reasonable core coolant void fraction.

Table 7 gives a tabular summary of the various parameters for the superheater.

Table 7

DESIGN SUMMARY OF 500-Mw, LIGHT WATER-MODERATED SEPARATE SUPERHEATER

Thermal power to superheating	467 Mw
Steam flow rate	583 kg/sec (4.63×10^6 lb/hr)
Core dimensions	311 cm dia (10.2 ft) x 311 cm high (10.2 ft)
Number of coolant passes	2
Power density	21.6 kw/liter
Specific power	9.9 kw/kg U
Operating pressure	71 atm (1050 psia)
Core pressure drop	3.4 atm (50 psi)

Table 7 (Cont'd.)

Steam exit temperature	566°C (1050°F)
Steam entering temperature	288°C (550°F) (Sat)
Fuel elements - rods, UO ₂	1.02 cm (0.40 in.) OD
Cladding - stainless steel	0.51 mm (0.020 in.) thick
Fuel element configuration	19 rod bundles
Number of rod bundles	1453
Total fuel inventory	50.7 tonne U
Moderator/fuel, vol ratio	1.6
Core volume fractions	
Fuel	0.239
Moderator	0.383
Steam - coolant	0.263
Steam - insulating gap	0.043
Steel	0.072
Maximum clad temperature	677°C (1250°F)
Steam temperature from hot channel	604°C (1120°F)
Average heat flux	$17.0 \frac{\text{watts}}{\text{cm}^2}$ [54,000 Btu/(hr)(ft) ²]
Maximum heat flux	$63.0 \frac{\text{watts}}{\text{cm}^2}$ [200,000 Btu/(hr)(ft) ²]
$\int k(\theta)d\theta$ for fuel	16 watts/cm
Overall max/avg power ratios	
Radial	1.93
Axial	1.50
Hot channel factor	1.4
Heat flux peaking factor	1.4
Moderator velocity (natural circulation)	244 cm/sec (8 ft/sec)
Moderator temperature	288°C (550°F)
Moderator average steam void fraction	0.04
Critical enrichment	1.8%

B. Mechanical Description

A sketch of the reactor pressure vessel and core is shown in Fig. 6. The reactor core is divided into two sections for steam flow - an inner and an outer pass. The steam flow enters the inner pass at the bottom of the pressure vessel through the inlet nozzles in the head of the vessel and flows upward through the central region of the core. At the upper end of the core, the steam is turned 180 degrees, flows downward through the peripheral second pass, and exits from the pressure vessel through nozzles in the side of the vessel.

The core comprises 1453, 19-rod fuel element bundles contained in 6.35-cm (2.50-in.) diameter process tubes. The process tubes are arranged on a triangular pitch in a grid plate at the bottom of the pressure vessel. The tubes are double-walled stainless steel, with the inner tube thicker since it must withstand the pressure differential due to coolant flow pressure drop. The outer process tube wall is thin, and acts as a barrier to form a stagnant steam gap between the inner tube and the moderator. The process tubes extend beyond the full length of the active core, and through the upper and lower axial water reflectors. At the lower end, the tubes are reduced in diameter passing through the lower axial reflector to give a larger volume of reflector. In the upper reflector the tubes are maintained at full channel diameter. The fuel pellets are 0.914-cm (0.360-in.) diameter uranium dioxide with 0.51-mm (0.020-in.) stainless steel cladding.

About 8% of the total superheater power, or 37 Mw, is dissipated in the moderator. This heat is removed by steam from the boiling moderator and is added to the main superheater steam flow in the upper region of the pressure vessel. In order to keep the steam void fraction in the moderator at a low value, a natural convection flow path for the moderator is provided.

The 71-atm (1050-psia) pressure vessel for the reactor is 366 cm (12.0 ft) in. ID with a 15.2-cm (6-in.) thick wall. This is about equal in size to the Dresden BWR pressure vessel and is currently the limit for shop-fabricated pressure vessels. As shown in Fig. 6, all steam nozzles are grouped at the lower end of the pressure vessel. This leaves the upper head of the pressure vessel free for the installation of control rod drives. There are four 46-cm (18-in.) steam inlet nozzles, and four 61-cm

(24-in.) steam outlet nozzles. The pressure vessel wall is cooled by natural convection of the water moderator. The superheater pressure vessel involves design difficulties not encountered in a BWR vessel because of the sharp temperature gradients existing in the large steam exit nozzles and core support plate.

Fuel is handled in the same manner as a BWR, i.e., with grappling tools while the core is flooded. Although the control rod drive units must be removed prior to fuel changes, it is believed that this arrangement results in a less complicated internal core structure and easier fueling than the alternate method of putting the steam nozzles at the top of the vessel and the control rod drives at the bottom.

C. Heat Transfer

A solid rod fuel element was selected over a tubular configuration because the rod offers about 30% more surface-to-volume ratio than the tubular type element for equal fuel center temperature and fuel power density. The two-pass, separate region, coolant flow pattern was selected because it offers an increased average power density over either single-pass flow or a two-pass bayonet-type fuel element. The film coefficients for heat transfer at the hot spots were calculated by means of the Dittus-Boelter equation, with the heat transfer properties of the steam assumed to be constant for the temperature range 427-593°C (800-1100°F). The equation used is

$$h = 0.0072 \frac{G^{0.8}}{D^{0.2}} \quad , \quad (1)$$

where,

h = heat transfer coefficient, Btu/(hr)(ft)²(°F)

G = mass flow rate, lb/hr

D = equivalent diameter, ft

The coolant flow channels are orificed to give equal coolant exit temperature from all channels. The overall maximum-to-average power ratios for the core are 1.93 radial and 1.50 axial. With the two-pass flow scheme, the maximum-to-average radial power ratio in the inner pass is 1.33 and 1.70 in the outer pass. The ratio of average power densities between the inner and outer passes is 2.66. With an average core power density of 21.6 kw/liter this gives an average for the center pass of 31.3 kw/liter and an average for the outer pass of 11.9 kw/liter. The flow area in each pass was adjusted to give equal maximum cladding temperatures, and this occurred with roughly equal cross-sectional areas for the two passes. With a core pressure drop of 3.4 atm (50 psi), the maximum steam exit velocity from the second pass is 53 m/sec (175 ft/sec) and 30 m/sec (100 ft/sec) at the pressure vessel exit nozzles.

With a 1.6-mm (0.062-in.) layer of stagnant steam between the inner and outer process tubes, the heat loss by conduction and radiation to the moderator is about 3% of the gross superheater thermal output. Nuclear heat losses to the moderator are assumed to be 5% of the gross thermal output, resulting in a total of 8% of gross thermal power going to produce boiling in the moderator. In order to limit the steam void fraction in the boiling moderator to a reasonably low value, natural convection is needed. A moderator circulation velocity of about 2.4 m/sec (8 ft/sec) will maintain the overall steam void fraction at 4%. In disposing of the steam generated in the light water moderator, two options are available: (1) the steam can be piped off separately to the inlet of the superheater, and thereby in effect add capacity to the steam supply reactor; or (2) the steam can be mixed with partially superheated steam exiting from the first pass of the superheater, thereby slightly lowering the temperature of the steam entering the second pass. The second method is selected because it offers the following advantages: (1) simpler mechanical design due to the elimination of flow barriers and external piping; (2) the moderator is available for emergency cooling by blowdown through the core. The results of the heat transfer calculations are summarized in Table 7.

D. Turbine Cycle

The light water-moderated, pressure vessel superheater design is not readily adaptable to steam reheating. Internal reheating introduces mechanical complexity for the pressure vessel design, and while the difficulties are not insurmountable, it is believed that, a priori, the gain in thermal efficiency would not be warranted in view of the increased complexity in pressure vessel design and fabrication. External reheat to initial throttle conditions requires an external heat exchanger and doubling of the reactor steam flow. Since the resulting pressure drop in the core is excessive, this is not feasible. Exceptions to this conclusion on external reheat are encountered in some PWR and BWR dual-cycle saturated systems where reheating of wet steam is done at a reduced pressure by condensing primary steam.

