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JUNE, 1978

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MARCH 1978
STATUS REPORT ON THE
PRELIMINARY CONCEPTUAL DESIGN
OF A
DEMONSTRATION TOKAMAK HYBRID REACTOR
(DTHR)

JANUARY 1978 - MARCH 1978

DoE CONTRACT NO. EG-77-C-02-4544

fusion power
systems department

Westinghouse Electric Corporation
P.O. Box 10864, Pgh. Pa. 15236

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SECTION 1.0 INTRODUCTION

A serious problem confronting this nation and most of the other nations in the world is the need to develop effective means of meeting future energy requirements. It is both timely and important to demonstrate the practical application of a fusion reactor for the production of fissile fuel to assure an adequate supply of fuel for the proven nuclear energy option. Numerous studies conducted during the past several years indicate an impending shortfall of electrical generating capacity in the near future. The results of a recent EPRI analysis⁽¹⁻¹⁾ of projected energy demand and the likely electricity generation mix are shown in Figure 1-1. This projection predicts a potential energy shortfall shortly after the turn of the century. Rhetorical arguments over exactly when the shortfall will occur seem to have obliterated the real need, energy sources. Visions of new sources suddenly appearing and being fully operation are diluting efforts toward concentrated progress on proven or highly potential energy sources.

The EPRI projections presented in Figure 1-1 were selected as a reasonable basis for long range energy planning on the part of the utilities. The electricity generation demand shown in this data is based on a growth rate of 5.6% per year from now to the end of the century. This compares to historical growth data for the electric utility industry of about 7% per year. Therefore, some decline is forecast in the near-term future due to curtailment of demand caused by higher energy costs and the effect of conservation. After the turn of the century, EPRI utilizes a substantial reduction in the demand forecast resulting in a projected growth rate of 3.3% per year. This magnitude of reduction in growth was selected primarily to reflect the effects of an intensive program of energy conservation.

Electricity generation mix shown in Figure 1-1 indicates that contributions from oil and natural gas will diminish after the mid-1980's and be phased out shortly after the turn of the century. Hydro, geothermal and solar represent a growing but small contribution to the central station electrical generation mix (as opposed to residential heating and cooling, for example,

Figure 1-1 EPRI Projected Electricity Generation Mix⁽¹⁻¹⁾

in the case of solar energy). Coal represents an increasing source of electrical generation through the rest of this century. This will require a substantial increase in mining and associated facilities. The contribution of coal to the total generation mix levels off, however, after the turn of the century. This limitation in the growth of coal is caused by competition for coal as a resource in other energy applications. However, other considerations such as increasingly stringent air pollution control requirements and concerns with regard to the global effects of CO_2 release to the atmosphere could also lead to the limitation of coal usage as a basic energy resource for electrical generation. The balance of the demand for electrical generation must be provided by some form of nuclear power.

Contributions from nuclear power consist of three increments. The base case is comprised of 1.8 million tons of U_3O_8 (generally conceded to be a conservative, but possibly very realistic, estimate of known plus undiscovered domestic reserves) and no fuel reprocessing. The next increment represents a total of 3.5 million tons of U_3O_8 , which roughly corresponds to previous ERDA estimates of domestic resources, and fuel reprocessing. The third nuclear increment represents the LMFBR with commercial introduction assumed to begin at the turn of the century. For the first ten years, LMFBR introduction is limited by the rate at which a new technology can be introduced, as constrained by the development of vendor capability to manufacture components such as new types of pumps, valves, heat exchangers and pressure vessels. After that time, the rate of LMFBR introduction is limited by the availability of fissile fuel resources (plutonium, in this instance) to provide startup inventory. Even with these three increments of nuclear power, a shortfall is seen to develop beginning at about the year 2000. This shortfall increases in magnitude during the first two decades of the next century. If certain of the key assumptions in these projections for nuclear power are not realized, e.g., if the LMFBR is delayed or cancelled, fuel reprocessing capability is not developed and/or U_3O_8 resources of 3.5 million tons are not available, the potential shortfall could become significantly more serious. The relevance of these data with regard to the hybrid is to provide a perspective showing the gap in energy resources that could be filled by an alternate means of fissile fuel production

and to set a time frame in which that source must be developed to avoid having such a shortfall occur. In this framework, the hybrid would need to be demonstrated by about 1990 to insure that deployment of the technology to provide fuel on a commercial basis could be insured by the year 2000. This is the basic scenario on which the DTHR feasibility study described in this report is predicated.

The matter of uranium resources and the restriction of certain nuclear options due to perceived concerns regarding nuclear weapons proliferation have been the subject of much recent controversy. Furthermore, the hope has been expressed that a massive effort to implement conservation practices will have a dramatic effect in reducing historical growth trends in electrical demand. The scenario shown in Figure 1-2 is an attempt to synthesize an alternate forecast to test the effects of these assumptions on the projected energy shortfall. The demand for installed capacity is based on the "ERDA Conservation Case" that reduces the contribution of nuclear power to 330 GWe installed capacity by the year 2000. This compares to about 500 GWe nuclear from the EPRI analysis. Beyond the year 2000, the EPRI assumptions of 3.3% annual growth in electrical demand and a limitation in the contribution of non-nuclear generating capacity have been assumed. Nuclear options have been restricted using the guidelines of the present Administration, namely no fuel reprocessing and no fission breeder (i.e., no LMFBF). It is assumed that these policies will continue to prevail in the time frame of the scenario of Figure 1-2. With these assumptions and the ERDA U_3O_8 estimate of 3.7 million tons, the required generating capacity can be provided only to the year 2002. It is interesting to see the effect that three times the ERDA forecast of undiscovered uranium reserves has on this projection. (The Ford-Mitre Study⁽¹⁻²⁾ concluded that ERDA data may underestimate U_3O_8 reserves by a factor of two to three). If it is assumed that three times the ERDA forecast is correct, then the capacity limit is reached in 2018, an extension of 16 years. Therefore, the total impact of revised U_3O_8 estimates, even those as optimistic as presented in the Ford-Mitre Study, is a postponement of the shortfall in energy resources by somewhat more than a decade. Should these assumptions prove to more accurately

Note: 0.2% tails give 3 to 5 years more time before reaching capacity limit.

Figure 1-2. Projected Electrical Generation Limitations

forecast future energy resources, the need for the hybrid might be postponed by 1-2 decades, but it would not be eliminated. This degree of uncertainty in need date is not considered significant in a long range technology such as fusion.

The ten to twelve years lead time is currently needed from the time a utility decides to build a large central station LWR system until that system goes into operation and can be counted as part of the installed capacity. Considering this, a utility whose goal is to place a plant into operation by the year 2002 might have to make a commitment to build that plant as early as 1990. In this case, demonstration of hybrid technology by the year 1990 could conceivably have a major impact on that utility's decision by insuring that a viable approach to afford an alternative source of fissile fuel was under development. Without this assurance, the utility could well be extremely reluctant to make a major investment in a LWR system, even though no viable alternatives were available to supplement the installed capacity.

The potential role of the hybrid can be more clearly defined based on work to date in this study, as well as design work which preceded the present effort. The primary role of the hybrid appears to be the production of fissile fuel. The hybrid concept is characterized by unique generic properties with regard to neutronics as contrasted to fission breeder reactors. Of the 2 1/2 to 3 neutrons per fission produced in a fission breeder, one neutron must be used to cause a successive fission (i.e., to keep the reactor critical) and one neutron must be used to replace the fissile nucleus destroyed. Therefore, somewhat less than one neutron is available to account for leakage and parasitic losses and to produce a net surplus of fissile material. The hybrid, on the other hand, is comprised of a subcritical fertile/fissile blanket which is driven by a source of high energy fusion neutrons. Thus, the blanket need not be maintained in a critical state (neutronically). Furthermore, very high energy fusion neutrons can produce as much as 4 to 5 neutrons per fission which then enhance neutron multiplication characteristics of the blanket lattice. These features tend to enhance the fissile fuel production capability of the hybrid. The production of energy assumes a secondary role in the fusion-fission system. Fission reactors are better suited to energy production as a

primary role since they are usually characterized by simple geometries and more uniform power distributions.

The above characteristics permit a forecast of the most probable role for the hybrid in future energy scenarios. With the primary role being the production of fissile fuel, the hybrid can be thought of as a replacement for the isotopic enrichment plant. The hybrid actually does more than simply replace the enrichment plant, however, since it eliminates the need for natural fissile material in the feedstock. This permits the beneficial use of thorium and/or uranium tails without requiring that supplementary fissile material be provided to convert these feedstocks to fissile fuel. Thus, the hybrid can serve as an alternate source of fuel for LWR's, drawing on a huge resource base for feedstocks. This point is extremely significant in view of the large present utility investment in LWR power stations and the future commitments for construction of such facilities.