The cycle selected, therefore, is a 69-atm (1000-psig), 566°C (1050°F) regenerative non-reheat type (see Section V, Table 5). Five stages of feedwater heating are utilized, giving a boiler return enthalpy of 80% of the maximum. Two tandem-compound, double-flow, 1800-rpm turbines of 287,000-kw rating each are used in parallel. Since the high-pressure, 3600-rpm noncondensing turbines used in the cross-compound type of installations are not available for 69 atm (1000 psig), it is necessary to use tandem compound units. Although two turbines in parallel are used, one manufacturer has stated that a single large unit can be supplied. This would give a slight saving in turbine plant cost. The gross cycle efficiency is 39.2%; with 6% consumed by auxiliaries, this drops to a net efficiency of 36.8%.

The net electric output is 540 Mw, and the net heat rate for the cycle is 0.6489 cal/watt-sec (9270 Btu/kw-hr).

E. Reactor Physics

The reactor physics calculations for the light water-moderated separate superheater are summarized in Appendix A. The hot, clean critical enrichment is 1.8%.

A problem associated with a light water-moderated, steam-cooled reactor is the change in sign, over the operating range, of temperature and flooding coefficients. This is caused by the fairly large change in moderator-fuel ratio between the cold flooded condition and the hot operating condition. Preliminary calculations show that the effect of going from the hot operating condition to a cold flooded system is a loss of about 6% $\Delta k/k$. On the other hand, the low moderator-fuel ratio of 1.6 used in this design will probably result in a positive hot flooding coefficient. This positive coefficient can be reduced by increasing the moderator-fuel ratio at the expense of creating a positive cold temperature coefficient. This probably can be safely handled by heating the superheater with steam prior to bringing it to criticality. Further physics work is necessary to resolve these problems.

VIII. THE SODIUM-COOLED, GRAPHITE-MODERATED SUPERHEATER

A. Design Summary

In the preliminary screening of reactor concepts for use as separate superheaters (see Section VI), the sodium-cooled system was selected as a promising candidate in the indirect-cycle class of reactors. To further explore the potential of the sodium system, a reference design was selected for an SGR reactor. The Advanced Sodium Graphite Reactor⁽²³⁾ of Atomic International served as the basis for the SGR superheater design. An SGR rather than a fast reactor was selected to simplify the economic comparisons. The advantages (and difficulties) of sodium-steam systems, however, apply to both reactor types.

As with the water-moderated superheater, steam is assumed to be generated by a 1000-Mwt, direct-cycle boiling water reactor operating at a pressure of 71 atm (1050 psia). The steam temperature leaving the superheater is 566°C (1050°F). These conditions fix the power of the separate superheater at 467 Mwt and the steam flow rate at 583 kg/sec (4.63×10^6 lb/hr).

The reactor utilizes uranium carbide fuel rods clad with stainless steel. The rods are clumped together in process tube channels which are surrounded by the graphite moderator. There are nineteen 1.37-cm (0.54-in.) dia fuel rods to a bundle. The sodium flows upward through the core and thence to a secondary coolant loop heat exchanger. The core is 336 cm (11.0 ft) in. dia x 336 cm (11.0 ft) high. The maximum cladding temperature is 746°C (1375°F). The core power density is 17.2 kw/liter.

Table 8 gives a summary of the various parameters for the superheater.

Table 8

DESIGN SUMMARY OF 500-Mw SODIUM-GRAPHITE SEPARATE STEAM SUPERHEATER

Thermal power to superheating	467 Mw
Sodium flow rate	1970 kg/sec (15.6×10^6 lb/hr)
Core Dimensions	336 cm (11.0 ft) dia x 336 cm (11.0 ft) high
Number of Coolant passes	1
Power density	17.2 kw/liter
Specific Power	39.9 kw/kg U
Reactor operating pressure	3.4 atm (50 psi)
Sodium exit temperature	649°C (1200°F)
Sodium inlet temperature	460°C (860°F)
Steam generator operating pressure	71 atm (1050 psia)

Table 8 (Cont'd.)

Steam exit temperature	566°C (1050°F)
Steam entering temperature	288°C (550°F) (sat)
Steam flow rate	583 kg/sec (4.63 x 10 ⁶ lb/hr)
Fuel elements - rods, UC	1.37 cm (0.54 in.) OD
Cladding - stainless steel	0.25 mm (0.010 in.)
Fuel element configuration	19 rod bundles
Number of rod bundles	121
Total fuel inventory	11.7 tonnes U
Maximum clad temperature	746°C (1375°F)
Maximum fuel centerline temperature	1579°C (2875°F)
Average heat flux	141 watts/cm ² [446,000 Btu/(hr)(ft) ²]
Maximum heat flux	365 watts/cm ² [1,160,000 Btu/(hr)(ft) ²]
$\int k(\theta) d\theta$ for fuel	125 watts/cm
Hot spot factor on fuel center temperature	1.4
Hot channel factor	1.4
Heat flux peaking factor	1.4
Initial enrichment	2.7%

B. Mechanical Description

In the 467-Mwt SGR separate steam superheating plant, sodium is forced through process tubes containing rod-type fuel and heated from 460°C (860°F) to 649°C (1200°F). The process tubes are surrounded by unclad graphite which is contained in a calandria vessel. The pressure tubes pierce the vessel and are welded to it. The hot sodium flows to three intermediate sodium-to-sodium heat exchangers and returns to the circulating pumps. In the secondary sodium loop the maximum sodium temperature is 621°C (1150°F); the rise in fluid temperature is the same as for the primary fluid, namely, 189°C. The 621°C (1150°F) sodium flows to the shell side of three shell-and-tube heat exchangers which are supplied with 71-atm (1050-psia) saturated steam on the tube side. The steam is then superheated to 566°C (1050°F) as it passes through the sodium-to-steam heat exchanger.

The calandria vessel is 4.57 m (15 ft) in diameter, 5.48 m (18 ft) high, and 1.91 cm (0.75 in.) thick. It is pierced by 158 vertical process tubes with a 8.56-cm (3.37-in.) OD and a 0.51-mm (0.020-in.) wall, positioned on a 25-cm triangular pitch. The equivalent diameter and height of the fuel region is 3.36 m (11 ft). The stainless steel process tubes are surrounded by an 8.71-cm (3.43-in.) OD by 0.25-mm (0.010-in.) wall Zircaloy tube which protects the graphite in the event of a sodium leak in a pressure tube. The graphite, in the form of hexagonal columns 25.2 cm (9.91 in.) across the flats, has a 8.74-cm (3.44-in.) axial hole throughout

its 4.57-m (15-ft) length. This hole accommodates the Zircaloy and stainless steel tubes. A static helium atmosphere is maintained within the calandria for leak detection purposes and also improves heat transfer from the graphite to the process tubes.

The process tubes contain a bundle of 19 rod-type fuel elements on an equilateral pitch. Uranium carbide pellets, 1.27 cm (0.50 in.) in diameter and 2.5 cm (1 in.) high, are bonded with sodium to a stainless steel tube with an OD of 1.37 cm (0.54 in.) and a wall thickness of 0.25 mm (0.010 in.). The active length of fuel is 3.36 m (11 ft). About 61 cm (2 ft) is allowed for fission gas accumulation. A grid with special centering and gripping features at each end positions the fuel and provides a means of insertion and removal.

C. Heat Transfer

The heat transfer calculations and the operating conditions for the SGR superheater are based on data from reference 23. Hot spot factors of 1.4 were estimated for the channel, film, and fuel temperature rises. A maximum sodium velocity in the core of 10.7 m/sec (35 ft/sec) is necessary to limit the fuel surface temperature. The core temperature rise was fixed at 189°C (340°F). Sodium enters the process tubes at the bottom plenum of the calandria, flows vertically along the axis of the fuel rods, and exits through an orifice which gives sodium flow proportional to channel power. The required core inlet temperature is a function of plant steam conditions, the temperature rise in the core, and the temperature differences in the steam superheater sodium-to-sodium intermediate heat exchanger. For 71-atm (1050-psia) saturated steam, the required sodium inlet temperature is 460°C (860°F) with a 56°C (100°F) terminal temperature difference in the superheater and a 28°C (50°F) drop in the intermediate heat exchanger.

An axial chopped cosine power distribution conforming to a max/avg power ratio of 1.38 is assumed. The radial power distribution is a J_0 Bessel function with a max/avg ratio of 1.6. The results of the heat transfer and fluid flow analysis are summarized in Table 8.

D. Turbine Cycle

As mentioned in Section V-B, Selection of Reactor Plant Steam Cycles, it was recognized that the indirect-cycle reactors might be more amenable to the use of a high-pressure, reheat steam turbine cycle. Such a cycle is indeed feasible with the sodium-graphite reactor. Under these conditions, the concept of a separate superheater is less significant since the steam source reactor cannot be a BWR but must be a type capable of supplying 164-atm (2400-psig) steam. Since a sodium-graphite reactor

in the 500-Mwe-size range is a definite possibility, the logical reactor plant for the high-pressure, reheat cycle is a single, large sodium-graphite reactor. Accordingly, a single SGR is the reference system for the 164-atm (2400-psig) plant in the economic comparisons in Section IX. No independent design other than the selection of the steam cycle parameters was done for this plant, since it is essentially the same as the Advanced Sodium Graphite Reactor.

The 69-atm (1000-psig) separate SGR superheater uses the same steam cycle as the water-moderated separate superheater (see Section V, Table 5).

E. Reactor Physics

The volume fractions, composition, and lattice spacing based on the information supplied in reference 23 were used in a 13-group calculation reported in Appendix A. These calculations indicate that an enrichment of 2.7% for a hot operating core is reasonable for UC fuel.

IX. ECONOMICS

In order to determine power cost trends for the nuclear superheaters, economic comparisons were made between an all BWR system, separate superheaters, and integral superheater, and a coal-fired plant. In all cases, the net electrical plant output was 540 Mw, corresponding to the largest central station plants currently under construction. In order to show the trends in net power cost with time for each of the plants, power cost calculations were made for plants commencing operation in 1964 and plants starting in 1970. The breakdown of items under direct capital costs for the economic studies are listed in Table 9.

Table 9

CAPITAL COST ITEMS FOR SUPERHEAT PLANTS

Capital Cost Item	Source of Cost Information References
1. Land and rights	25
2. Site preparation	25
3. Buildings and structure	25,26
4. Reactor plant	25,26,27
5. Turbine generator system	28,29
6. Miscellaneous power plant equipment	25,26

The cost data for the coal-fired plant was obtained from, "Study of a 306 Mwe Coal-Fired Installation,"⁽³⁰⁾ by Ebasco Services, Inc.

In order to extend the existing cost information on the reactor portion of the power plant from the references in Table 9 to the power plants of higher efficiency under consideration here, the capital costs for Item 4, Reactor plant, were calculated in dollars per reactor thermal megawatt. This capital cost per thermal megawatt is assumed to be the same for all the direct-cycle, water-moderated reactors. Capital costs for the indirect-cycle SGR plants are handled in the same manner. Items 1 and 2 are constant for all plants with the exception that Item 2, Site preparation, is slightly greater in the case of the fossil-fired plant. Items 3 and 5 are charged on the basis of dollars per electrical kilowatt output.

The fuel costs for the direct-cycle nuclear plants are based on present BWR fuel costs of 3.0 mills/kw-hr. The future nuclear fuel costs are based on projected BWR fuel costs of 2.1 mills/kw-hr.⁽³¹⁾ The fuel costs for the indirect-cycle SGR plants are taken from reference 27. The ground rules for the economic comparisons are summarized as follows:

A. Nuclear Plants

1. Unit capital costs of BWR, integral superheater, and separate superheater reactors are the same. Unit capital costs of separate and integral SGR reactors are the same.

2. Same turbine cycle for the integral and separate superheaters.

3. Land and rights and site preparation are the same.

4. Present fuel costs for direct cycle based on 3 mills/kw-hr BWR fuel cost at $\eta = 0.31$, or $108\phi/10^6$ kcal ($27.3\phi/10^6$ Btu). Superheater fuel costs 20% more, or $129\phi/10^6$ kcal ($32.7\phi/10^6$ Btu).

5. Future fuel costs for direct cycle based on 2.1 mills/kwh BWR fuel at $\eta = 0.31$, or $75\phi/10^6$ kcal ($18.9\phi/10^6$ Btu). Superheater fuel costs 20% more, or $89\phi/10^6$ kcal ($22.6\phi/10^6$ Btu).

B. Coal Plant

1. Capital costs taken from Ebasco coal-fired 306-Mw electrical plant study and adjusted to 540-Mwe plant size.

2. Present fuel costs based on $139\phi/10^6$ kcal ($35\phi/10^6$ Btu).

3. Future fuel costs are 10% greater or $152\phi/10^6$ kcal ($38.5\phi/10^6$ Btu).

C. All Plants

1. Total indirect costs are 20% of direct construction costs.

2. Contingencies 10%

3. Escalation to plant completion date of 1964 for "present cases" and three-year construction time gives, at 4%/yr, 12% increase over 1959 prices.

4. Interest during construction for three-year period gives 8.1% average on total direct construction costs.

5. Startup cost is 1% of direct construction cost.

6. Capital is charged at 14% per year and with a 80% load factor.

7. "Future plant" capital costs are not escalated, i.e., technological improvements are assumed to balance inflation.

The results of the calculation of net power costs for the various plants are listed in Table 10. The bottom row of the table is the incentive, in mills/kw-hr, of the various nuclear superheating plants over an all-BWR reactor plant. It will be noted that the incentive for the direct-cycle nuclear superheaters over a boiling water nuclear plant decreases slightly with time, namely, about 0.1 mill/kw-hr between "now" and 1970 in each case. This is a reflection of the fact that capital charges have been assumed to be constant with time, and, hence, the capital saving incentive is constant; on the other hand, the nuclear fuel cost is assumed to decrease with time, resulting in slightly reduced gain in mills/kw-hr on fuel cost. The breakdown on the superheat power cost savings of 0.60 to 0.70 mill/kw-hr is roughly 40% on fuel cost and 60% on capital charges. Also, the cost figures indicate a slight advantage of the integral superheater over a separate superheat plant. This results from the fact that, for the three direct-cycle nuclear plants listed in the table, there are two reactors involved for a 540-Mwe electrical plant. The separate superheat plant consists of the 1000-Mw BWR and a 467-Mw separate superheating reactor. The integral superheat plant consists of two 733-Mw integral superheating reactors. The use of these two equal thermal rating reactors in the latter case result in a slightly lower unit cost for the reactor portion of the plant than in the case of the separate superheater employing a large BWR and a separate superheater with about one-half the rating of the BWR. This is probably not particularly significant in the case of the large-size plants under consideration here; however, the advantage could shift more strongly to an integral superheater for smaller power plant sizes.