In the longer term, it will still be necessary to consider the efficiency with which fissile fuel resources are utilized in the generation of nuclear power. While the hybrid gives access to a large resource base, it also involves a comparatively complicated and sophisticated technology. Thus, there will be a need to use fissile fuel generated by the hybrid in as efficient a manner as possible. LMFBR technology best suits this need since years of development have been invested in making the LMFBR the most efficient user of fissile material for power production that has been created by the nuclear energy program. Viewed in this context, the hybrid complements the fission breeder rather than serving as a replacement for fission breeder technology. One scenario to illustrate this point is given by the EPRI forecast presented in Figure 1-1. The shortfall in startup inventory projected to occur after the first decade of the next century could be provided by hybrid systems, thus permitting accelerated introduction of LMFBR's to minimize the potential shortfall in electricity generation. Another option might be to afford some relief in the breeding performance of the LMFBR, and thereby to realize a reduction in LMFBR costs, by relying to a greater extent on the hybrid to

produce fissile fuel. A "break-even" fission breeder (i.e., infinite doubling time) or even a near breeder can be envisioned depending on economic trade-offs of the coupled hybrid/fission breeder symbiosis. Thus, an entire spectrum of scenarios worthy of further study can be formulated. The key point is that the hybrid provides an additional degree of freedom that can have a major impact on the utilization of nuclear energy to meet future energy requirements.

The study reported herein builds on several prior studies, but with significant differences in objectives. The main thrust of the present work is to define the preliminary conceptual design for a near-term Demonstration Tokamak Hybrid Reactor (DTHR) which could be the precursor to a viable first generation of commercial hybrid plants to produce fuel on a commercial basis.*

Although advanced, high performance fuel materials, high temperature coolants and advanced structural materials may offer the potential of higher fissile production rates and more efficient thermal-to-electrical power conversion in the long run than is possible with the reference concept, there is clearly evidence from the work completed thus far and from the development history of fission power reactors that a less-than-optimum system in terms of performance may actually be more viable commercially. As a consequence, an important ongoing goal of the present study is to make maximum use of proven light water fission reactor technology. This approach would not preclude the use of advanced concepts in later stages of the study or the testing of advanced fuel modules in the DTHR.

The principal goals which have been set for the DTHR include:

- Demonstration of the production of a significant amount of fissile fuel in a proliferation-resistant form in a near-term, relatively low cost, fusion-fission reactor.
- Demonstration of reactor technology, hardware and performance that can serve as proof-of-principal for future commercial applications (hybrid or pure fusion).

* These fuel production facilities would probably be Government owned and operated in a manner similar to present isotopic enrichment facilities.

- Demonstration of long pulse plasma operation to assist in determining meaningful duty cycle factors for commercial hybrid and pure fusion applications.
- Demonstration of blanket-fuel remote handling technology to determine a basis for plant availability projections.

In addition, subsequent to the demonstration of the breeding of a significant amount of fissile fuel, the reactor as envisioned would provide an urgently needed capability for testing materials, components, systems and concepts for both advanced hybrids and pure fusion reactors. Such testing would include: 1) alternate converter blanket concepts, 2) tritium fuel breeding technology, 3) the evaluation of the potential for breakeven or net electrical power generation and/or process heat capability for future applications, and 4) extended operation as a materials and engineering test reactor.

REFERENCES-SECTION 1

1. Research and Development Program for 1978-1982 Overview, EPRI, Palo Alto, California, September 1, 1977.
2. Nuclear Power Issues and Choices, Nuclear Energy Policy Study Group, Balinger Publishing Co., Cambridge, Mass., 1977.

SECTION 2.0

SUMMARY AND CONCLUSIONS

The development and deployment of advanced energy technologies has emerged as an urgent factor in meeting the nation's future energy needs. Nuclear energy must assume a significant role in satisfying these needs to avoid excessive dependence on imported fossil fuels, insufficient conservation or undeveloped energy alternatives. The future growth of the nuclear power industry appears to be constrained by two circumventable factors; the projected insufficient quantity of low cost uranium ore, and the increasing concern regarding possible diversion of fissile materials for weapons use. Recent studies have indicated that the combination of fusion and fission in a hybrid system can supply more than adequate amounts of fissile material from existing fertile materials in a proliferation resistant fuel form. The flexibility of the hybrid to use alternative fuel cycles and its ability to function as a fuel production facility, power plant, waste disposal burner or combinations of all of these, as well as its potential near-term availability, provides a strong incentive for the development and assessment of the concept.

The Demonstration Tokamak Hybrid Reactor(DTHR) preliminary conceptual design has been developed to provide the basis for subsequent assessment of the long-range potential of the hybrid concept. In the study efforts thus far, the following tasks have been achieved:

- The goals and objectives for DTHR were established.
- The baseline design approach for each major DTHR subsystem has been selected and scoping analyses performed.
- The computer code (COAST) modifications to couple hybrid performance and system costs have been modeled.
- A first iteration preconceptual design has been completed.
- Key feasibility issues have been assessed for the fusion driver and the hybrid blanket.

- An initial thorium-uranium 233 fuel cycle scenario has been evaluated.