Table 10

TABULATION OF TOTAL POWER COSTS - 540-Mwe PLANTS

	All BWR		Separate Superheat		Integral Superheat		BWR and SGR		All SGR		Coal	
	Now	1970	Now	1970	Now	1970	Now	1970	Now (69 atm)	1970 (164 atm)	Now	1970
Capital Charges (mills/kw-hr)	5 07	5 07	4 71	4 71	4 67	4 67	4 88	4 88	5 42	4 96	3 33	3 33
Fuel (mills/kw-hr)	3 00	2 08	2 69	1 86	2 69	1 86	3 12	1 86	4 12	2 00	3 28 (10% fuel cost increase in 1970)	3 61
Maintenance and Insurance (mills/kw-hr)	0 70	0 70	0 70	0 70	0 70	0 70	0 70	0 70	0 70	0 70	0 40	0 40
Total Power Cost (mills/kw-hr)	8 77	7 85	8 10	7 27	8 06	7 23	8 70	7 44	10 24	7 66	7 01	7 34
Incentive (mills/kw-hr) (All BWR Base)			0 67	0 58	0 71	0 62	0 07	0 41	-1 43	0 19	1 76	0 51

As shown by the data of the table, the combination of a BWR and SGR in a 69-atm (1000-psig) cycle results in about the same power cost "now" as in the all-BWR system. The incentive increases for 1970 plants but is still less than that for the water-moderated superheaters. The all-SGR system gives the highest power cost of all the nuclear plants, both "now" and in 1970. It also shows the largest change in incentive with time

because it was assumed that the 1970 plant would use a higher efficiency, 164-atm (2400-psig) steam cycle. The poorer economic position of the SGR systems is a reflection of the higher fuel and capital costs of this reactor type. Currently, these reactors use metallic fuels and consequently are limited to relatively low fuel exposures. The future fuel costs of SGR reactors will be greatly reduced if very long fuel exposures are possible with ceramic fuels. For the purposes of this study, however, a maximum exposure of 15,000 Mwd/tonne was assumed.

The costs for the coal-fired plant in the last column are not to be construed as a prediction of trends for fossil-fired plants, but are included merely for the sake of a clearer picture in making the comparisons. It will be noted that a 10% increase in fossil fuel costs will make the direct-cycle, light water-moderated superheater competitive for large central station plants in 1970.

X. CONCLUSIONS

The results of this study indicate that the addition of nuclear superheat to advanced reactors leads to a net reduction in power costs. Within the framework of current turbine plant technology, two steps are open for the improvement of thermal efficiency with separate nuclear superheaters. The first is the realization of existing throttle temperature limits of 566 to 593°C (1050 to 1100°F). The second is use of the higher pressure, reheat-turbines that are available in large-size units. After modern steam plant conditions have been reached, future nuclear plants can aim for even higher temperatures and pressures in anticipation of a breakthrough in materials and turbine technology.

Starting with current reactor technology, each of these successive steps to higher efficiencies becomes more difficult and, at this time, it is not clear where the point of vanishing economic incentive will occur. Also, it seems unlikely that a single reactor type will remain "most attractive" through each stage of progress in technology. For superheat to 566°C (1050°F) in the low-pressure range (50 to 100 atm), the direct-cycle, water-moderated, pressure vessel reactors appear to have fewer technical difficulties and slightly better economic promise than the other reactor types. This concept, however, is in a poorer position to benefit from the increase in efficiency possible through the use of higher pressure (164 atm, 2400 psig) reheat steam turbines. Practical limits on pressure vessel construction plus mechanical complexities associated with an internal reheat loop present formidable obstacles. Consequently, in the intermediate pressure range either the indirect-cycle or direct-cycle pressure tube concepts lead to an approach to modern steam plant efficiencies. In an advance to the high-pressure region (200-350 atm), the direct-cycle, pressure tube concept will suffer from loss in neutron economy brought about by increased amounts of structural material needed in the core. In addition, the consequences of tube failure within the reactor could impose rather drastic restrictions on the design to insure integrity. One answer to these problems would be through the use of high-temperature, low-vapor-pressure coolants within the core to superheat steam outside the reactor in an indirect cycle. The good heat transfer properties of liquid metals can be utilized in the indirect cycle to reduce the absolute differential between maximum and average temperature in the fuel region. Also the area for heat transfer to steam in the heat exchanger can be increased without penalty to the neutron economy.

XI. RESEARCH AND DEVELOPMENT

There are many paths open to the realization of the potential of nuclear superheating to provide high efficiency economically. Already there are several reactor programs aimed toward achieving modern steam plant temperatures by means of nuclear superheat. The next step will be in the direction of increased pressure and the use of steam reheat until the limits of existing steam turbine plant technology are reached. Afterwards, yet higher temperatures and pressures may be profitable. To prepare for this line of development, work is needed to provide fuels and structural materials capable of operation in a steam environment in the region of 164 atm (2400 psig) and 593°C (1100°F). There are certain areas of emphasis for achieving higher temperatures and pressures that apply to all concepts - direct or indirect. Obviously, better fuels and cladding materials are needed as are improved heat transfer techniques. Since, to produce 566°C (1050°F) steam, cladding temperatures in the indirect-cycle sodium system are near 760°C (1400°F) (the boiling point is 870°C), work on boiling liquid metal technology is indicated for advanced concepts.

Currently, the limit on increased pressure for the direct-cycle, water-moderated system is imposed by the capabilities for fabricating pressure vessels. There are two possible paths to provide higher pressure: (1) pressure tube designs, and (2) multiple pressure vessels. These two approaches are basically the same and differ in degree only, in the sense that a pressure tube is actually a small pressure vessel. In each, however, it would be possible to use the steam reheat cycle that cannot be adapted readily to the reactor concept in which a single vessel is employed. The optimization of pressure tube designs will require materials with low capture cross sections and, at the same time, good strength and corrosion resistance to high-temperature, high-pressure steam. The use of a pressure vessel reduces parasitic capture losses and helps the neutron economy. It also alleviates the problem of irradiation damage in the pressure container. On purely technical grounds the use of a number of pressure vessels, consisting of boilers, superheaters, and reheaters, is sound. With improvements in fabrication techniques and materials, the number of vessels required for a large power output system could be reduced. Consideration of this approach to higher pressures for the direct cycle appears to be warranted.

The indirect cycle, utilizing high-temperature, low-vapor-pressure coolants, is attractive because complications within the reactor core arising from high pressure are minimized. Nevertheless, these problems again return to some extent at the liquid metal-to-steam heat exchanger. Consequently, the development of cheaper dependable components for liquid metal systems is a major hurdle in the path to high-temperature superheating.

The scope of this report was limited to a review of those reactor concepts for superheating which have good prospects for producing 566°C (1050°F) superheated steam within a relatively short period of time. The difference between existing and theoretical thermodynamic efficiencies continues to offer incentive for all power plants, and it is noted that steps in this direction are being considered for coal-burning plants through the use of combination gas and steam turbine cycles. Both the high-temperature gas-cooled or boiling liquid metal-cooled reactors offer possibilities for such cycles. As part of an advanced program for high-efficiency nuclear power cycles, the binary and supercritical systems warrant consideration.

APPENDIX A

REACTOR PHYSICS CALCULATIONS

1 Introduction

Four different systems were examined by means of fundamental mode diffusion theory to give approximation calculations. The first two were steam-cooled systems and used either water or graphite as moderator. In the latter case, the graphite simply replaced the water moderator of the first system. Owing to the greatly decreased moderating power of graphite as compared with water, the graphite system is considerably undermoderated. These first two cases were assumed to be essentially homogeneous as far as the physics was concerned and will be referred to hereafter as the water-steam and graphite-steam systems.

The third system examined was also water moderated and steam cooled, but this system was analyzed by taking into account the geometric clustering of the fuel elements. This system will be referred to as the clustered water-steam system.

The last system was graphite moderated and sodium cooled. In this case, the clustering of the fuel elements was again taken into account. This last system is referred to as the sodium-graphite system.

All of the systems utilized UO_2 fuel with an assumed density of 10.2 gm/cc. In the case of the sodium-graphite system, some calculations were also made for the case of uranium carbide fuel having a density of 12.7 gm/cc.

2 Calculational Procedures and Cross Sections

Since the graphite-steam system was expected to have a large epithermal fission contribution, it was decided to use thirteen-group calculations for all of the systems. The use of many groups for the other well-thermalized systems is clearly unnecessary, but, owing to the brevity of the study, it was more expedient to persist with the same (already prepared) multigroup set for all cases examined. The thirteen-group cross sections are given in Table 11. The thermal temperature given in Table 11 (700°K) is appropriate for the graphite-steam system. The other systems had assumed temperatures as follows: water-steam and clustered water-steam, 547°K; sodium-graphite, 1144°K. The thermal cross sections for these systems were synthesized by adding or subtracting pseudo cross sections from the values given in Table 11 to account for the change in neutron temperatures. Spectral hardening and $1/E$ contributions to the thermal group have been consistently omitted.

Table 11
THIRTEEN GROUP MACROSCOPIC CROSS SECTIONS (cm⁻¹)

Lower Energy E _L	Fission Spectrum	U ²³⁵ O ₂								U ²³⁸ O ₂							
		Σ _f	ν	Σ _c	3Σ _{tr}	Σ _{j→j+1}	Σ _{j→j+2}	Σ _{j→j+3}	Σ _{j→j+4}	Σ _f	ν	Σ _c	3Σ _{tr}	Σ _{j→j+1}	Σ _{j→j+2}	Σ _{j→j+3}	Σ _{j→j+4}
1 1353 Mev	0.575	0.0299	2.8	0.0038	0.5296	0.0317	0.0078	0.0022	0.0007	0.0121	2.8	0.0023	0.5113	0.0395	0.0102	0.0028	0.0009
2 0.4979	0.301	0.0276	2.54	0.0033	0.8712	0.0411	0.0056	0.0014	0.0004	0	0	0.0027	0.8294	0.0408	0.0056	0.0014	0.0004
3 0.1832	0.091	0.0322	2.46	0.0124	1.1745	0.0377	0.0025	0.0008	0	0	0	0.0036	1.1301	0.0376	0.0025	0.0008	0
4 0.0674	0.025	0.0391	2.44	0.0094	1.1835	0.0214	0.0010	0	0	0	0	0.0057	1.0744	0.0214	0.0010	0	0
5 0.0248	0.006	0.0506	2.43	0.0152	1.2908	0.0164	0	0	0	0	0	0.0080	1.2796	0.0167	0	0	0
6 0.00912	0.002	0.0690	2.42	0.0242	1.4288	0.0168	0	0	0	0	0	0.0114	1.3480	0.0171	0	0	0
7 0.00335	0	0.1035	2.42	0.0414	1.6066	0.0172	0	0	0	0	0	0.0125	1.3874	0.0176	0	0	0
8 1234 ev	0	0.1495	2.42	0.0673	1.8136	0.0190	0	0	0	0	0	0.0137	1.4558	0.0195	0	0	0
9 454	0	0.1955	2.42	0.0880	2.4346	0.0167	0	0	0	0	0	0.0148	1.4558	0.0161	0	0	0
10 200	0	0.3220	2.42	0.1610	3.1246	0.0178	0	0	0	0	0	0.0160	1.5242	0.0170	0	0	0
11 6	0	0.9200	2.42	0.4600	5.3326	0.0018	0	0	0	0	0	0.0342	1.2848	0.0017	0	0	0
12 0.4	0	0.9200	2.42	0.4600	5.3326	0.0015	0	0	0	0	0	0.0087	1.1138	0.0014	0	0	0
13 thermal (700°K)	0	7.0715	2.42	1.3065	26.377	0	0	0	0	0	0	0.0354	1.217	0	0	0	0

n 0.0228 x 10²⁴

J	C					Na					Fe				
	Σ _c	3Σ _{tr}	Σ _{j→j+1}	Σ _{j→j+2}	Σ _{j→j+3}	Σ _c	3Σ _{tr}	Σ _{j→j+1}	Σ _{j→j+2}	Σ _{j→j+3}	Σ _c	3Σ _{tr}	Σ _{j→j+1}	Σ _{j→j+2}	Σ _{j→j+3}
1 0	0.3614	0.03059	0.00001	0.132	0.00468	0.0014	0.00039	0.00012	0.0002	0.5436	0.0594	0.0023	0.0008	0	0
2 0	0.5541	0.0375	0.00002	0.2112	0.00742	0.00045	0.00011	0.00003	0.0003	0.5269	0.0158	0.0027	0.0016	0	0
3 0	0.7709	0.04545	0.00002	0.2112	0.00601	0	0	0	0.0004	0.8011	0.0082	0	0	0	0
4 0	0.9877	0.05605	0.00002	0.2112	0.00601	0	0	0	0.0007	0.8755	0.0096	0	0	0	0
5 0	1.06	0.05966	0.00002	0.264	0.0075	0	0	0	0.0009	1.1194	0.0131	0	0	0	0
6 0	1.06	0.05958	0.00002	0.297	0.00843	0	0	0	0.0011	0.8802	0.0103	0	0	0	0
7 0	1.06	0.0595	0.00002	0.99	0.02812	0	0	0	0.0005	1.8869	0.0165	0	0	0	0
8 0	1.06	0.0595	0.00004	0.99	0.02812	0	0	0	0.0008	1.6366	0.0160	0	0	0	0
9 0	1.06	0.0595	0.00007	0.264	0.0075	0	0	0	0.0014	2.3171	0.0182	0	0	0	0
10 0	1.06	0.07259	0.00011	0.198	0.00686	0	0	0	0.0022	2.6536	0.0209	0	0	0	0
11 0.00001	1.06	0.01694	0.00035	0.198	0.00165	0	0	0	0.0049	2.6618	0.0018	0	0	0	0
12 0.00003	1.06	0.01317	0.00154	0.2178	0.00128	0	0	0	0.0243	2.7200	0.0013	0	0	0	0
13 0.0001474	1.06	0	0.00637	0.2338	0	0	0	0	0.13756	3.0596	0	0	0	0	0

n 0.0803* n - 0.022* n 0.0848 x 10²⁴*

H ₂ O ^a										Zr					
J	Σ _c	3Σ _{tr}	Σ _{J→J+1}	Σ _{J→J+2}	Σ _{J→J+3}	Σ _{J→J+4}	Σ _{J→J+5}	Σ _{J→J+6}	Σ _c	3Σ _{tr}	Σ _{J→J+1}	Σ _{J→J+2}	Σ _{J→J+3}	Σ _{J→J+4}	
1	0 0010	0 3085	0 0665	0 0230	0 0084	0 0031	0 0011	0 0008	0 0008	0 3825	0 0241	0 0072	0 0020	0 0006	
2	0 0687	0 1586	0 0528	0 0194	0 0071	0 0026	0 0017	0 0008	0 0017	0 0008	0 0137	0 0026	0 0006	0 0002	
3	0 10271	0 2343	0 0793	0 0292	0 0108	0 0039	0 0027	0 0008	0 0008	1 0200	0 0106	0 0008	0 0002	0	
4	0 11389	0 3375	0 1198	0 0441	0 0162	0 0059	0 0035	0 0008	0 0008	1 0840	0 0080	0	0	0	
5	0 14150	0 4279	0 1536	0 0563	0 0208	0 0067	0 0053	0 0008	0 0008	1 0200	0 0075	0	0	0	
6	0 15686	0 4886	0 1752	0 0646	0 0210	0 0160	0 0005	0 0008	0 0008	0 8925	0 0065	0	0	0	
7	0 0001	1 6742	0 5110	0 1843	0 0600	0 0458	0 0014	0 0001	0 0008	0 8925	0 0065	0	0	0	
8	0 0001	1 7009	0 5405	0 1719	0 1313	0 0038	0 0004	0	0 0008	0 8288	0 0061	0	0	0	
9	0 0001	1 7009	0 4524	0 3379	0 0098	0 0008	0	0	0 0008	0 7650	0 0056	0	0	0	
10	0 0002	1 7009	0 8396	0 0239	0 0018	0	0	0	0 0008	0 8925	0 0080	0	0	0	
11	0 0005	1 7343	0 1727	0 0122	0	0	0	0	0 0008	0 7778	0 0017	0	0	0	
12	0 0023	1 7677	0 2624	0	0	0	0	0	0 0010	0 7778	0 0013	0	0	0	
13	0 01234	4 503	0	0	0	0	0	0	0 00438	0 7811	0	0	0	0	

n = 0.0334 x 10²⁴†

n = 0.0425 x 10²⁴*

^aThe calculated age to 0.4 ev is 341 cm²

* Atoms/cc

† Molecules/cc

The fundamental mode flux in group j is given by:

$$\phi_j = \frac{\beta_j + \sum_{i=1}^{j-1} \Sigma_{i \rightarrow j} \phi_i}{\Sigma_{cj} + \Sigma_{fj} + \Sigma_{remj} + D_j B^2} \quad (A-1)$$

In Eq. (A-1), β_j is the fraction of the fission spectrum neutrons born into group j , $\Sigma_{i \rightarrow j}$ is the group macroscopic cross section for transfer from group i to group j , Σ_{cj} , Σ_{fj} , and D_j are, respectively, the group capture cross section, fission cross section, and diffusion constant, and B^2 is the buckling. The group removal cross section Σ_{remj} is given explicitly by

$$\Sigma_{remj} = \sum_{i=1}^{N-j} \Sigma_{j \rightarrow j+i} \quad (A-2)$$

where N is the number of groups. The required value of B^2 is such that the resulting group fluxes are consistent with the condition

$$\sum_{j=1}^N \nu_j \Sigma_{fj} \phi_j = 1 \quad (A-3)$$

where ν_j is the number of neutrons per fission in group j .