The study has shown that the DTHR is not only feasible for the late 1980's, but that a significant quantity of fissile fuel can be produced from fertile thorium using present day fission reactor blanket technology in what appears to be a relatively low cost reactor concept. The projected schedule⁽²⁻¹⁾ for DTHR is shown in Figure 2-1, which includes the existing and supporting programs as well as a possible path to commercialization. Early cost estimates based on scaling down cost estimates from the TCT Hybrid Reactor Study⁽²⁻²⁾ done for EPRI indicate that DTHR could be constructed at a capital cost of less than \$1B. Preliminary cost estimates from the COAST code seem to substantiate this cost estimate.

The DTHR preliminary conceptual design consists of a magnetically confined fusion reactor fitted with a fertile thorium blanket. The fusion driver concept is based on a beam driven plasma, but at sufficiently high plasma densities that neutrons originating from the interactions of bulk plasma ions contribute significantly to the wall loading. The tokamak has a major radius of 5.2 m, a minor radius of 1.2 m, and the elongation is 1.6. All of the magnetic coil systems are superconducting Nb₃Sn based on the Large Coil Project (LCP) technology. The toroidal field (TF) coils employ an innovative concept, the "compact D" configuration. Although similar to the constant-current D concept, an hourglass shaped bucking cylinder is used to enable a reduction in the overall height of the TF coils. An engineered bundle divertor concept has been developed based on the bundle divertor design techniques developed for TNS and ISX-B. A thermal power of 150MW of 200 keV deuterium is injected into the plasma through six ducts of a positive ion, neutral beam injection system (NBIS). A water cooled, 316 stainless steel vacuum vessel concept was developed and initial scoping analyses look encouraging. The fissile fuel handling system was evaluated and defined. Details of the tritium injection system remain to be developed. Tritium breeding will be assessed in subsequent phases of the DTHR operation. The fusion driver provides a neutron first wall loading of 2MW/m² for fissile production in the blanket. The plasma fusion power is 950MW (neutron power = 760MW) from a

Figure 2-1.

Key Elements of the Tokamak Hybrid Reactor Development, Demonstration and Commercialization Program

13keV plasma, which has an $n_e \tau_E$ of $7.5 \times 10^{19} \text{ m}^{-3} \text{ s}$. Some of the preliminary fusion driver design features are shown in Table 2-1. Other DTHR System features and parameters are highlighted in Table 2-2.

An extensive assessment of the application of fission technology to the hybrid reactor blanket was made. Criteria relating to high operational reliability, minimal fabrication development requirements, simple design characteristics and applicability to commercial operation were established. Several concepts were evaluated and a prime reference concept using thorium (ThO_2) fuel rods clad in zircaloy based on the LWBR (Shippingport) blanket fuel rods was selected. The module design consists of a simple, rectangular two-zone zirconium structure about 4 m long. The front zone (plasma side) is 10 cm thick with a tighter lattice than the 15 cm thick outward zone. These vertical modules are placed in 27 locations around the periphery of the vacuum vessel. Even with this modest placement of fertile fuel, a fissile production rate exceeding 200 kg per year at concentrations of ^{233}U greater than 0.2% was achieved. The fissile production with metallic thorium fuel rods was ~20% higher. The coolant was selected as low temperature, low pressure water in line with the low cost, near-term technology goal of the DTHR. Performance with helium as coolant indicated a potential for 20% improvement in fissile production if the blanket thickness was increased from the 25 cm used as the reference case. The key blanket parameters are listed in Table 2-3.

The pulsed operation of the hybrid results in cyclic stresses in the fuel and clad as well as the module structures. While preliminary analysis indicates that stress and fatigue limits are not exceeded, additional design refinement and analysis will be required to assure conservative design margins, especially in a higher pressure commercial application. A simple, once-through coolant system was assumed for the initial evaluation of the DTHR. However, environmental and economic considerations will have to be evaluated more thoroughly to determine if this approach is satisfactory. A simple extrapolation of the DTHR concept to a commercial application was made, which indicated that one hybrid could provide make-up fuel for over 5 present day type PWR's (1000MWe per unit) per year using the chemically separable $\text{Th}-^{233}\text{U}$ fuel cycle and almost 4 PWR's (1000MWe) per year using the denatured $\text{Th}-^{233}\text{U}$ fuel cycle. This was based on the hybrid producing 1300 kg per year of ^{233}U .

TABLE 2-1

PRELIMINARY DTHR FUSION-DRIVER DESIGN FEATURES

Plasma Major Radius	5.2 m
Plasma Minor Radius	1.2 m
Plasma Elongation	1.6
Toroidal Field on Axis	5.5 T
Plasma Current	5.1 MA
First Wall Neutron Loading	2.0 MW m ⁻²
Fusion Power	950 MWt
Neutral Beam Energy, Power	200 keV, 150 MW
Plasma Pulse Duration, Period (Max.)	70 s, 85 s
Duty Factor x Plant Availability (Annual)	0.20
Vacuum Vessel	Water-cooled 316 SS
Plasma Exhaust	Bundle Divertor
Divertor Particle Collection	ZrAl and Cryopanel
TF, PF and Divertor Coil Conductors	Superconducting Nb ₃ Sn
Coil Conductor Cooling	Forced Flow Supercritical Helium
TF Coil Number	16
Peak Field at TF Windings	12 T
SF Winding Location	External to TF Array

TABLE 2-2

OTHER DTHR SYSTEM FEATURES AND PARAMETERS

PLASMA OPERATION

Plasma Pulse Duration, s	70
Effective Burn Duration, s	63
Recycle Time, s	15
Design Basis Duty Cycle Factor	0.82
Average Duty Cycle Factor	0.50
Design Basis Plant Availability	0.40
Design Basis Number of Pulses/Year	10^5
Pulse Scenario:	
Pulse OH (Bipolar)	2 seconds
Neutral Beam Injection (Heating to steady state)	5 seconds
Burn (with NBI)	63 seconds
Shutdown	2 seconds

TOROIDAL FIELD COILS

Number of Coils	16
Conductor Type	Nb ₃ Sn
Coil Shape	Compact "D"
Peak Field at Conductor	12 T
Current per Turn	15 kA
Average Current Density	4.1 kA/cm ²
Cooling Mode	Forced Flow Supercritical He
Structural Material	Stainless Steel

POLOIDAL FIELD SYSTEM

Conductor Material	Nb ₃ Sn
Location	Outside TF
OH Flux Swing	53 v-s
Conductor Current	16 kA
Average Current Density	4.4 kA/cm ²
Cooling Mode	Forced Flow Supercritical He

TABLE 2-2 (continued)

PLASMA EXHAUST SYSTEM AND
IMPURITY CONTROL

Bundle Divertor

Single Toroidal Position
at Torus Mid-plane, Super-
conducting Nb₃Sn "D" Coils,
On-line Regenerable Zr Al
Gettering, Cryo-panels.

Throughput to Divertor Burial Chamber

$\sim 3 \times 10^{22}$ particles/s

VACUUM VESSEL

Vessel Material

316-SS

Construction

Rib stiffened shell

Vessel Cross Section

D-Shaped

Wall Thickness (Shell)

0.4 cm

Toroidal/Poloidal Limiters

Tungsten Rails (Radi-
ation Cooled)

Peak Bulk Wall Temperature

380°C

Vessel Coolant

Pressurized Water

VACUUM PUMPING

Pumping System

Cryosorption Pumps or Panels

Base Pressure

1×10^{-8} torr

TRITIUM SYSTEMS

On-site T₂ Inventory

One Day

Tritium Storage

Uranium Chip Beds

Fuel Recovery-Recycling Plant

Off-site

Tritium Cleanup Systems

Reactor Torus and Cell

Fueling

Prefill: Gaseous T

Refueling: T Pellets &
D⁰ ions

TABLE 2-2 (continued)

NEUTRAL BEAM INJECTION LINES

Beam Power, Energy	150 MW, 200 keV
Single Arm Specifications:	
Sources	8-125 A Positive Ion
Vacuum Enclosure	7.5 m long x 3.5 m wide x 4.1 m high
Number of Arms	6
Vessel Aperture Per Line	1.4 m x 0.8 m

HEAT DISSIPATION SYSTEMS

Facility Heat Sink	Once-through Natural Source
Vacuum Vessel Coolant	Pressurized Water
Shield Coolant	Borated Water
Cryogenic Refrigeration Systems	L He; L N ₂
Facility Air Conditioning	Fixed Base Load, Reactor Cell Air Conditioning

ELECTRICAL SYSTEMS

Facility Feed	Fixed Utility Constraints
S/C TF Supplies	Line-driven Rectifier Supplies
Neutral Beam Supplies	Line-driven Rectifier Supplies with Load Dumping Resistors
Poloidal Field Power Supplies	AC-MG Storage-Rectifier Conversion

TABLE 2-3.

DTHR BLANKET DESIGN FEATURES

Fuel Form	Thorium Oxide or Metal Rods
Clad	Zircaloy
Module Construction	Rectangular, Two Zone, Zr, LWBR Technology
Coolant	Low Temperature and Pressure Water

^{233}U Production (BOL)

ThO_2	200 kg/yr. (~0.2% conc.)
Metallic Th	> 20% (over ThO_2)
He Coolant	> 20% (over Water)

Tritium Breeding	] Provide for in a Phased Program
Tritium Recycle System	
Material and Component Testing	
Alternate Fuel Modules	
Electrical Power Generation	
Synthetic Fuel Test Modules	

with an average concentration of 0.65% in the ^{232}Th . The tritium breeding ratio was projected to be 0.77 in the hybrid reactor. If metallic fuel and helium cooling were used, the hybrid fissile production rate would be sufficient to supply 6 to 8 PWR's (1000MWe) per year, depending on the assumed fuel cycle. Of course, with advanced reactors having higher conversion ratios, make-up fuel could be provided for a larger number of fission reactors.