For a critical system, the calculated value of B^2 should equal the geometrical buckling. For the present cylindrical systems, B_g^2 is given by

$$B_g^2 = \frac{\pi^2}{(H + 2h)^2} + \frac{(2.405)^2}{(R + r)^2} \quad (A-4)$$

where H and h are, respectively, the actual height and estimated axial reflector savings; R and r , respectively, the actual radius and estimated radial reflector savings.

For the present studies, h has been taken to be equal to r and to have the following values: water-steam and clustered water-steam, 8.0 cm; graphite-steam, 22.0 cm; sodium-graphite, 30.0 cm. All three of the steam systems have $H = 317$ cm and $R = 158.5$ cm with a 30-cm water reflector radially and axially. The sodium-graphite system has $H = 341$ cm and $R = 170.5$ cm with a 30-cm graphite reflector radially and axially. The geometrical bucklings for the various systems are, therefore: water-steam and clustered water-steam, $2.976 \times 10^{-4} \text{ cm}^{-2}$; graphite-steam, $2.532 \times 10^{-4} \text{ cm}^{-2}$; sodium-graphite, $2.052 \times 10^{-4} \text{ cm}^{-2}$.

The assumption of equal radial and axial reflector savings is no doubt in error owing to the necessarily dilute nature of the axial reflector. However, since the axial buckling only accounts for about 30% B_g^2 and since $H \gg h$, a large error can be made in the value of h without serious change in B_g^2 . For example, if h is set equal to zero in the water-steam systems, B_g^2 only increases by 3%.

3. Resonance Group Considerations

The resonance escape probability was calculated by means of the expression

$$p^{28} = \exp \left\{ -\frac{N}{\bar{\xi} \Sigma_s} \right\} I_{\text{eff}}^{28}, \quad (\text{A-5})$$

where

$$\bar{\xi} = \frac{\sum_i \xi_i \Sigma_{s_i}}{\sum_i \Sigma_{s_i}} = \frac{\sum_i \xi_i \Sigma_{s_i}}{\Sigma_s} \quad (\text{A-6})$$

and the summation extends over all materials of the mixture. Here N is the number of absorber atoms per cc, ξ_i is the average logarithmic energy loss suffered by a neutron per collision with material i , and Σ_{s_i} is the resonance group scattering cross section of the material i . The same expression was used both for the clustered and non-clustered geometries, so that resonance disadvantage factors are being ignored in the clustered case.

The U^{238} effective resonance integral was calculated by means of the expression

$$I_{\text{eff}}^{28} = 3.8 \left(\frac{10^{24} \Sigma_s}{N_U} \right)^{0.42}, \quad (\text{A-7})$$

where Σ_s is the homogenized resonance group scattering cross section and N_U is the number of U^{238} nuclei per cc. For the clustered water-steam and sodium-graphite systems, the Σ_s appropriate to the fuel cluster was used in Eq. (A-7). Only the portion of I_{eff}^{28} appropriate to the "resonance group" was used in Eq. (A-5).

Group 11 was taken to be the "resonance group," and the U^{238} group 11 capture cross section was adjusted so that, when combined with the other U^{238} group capture cross sections, as

$$\sum_{j=1}^{N-1} \Delta u_j \sigma_{c_j}^{28} \quad , \quad (A-8)$$

the total effective resonance integral was obtained. (N is the number of groups and Δu_j the group lethargy widths.) For materials other than U^{238} , infinite dilution resonance integrals were assumed.⁽³²⁾ A 1.4-barn $1/v$ correction has been added to the value $I_{eff11}^{28} = \Delta u_{11} \sigma_{c11}^{28}$ before inserting in Eq. (A-5).

The group 11 transfer cross section $\Sigma_{11}^{removal}$ was obtained by means of the expression

$$\Sigma_{11}^{removal} = \frac{p^{28}}{1 - p^{28}} \Sigma_{c11}^{28} \quad . \quad (A-9)$$

Pseudo cross sections were used to adjust Σ_{c11}^{28} and $\Sigma_{11}^{removal}$ obtained from the cross sections of Table 11 so as to force agreement with the above considerations for the various systems considered.

In one case for the clustered water-steam system and for the sodium-graphite system, I_{eff}^{28} was also obtained from the expression⁽³³⁾

$$I_{eff}^{28} = 4.15 + 26.6 \sqrt{\frac{S_{eff}}{M}} \quad , \quad (A-10)$$

where S_{eff}/M is the ratio of effective surface to mass of the oxide fuel elements. The empirical method of Carlvik and Pershagen^(33,34) was used to obtain the value of S_{eff} for use in Eq. (A-10). For the clustered water-steam case examined (2% enrichment) I_{eff}^{28} from Eq. (A-7) was 16.6 barns and from Eq. (A-10) was 14.8 barns. For the sodium-graphite case (2% enrichment) I_{eff}^{28} from Eq. (A-7) was 16.1 barns and from Eq. (A-10) was 14.7 barns. The effect of these differences on B^2 is shown in Figs. 8 and 9.

4. Thermal Group Considerations

For the clustered water-steam and sodium-graphite systems, all of the thermal cross sections were obtained by a spatial flux weighting. A diffusion theory cell calculation was used to evaluate the thermal disadvantage factors. In the clustered water-steam system, the sources in fuel and moderator zones of the cell were assumed to be proportional to the amount of H_2O present in each region. For the sodium-graphite case, a zero source was assumed for the fuel zone of the cell. Pseudo cross sections were used to adjust the thermal cross sections obtained from Table 11 to force agreement with the disadvantage factor weighted results.

The water-steam and graphite-steam systems were treated as being homogeneous so that no flux weighting was required.

5. Results

a. Critical Enrichments

The composition and geometry of the various systems examined are given in Tables 12 through 14.

Table 12

COMPOSITIONS OF SYSTEMS STUDIED

System	Material Volume Fractions						Cluster Geometry	
	UO ₂	Fe	H ₂ O ^a	C	Na ^b	Zr	Fuel Zone	Moderator Zone
							Radius, cm	Radius, cm
H ₂ O-Steam	0.219	0.079	0.34041	0	0	0	-	-
C-Steam	0.219	0.079	0	0.438	0	0	-	-
Clustered H ₂ O-Steam	0.240	0.072	0.28776	0	0	0	3.683	5.067
Na-C	0.044	0.006	0	0.8927	0.0551	0.0011	4.229	13.216
Flooded	0.240	0.072	0.688	0	0	0	3.683	5.067

^aEquivalent normal density H₂O.

^bNa temperature assumed to be 839°K. The amount 0.0537 is used with the Na values given in Table 11.

Table 13

CELL COMPOSITIONS

System	Material Volume Fractions							
	Fuel Zone				Moderator Zone			
	UO ₂	Fe	H ₂ O ^a	Na ^b	Fe	Zr	H ₂ O ^a	C
Clustered H ₂ O-Steam	0.438	0.080	0.011525	0	0.06326	0	0.61762	0
Na-C	0.42836	0.03632	0	0.53532	0.00276	0.00141	0	0.99304
Flooded	0.438	0.080	0.482	0	0.06326	0	0.93674	0

^aEquivalent normal density H₂O.