Core calculations and safety analyses were made using thorium fuel enriched with hybrid bred ^{233}U in a standard pressurized water reactor (PWR) utilizing the oxide fuel form. Highly satisfactory performance is predicted. The make-up fissile fuel required is less than for the standard fuel cycle. No safety, performance or capital cost penalties have been identified. An average conversion ratio of 0.74 for the chemically separable fuel cycle and 0.78 for the denatured fuel cycle were determined as compared to the present day PWR designs which average 0.60. The hybrid must supply 255 kg/yr ^{233}U for the PWR on the chemically separable fuel cycle and 340 kg/yr ^{233}U for the denatured fuel cycle. It was determined that by replacing the pyrex burnable poison pins with LiF pins in the PWR's supplied by the hybrid, the PWR's (~ 7 , 1000MWe) on these fuel cycles could produce as much as 38% of the tritium burned by the hybrid. This combined with the projected 0.77 tritium breeding ratio in the hybrid would result in an adequate tritium fuel supply. One on-site PWR could probably supply the electrical power required by the hybrid, if the hybrid does not produce its own power.

The definition of the fusion driver, the related tokamak auxiliary systems, the hybrid blanket concept and performance, and the fission reactor fuel cycle considerations are presented in detail in the text of the report.

The planned effort for the remainder of the fiscal year includes completion of the verification of the COAST code modifications, performing trade studies on the overall system performance involving major design parameters and operating scenarios, and evaluating the sensitivity of the subsystem designs with respect to reactor size, performance and cost. The blanket engineering

efforts will concentrate on further development of more advanced module concepts; neutronic studies of material, geometry and exposure variations for the thorium fuel forms; parametric analysis of alternative fuels and coolants; and further refinement of the neutronic analyses tools (cross-section library, 2-D analysis, etc). The major goal of this effort is to reach a position enabling the selection of a point design which can then be studied for a commercial application.

REFERENCES

- 1) "Demonstration Tokamak Hybrid Reactor," Westinghouse Proposal WFPS-PR-77-016, November 1977(Proprietary).
- 2) "Design Study of a Fusion-Driven Tokamak Hybrid Reactor for Fissile Fuel Production", EPRI Contract RP473, (Volume II, WFPS-TME-043, May 1977).

SECTION 3.0

PRELIMINARY DTHR FUSION DRIVER CONCEPTUAL DESIGN

The concept and parameter selection for DTHR fusion driver had the objective of near-term technology and minimal development requirements. A beam driven hybrid has the advantage of significantly relaxed plasma conditions relative to an ignition reactor. Considerable effort has been made to remove unnecessary complications and to simplify the design within the constraint of effecting a meaningful and convincing demonstration reactor. PLT has already achieved most of the DTHR plasma conditions, being within a factor of five of the plasma temperature and a factor of ~ 10 of $n_e \tau_E$. TFTR will serve as a plasma prototype for DTHR with results available in the early 1980's. An overview of the fusion driver concept is presented in the next subsection and the detailed descriptions of the major systems are given in the following subsections. Subsequent subsections define the tokamak auxiliary systems, and the electrical power and energy storage systems. In parallel with this effort, the computer code COAST (Costing And Sizing of Tokamaks) was modified to include the hybrid reactor to provide self-consistent sizing data, plant and capital equipment costs, operating costs, and parametric data for trade-off and sensitivity studies. The status of this modification, called COAST 4, is presented in Appendix A.

3.1 TOKAMAK FUSION DRIVER DESIGN

The function of the fusion driver in the DTHR is to generate neutrons that will be used in the transmutation of fertile material to fissile fuel in a blanket surrounding the plasma. Test modules may also be placed in the blanket area to demonstrate tritium breeding, and for the purpose of irradiating materials to develop a data base for material properties that will subsequently be necessary for the design of fusion reactors. As discussed

in the previous progress report⁽³⁻¹⁾, various tokamak designs and modes of operation are possible for the DTHR.

In order to allow design studies to be initiated it was necessary to specify a preliminary plasma geometry and plasma operating conditions. For the reference, a flexible tokamak design has been assumed that is based on a beam driven plasma, but at sufficiently high plasma densities that neutrons originating from the interactions of bulk plasma ions contribute significantly to the wall loading. Based on the present empirical transport scaling laws, it is possible that ignition could be achieved with the reference tokamak design. In this sense, the DTHR could satisfy the goals of TNS in addition to those of the DTHR. Although the electrical power requirements and the amount of gas added to the system via the ion sources could be reduced by turning off the neutral beams if ignition were achieved, there would be a great advantage in being able to meet the neutron production goals without having to reach ignition. The operating conditions of the beam driven plasma would be easily controlled by the neutral beam power, and steady plasmas operating in new regimes could be studied without the restrictive prerequisite of achieving ignition. The reference plasma parameters and method of selection are presented in the following subsections.