^bNa temperature assumed to be 839°K. The amount 0.5218 is used with the Na values given in Table 11.

Table 14

FUEL CLUSTER GEOMETRY

System	Fuel Pins Per Cluster	Fuel Diameter, cm	Stainless Steel Clad Thickness, cm	Fuel Pin Pitch, cm
Clustered H ₂ O-Steam	19	1.168	0.0508	1.448
Na-C ^a	19	1.27	0.0254	1.6916

^a0.0254 cm Na bond between fuel pin and cladding.

For each system, various enrichments were considered and fundamental mode calculations performed for each enrichment. Table 15 compares the group 11 portion of the U^{238} effective resonance integral, the group 11 resonance escape probability p^{28} , and the thermal disadvantage factor for the various systems, all at 2% enrichment. The resonance group values are quite insensitive to enrichment in the range considered. The thermal disadvantage factor on the other hand is strongly dependent on enrichment. For 5% enrichment, the clustered water-steam and sodium-graphite systems have thermal disadvantage factors of 2.11 and 2.15, respectively.

Table 15

COMPARISON OF I_{eff11}^{28} , p^{28} , AND THERMAL
DISADVANTAGE FACTORS AT 2% ENRICHMENT

System	I_{eff11}^{28} , ^a b	p^{28}	$\bar{\phi}_1/\bar{\phi}_0$
H ₂ O-Steam	27.1	0.758	-
C-Steam	19.4	0.106	-
Clustered H ₂ O-Steam	12.3	0.850	1.49
Na-C	11.8	0.808	1.55
Flooded	23.3	0.878	2.00
Dry Na-C	10.8	0.822	1.50

^aUsing Eq. (A-7)

The results of the fundamental mode calculations are shown in Figs. 7 through 9. For each curve, the geometrical buckling B_g^2 is indicated by a horizontal line crossing the curve. In Figs. 8 and 9 the influence of the change in I_{eff}^{28} [due to use of Eq. (A-10)] is shown. Figure 9 also gives the effect of replacing the Zr with Fe in the Na-C system. At constant B^2 , the replacement of Zr with Fe produces a Δk_{eff} of about -2%. This would require an incremental increase in enrichment of about 0.2% to maintain a critical system.

If one assumes the Eq. (A-10) results for the clustered cases, the critical enrichments for the H₂O-steam, graphite-steam, clustered H₂O-steam, and Na-graphite systems are about 2.2%, 10%, 1.8%, and 2.4%, respectively. These results are, of course, not only dependent on the cross sections used and the various calculational procedures, but also on the assumed reflector savings for the various systems.

FIG. 7
 B^2 vs. ENRICHMENT FOR H₂O-STEAM AND C-STEAM SYSTEM

FIG. 8
 B^2 vs. ENRICHMENT FOR CLUSTERED H₂O-STEAM SYSTEM

FIG. 9
 B^2 vs. ENRICHMENT FOR Na-C SYSTEM

b. Sodium-Graphite System with Uranium Carbide Fuel

The sodium-graphite system was also examined briefly for the case of uranium carbide fuel instead of the uranium oxide fuel. Using Vernon's equivalence factors⁽³⁵⁾ and Eq. (A-10), one calculates 7.93 barns for I_{eff}^{28} in the case of 2% enrichment. This decreased value of I_{eff}^{28} as compared with that for UO_2 [10.4 barns using Eq. (A-10)] is compensated for by the increased atomic density of UC as opposed to that of UO_2 . Assuming the same fraction of theoretical density for UC and UO_2 , the resonance group parameters (p , Σ_{C11}^{28} , $\Sigma_{\text{11}}^{\text{removal}}$) are found to be essentially unchanged when UO_2 is replaced by UC.

In the case of the thermal group, the increased uranium density results in a thermal disadvantage factor of about 1.73 for the 2% enrichment case and a thermal value for η_f of 1.469. The UO_2 system has a value for η_f of 1.420. Hence, a carbide fuel system will have a critical enrichment of perhaps 0.4% less (1.6% against 2.0%) than a comparable oxide system, simply owing to the change in thermal utilization.

c. Flooding of Clustered Water-Steam System

The clustered water-steam system was examined briefly for the case of room-temperature water in both the fuel and moderator zones of the cell. The composition of such a flooded system is given in Tables 12 and 13 and the system is designated "flooded." The values of k_{eff}^{28} , p^{28} , and the thermal disadvantage factors are given in Table 15. The increase in $\bar{\phi}_1/\bar{\phi}_0$ is due mainly to the increased value for the fuel zone thermal κ ($3\Sigma_{tr}\Sigma_a$), which determines the ratio of thermal flux at the fuel zone outer radius to the average in the fuel zone. The influence of κ overrides the effect of the large increase of the source in the fuel zone of the cell.

If one assumes 28 cm² for the age in the flooded system and 100 cm² for the age in the operating system, then for a value of $B^2 = 0.000452$, it is found that values of $k_{eff} \sim \eta_{fp} (1 + L^2 B^2)^{-1} (1 + \tau B^2)^{-1}$ for the hot operating and flooded cases are, respectively, 1.005 and 0.940. Hence, under the approximations utilized, the effect of going from the operating clustered water-steam system to a room-temperature flooded system is a loss of about 6% in k_{eff} .

d. Loss of Sodium from Sodium-Graphite System

The sodium-graphite system was examined for the case of no sodium coolant. The composition of such a system is the same as for the Na-C system in Tables 12 and 13 except that the sodium volume fraction is set equal to zero. Table 15 gives the resonance and thermal parameters for this case (designated dry Na-C). Assuming that the age is unchanged when the sodium is absent, the change in k_{eff} due to loss of Na is about plus 5%. This dry Na-C system (really just a graphite system) might be considered as the starting point for a clustered graphite-steam system.

6. Discussion

The results obtained depend upon the cross sections used and upon the calculational techniques employed. The thirteen-group set of cross sections was originally designed for use in a steam-cooled fast plutonium oxide core. The thermal cross sections as mentioned above have been obtained by merely shifting a Maxwellian distribution to account for temperature changes. Although the cross sections used may be adequate for the heavily loaded graphite-steam system, the other, more thermal systems would probably be more properly analyzed with the use of cross sections appropriate to the individual system spectra. In particular, proper two-or three-group cross sections would no doubt be adequate for the thermal systems. Further calculations should be preceded by selection of such suitable cross sections.

The resonance group considerations require further examination. In particular, the calculation of I_{eff}^{28} for the clustered geometries should be more carefully done. For example, the possibility of interaction between clusters should be studied. The resonance escape probability is, of course, given by the product of all of the p^i , where i pertains to the various materials of the mixture. Our approximation [Eq. (A-9)] should be replaced by

$$\Sigma_{transfer} = \frac{p}{1 - p} \Sigma_a \quad ,$$

where $p = \frac{\Sigma_i p_i}{\Sigma_i p_i + \Sigma_a}$, Σ_i is the total fission plus capture cross section for the resonance group, and $\Sigma_{transfer}$ is the resonance group transfer cross section. By the use of fewer groups, the whole of the calculated I_{eff}^i can be used in the calculation of the p^i .

The thermal disadvantage factor for the clustered systems may have a strong influence on the thermal utilization. For the more heavily absorbing systems, transport theory calculations of $\overline{\phi}_1/\overline{\phi}_0$ may be expected to yield substantially different results. The calculated results for flooding the clustered water-steam system should be re-examined using a more careful calculation of the thermal flux depression.

The areas for further physics studies are numerous. A few of the more obvious are: control studies, xenon and samarium poisoning effects, geometric enhancement of the fast effect owing to clustering, burnup and core lifetime considerations, and calculations of conversion ratios.

APPENDIX B

COMPARISON OF DIRECT AND INDIRECT SUPERHEATER
REACTOR COOLANT CYCLES

As part of the study, a comparison was made of direct and indirect superheater reactor coolant cycles. For the calculations it is assumed that the superheater is supplied with saturated steam at 163 atm (2400 psia) (sat. = 350°C, 662°F) and the steam is heated to 566°C (1050°F) by the superheater. The temperature rise of the coolant to the hot spot is given by,

$$\Delta t_c = (t_2 - t_1) \left(\frac{x}{\ell} \right)_h F_b \quad . \quad (B-1)$$

The average heat flux is,

$$q = 4P/\pi \ell ND^2 \alpha \quad , \quad (B-2)$$

and since $\alpha = 4/D$ for rod type elements,

$$q = P/\pi \ell ND \quad . \quad (B-3)$$

Inserting the power distribution and heat flux peaking factors, the maximum heat flux is given by

$$q_m = PR_a R_r F_f / \pi \ell ND \quad , \quad (B-4)$$

Thus, the film drop at the hot spot is,

$$\Delta t = \frac{q_m C_1}{h} = \frac{PR_a R_r F_f C_1}{\pi \ell NDh} \quad . \quad (B-5)$$

The hot spot surface temperature is given by the sum of Eqs. (B-1) and (B-5) and the coolant inlet temperature:

$$t_s = t_1 + (t_2 - t_1) \left(\frac{x}{\ell} \right)_h F_b + \frac{PR_a R_r F_f C_1}{\pi \ell NDh} \quad . \quad (B-6)$$

Equation (B-6) gives the maximum surface temperature for the direct cycle. A similar equation is obtained for the indirect cycle by adding the heat exchanger drop:

$$t_s = \Delta t_e + t_1 + (t_2 - t_1) \left(\frac{x}{\ell} \right)_h F_b + \frac{PR_a R_r F_f C_1}{\pi \ell NDh} \quad . \quad (B-7)$$

In making the comparisons, the temperature profiles for all direct and all indirect cycles are constant due to the maximum surface temperature limitation. This determines a unique value of Δt_m for each case; thus,

$$\Delta t_m = \frac{PR_a R_r F_f}{\pi \ell NDh} = \text{constant} \quad (\text{B-8})$$

The criterion for the comparison was that the fuel surface temperature should be 677°C (1250°F) for both the direct and indirect cycles. Solving Eq. (B-6) by trial and error gives $\Delta t_m = 136^\circ\text{C}$ (245°F) for the direct cycle. Likewise, for the indirect cycle, assuming a mean heat exchanger difference of 59°C (106°F), Eq. (B-7) gives $\Delta t_m = 89^\circ\text{C}$ (161°F). The calculated temperature profiles for the direct and indirect cycles are plotted in Fig. 10.

FIG. 10
TEMPERATURE PROFILES FOR DIRECT AND INDIRECT
REACTOR COOLANT CYCLES

Having fixed the temperature profiles for the two cases, it is possible to vary parameters, holding Δt_m constant. The number of fuel rods is given by

$$N = \frac{PR_a R_r F_f}{4\pi \ell \int k(\theta) d\theta} \quad (\text{B-9})$$

Substitution of Eq. (B-9) into Eq. (B-8) gives

$$\Delta t_m = \frac{4 \int k(\theta) d\theta}{Dh} \quad (B-10)$$

Similarly, an equation for the average fuel power density is obtained:

$$Q = \frac{P}{V} = \frac{4P}{N\pi D^2 \ell} = \frac{16 \int k(\theta) d\theta}{D^2 R_a R_r F_f} \quad (B-11)$$

Thus Eqs. (B-10) and (B-11) determine relationships between Q , h , and D .

Figure 11 is a plot showing graphically the relationships determined by Eqs. (B-10) and (B-11) for a range of power densities, heat transfer coefficients, $\int k(\theta) d\theta$, and rod diameters. Examination of Eq. (B-10) shows that horizontal lines on the chart are essentially lines of constant heat flux. The discontinuity at the $\int k(\theta) d\theta = 20$ line is due to the reduction in Δt_m between the direct and indirect cycles. The values for $\int k(\theta) d\theta$ indicated on the curves are the maximum values in the core and hence have taken account of the overall maximum-to-average power ratios and the heat flux peaking factor.

The following example illustrates the use of the chart. Say a direct steam-cooled reactor has fuel rods, 0.64 cm (0.25 in.) in diameter, and the heat transfer coefficient is 0.312 watt/(cm²)(°C) [550 Btu/(hr)(ft²)(°F)]. From Fig. 11, the fuel thermal rating would be 5 watts/cm and the fuel power density is 65 kw/liter. Thus there is free choice for two parameters, i.e., for the limits of surface temperature imposed by the chart, the power density and fuel thermal rating are fixed upon selecting a heat transfer coefficient and rod diameter. Suppose, further, that it is desired to double the fuel thermal rating from 5 watts/cm to 10 watts/cm with the same heat transfer coefficient of 0.312 watt/(cm²)(°C) [550 Btu/(hr)(ft²)(°F)]. As shown, this necessitates an increase in rod diameter by a factor of 2 and a decrease in power density by a factor of 2. Decreased power density and increased rod diameter may appear at first examination to be incongruous with increased fuel rating, but this results from holding the same surface temperature limitation. Doubling the fuel thermal rating requires that the number of fuel rods be halved. Since the heat flux is constant, the heat transfer area must be held constant, and the rod diameter must be doubled. Doubling the rod diameter and halving the number of rods results in a net doubling of the total volume of fuel, which results in a reduction in power density by a factor of 2. Of course, in the practical case, one tends to go in the opposite direction, namely, maximizing fuel power density, and this requires rods of smaller diameter, and hence lower fuel thermal ratings for a given heat flux.

FIG. 11
 COMPARISON OF DIRECT AND INDIRECT REACTOR COOLANT CYCLES FOR DIFFERENT
 HEAT TRANSFER PROPERTIES OF COOLANT

As shown, the chart of Fig. 11 can be used for estimating fuel thermal rating and power density given the heat transfer coefficient and rod diameter of a nuclear superheater. Some further conclusions can be drawn. Note that for a liquid metal-cooled reactor where h might be 10,000 and the rod diameter is 1.27 cm (0.50 in.), the fuel specific power is 380 kw/liter. For a direct steam-cooled reactor with $h = 500$, and the same size of rod, it is reduced to 30 kw/liter. This advantage on specific power reduces considerably when considering fuel-to-moderator ratios for the two cases. If the direct-cycle reactor is water moderated, the overall power density is about 7.5 kw/liter, whereas if the indirect cycle reactor is graphite moderated, the overall power density is 17.3 kw/liter. The surface temperature is limiting in the direct cycle as the fuel rating is only about 9 watts/cm, which is considerably below the maximum permissible for oxide fuels for centerline melting to occur. Thus, in the case of a reactor that is limited by surface temperature, the power density can be increased by the use of multipass flow and/or decreasing the fuel rod diameter. As can be seen from the chart, the use of coolants with good heat transfer properties necessitates the use of fuels with better thermal conductivities, and hence higher permissible values of $\int k(\theta)d\theta$. Thus, the use of metallic or carbide fuels is indicated for the better coolants.

NOMENCLATURE

Δt_e	temperature difference in heat exchanger
Δt_m	maximum fuel to coolant temperature difference
Δt_c	coolant temperature rise
t_s	fuel surface temperature
t_1	coolant inlet temperature
t_2	coolant outlet temperature
$(x/\ell)_h$	fractional distance along coolant channel to hot spot
q	heat flux
P	reactor power
ℓ	channel length
D	fuel rod diameter
N	number of fuel rods
α	fuel surface to volume ratio
h	heat transfer coefficient
V	volume of fuel
Q	power density
C_1	correction factor, allowing for less than peak heat flux at hot spot - based on axial chopped cosine of 1.5 max/avg
R_a	axial max/avg power ratio = 1.5
R_r	radial max/avg power ratio = 2.0
F_b	hot channel factor = 1.4
F_f	heat flux peaking factor = 1.4
$\int k(\theta)d\theta$	fuel heat generation per unit length of fuel - watts/cm

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