Studies have also been initiated to determine the feasibility of a smaller tokamak which would operate at a relatively low plasma density and in the intensely beam driven (two component, TCT) mode. The incentive, of course, for pursuing this type of design would be potentially lower costs, less technological advancement requirements, and relaxed operating conditions for various components. Although this design would be less flexible than the reference, operate at low Q, and have potentially a smaller wall loading, it could be designed to meet the minimum goals of the DTHR. However, some serious questions have arisen concerning the operating conditions of this design. The progress of these studies is presented in Appendix B.

3.1.1 FUSION DRIVER CONCEPT/PARAMETER SELECTION

Two goals of the preliminary reference DTHR fusion driver design are to achieve a stable plasma under the required operating conditions and to produce a neutron wall loading in excess of 2 MW/m^2 . The blanket goal of producing $> 100 \text{ kg}$ of fissile fuel per year could be met with a wall loading of 1 MW/m^2 . Some guidelines that were used in defining the reference plasma are given in Table 3-1. The constraints given in Table 3-1 are not rigid, but were simply used as a guide. Each quantity will have to be examined and optimized for any final design taking into account the total design and cost impact.

An aspect ratio $A \leq 4.5$ was chosen for a number of reasons. The critical β above which a plasma is unstable, varies inversely as A based on current theories. Thus, the smallest A possible would be desirable. However, small aspect ratios are difficult to achieve in practice, especially when shielding must be provided around the torus for D-T operation. Most tokamak operating experience has been obtained with tokamaks having $A \sim 3$, and TFTR, which will provide proof-of-principle for beam driven plasmas, will have $3 \leq A \leq 4.5$ depending on the mode of operation. Thus, $A \leq 4.5$, has been used as a guide in the selection of the plasma parameters. The maximum allowed value of β has been assumed to be 6% for stable operation. It is presently accepted by many researchers in the fusion community that stable plasma operation at near-ignition conditions can be achieved for β values in the range of 5-8%. This acceptance is based on current theories, including effects resulting from flux conservation and elongated plasmas, and extrapolations from current experimental results. However, the maximum achievable β will depend on the overall tokamak design. The limitation on β_p , ($\beta_p \leq A$), was chosen to prevent a shrinkage in minor radius of the poloidal field separatrix so that a satisfactory equilibrium could be achieved. The minimum allowable safety factor, q , for MHD stability has been chosen to be $q \geq 2.5$.

As a general guideline, the plasma current was required to satisfy $I_p > 3 \text{ MA}$ in order that the drift surfaces of α -particles, which are born during fusion reactions, do not intercept the vacuum vessel.

TABLE 3 -1

GUIDELINES FOR REFERENCE DTHR PLASMA DEFINITION

(MARCH 1978)

STABLE PLASMA

ASPECT RATIO	$A \leq 4.5$
BETA	$\beta \leq 6\%$
POLOIDAL BETA	$\beta_p \leq A$
SAFETY FACTOR	$q \geq 2.5$

α - CONFINEMENT

$$I_p > 3 \text{ MA}$$

NEUTRAL BEAM PENETRATION

$$n_e a \leq 2 \times 10^{20} \text{ m}^{-2}$$

SCALING

TOROIDAL FIELD ON AXIS	$B_T > 5 \text{ T}$
PLASMA MINOR RADIUS	$a \geq 1 \text{ m}$

The product $n_e a$, where n_e is the electron density and a the plasma minor radius, is limited by $n_e a \leq 2 \times 10^{20} \text{ m}^{-2}$ so that adequate neutral beam penetration can be achieved with neutral beam energies $\leq 200 \text{ keV}$.

Since the confinement times should increase with increasing a and increasing n (or B_T since n can increase as B_T increases), it may be desirable to have the capability of achieving a toroidal magnetic field and plasma minor radius comparable to or larger than those achieved in TFTR. Thus, $B_T > 5 \text{ T}$ and $a \geq 1 \text{ m}$ were also used as guidelines.

Within these guidelines and under the engineering constraints such as providing a 20 cm thick plasma scrape-off layer, a 17 cm thick zone for the vacuum vessel, a 90 cm thick inner shield, constant tension TF coils, bucking cylinder for the TF coil, dewar and thermal insulation space, assembly gaps, blanket and outer shield, and space for poloidal field coils, the reference plasma parameters shown in Table 3-2 were obtained. The COAST⁽³⁻²⁾ code was used in establishing most of the design parameters.

A neutral beam power of 150 MW of 200 keV-D₀ injected into the plasma has been assumed. To enhance the wall loading during the intensely beam-driven mode of operation it is desirable to have as large a neutral beam power as possible. However, as a practical matter the neutral beam power must be limited. The greater the number of neutral beam arms, the smaller the blanket coverage which can be achieved. It was decided that for the reference, six beam arms would be used. Based on reasonable extrapolations from present day technology, eight sources can be used per beam arm to provide the 150 MW of injected power. There is discussed in Appendix B a possible negative effect on wall loading due to injecting an extremely high current of neutral beam. Detailed plasma calculations and cost analyses will be required to optimize the neutral beam power.

The required τ_E listed in Table 3-2 is based on an energy balance assuming $Z_{\text{eff}} \simeq 1$; the empirical τ_E is obtained from

$$\tau_E = 3.2 \times 10^{-21} n_e (sa)^2 q^{1/2}$$

TABLE 3 -2
DTHR REFERENCE PLASMA PARAMETERS
(MARCH 1978)

MAJOR RADIUS	5.2 m
MINOR RADIUS	1.2 m
ELONGATION	1.6
TOROIDAL FIELD ON AXIS	5.5 T
PLASMA CURRENT	5.1 MA
NEUTRAL BEAM POWER	150 MW _t
PLASMA TEMPERATURE	13 keV
ELECTRON DENSITY	$1.5 \times 10^{20} \text{ m}^{-3}$
$n_e a$	$1.8 \times 10^{20} \text{ m}^{-2}$
PLASMA β	6%
POLOIDAL β	4.0
WALL LOADING	2 MW/m ²
FUSION POWER	950 MW _t
REQUIRED τ_E	0.5 s
EMPIRICAL τ_E	2.0 s
$n_e \tau_E$	$7.5 \times 10^{19} \text{ m}^{-3} \text{ s}$
$(n_e \tau_E)_{\text{ign}} / (n_e \tau_E)$	3.2

where $s = \sqrt{(1 + \delta^2)/2}$ and δ is the elongation.

If the energy confinement time is accurately given by the empirical value it means that a certain amount of impurity can be accounted for in the energy balance, that a higher plasma temperature will be achieved than is given in Table 3-2, or that the neutral beam power can be reduced.

Since the collisionality is $\nu^* \sim 1 \times 10^{-3}$ for the reference geometry and plasma conditions, the energy confinement time may be expected to be determined by particle and energy transport due to trapped particle modes⁽³⁻³⁾. In this case $\tau_{\text{TIM}} \simeq 0.7$ s, which is still longer than the confinement time required for energy balance. Furthermore, if profile effects are taken into account, much higher energy confinement times (it may be an order of magnitude higher) are predicted⁽³⁻³⁾.

It is important to stress here that the fusion power and average wall loading were obtained from zero dimensional plasma estimates. If the profiles of density and temperature were taken into account,⁽³⁻⁴⁾ the fusion power and wall loadings would be about twice as large as the values given in Table 3-2. This currently is treated as margin in the design, which means that the 2 MW/m^2 could be achieved at lower densities and, hence, lower β .

To demonstrate this effect, Table 3-3 shows the effect of either decreasing the electron density from $1.5 \times 10^{20} \text{ m}^{-3}$ to $0.94 \times 10^{20} \text{ m}^{-3}$, or decreasing the average plasma temperature from 13 keV to 9 keV. In either case, β is reduced to 4% and the wall loading to 1 MW/m^2 (ignoring profile effects).

Table 3-4 demonstrates the capability of the DTHR as an irradiation facility. Sufficiently high neutron fluences from fusion neutrons can be achieved so that radiation damage effects can be studied for materials important to fusion reactor design. The atomic displacements and gas production in the first wall are large enough to show significant effects on the materials properties. The flux and fluences shown in Table 3-4 for the ThO_2 blanket are given for the blanket region closest to the plasma and, thus, are the maximum values that the fuel and clad will experience.

TABLE 3-3
DTHR OPERATION FOR WALL LOADING OF 1 MW/m²

	Reduced Plasma Temperature	Reduced Electron Density
MAJOR RADIUS (m)	5.2	5.2
MINOR RADIUS (m)	1.2	1.2
ELONGATION	1.6	1.6
TOROIDAL FIELD ON AXIS (T)	5.5	5.5
PLASMA CURRENT (MA)	5.1	5.1
NEUTRAL BEAM POWER (MW)	150	150
PLASMA TEMPERATURE (keV)	9	13
ELECTRON DENSITY (10^{20} m^{-3})	1.5	0.94
$n_e a$ (10^{20} m^{-2})	1.8	1.1
PLASMA β (%)	4	4
POLOIDAL β	2.6	2.6
FUSION POWER (MW)	460	470
REQUIRED τ_E , s	.45	.40
EMPIRICAL τ_E , s	2.0	1.3
$n_e \tau_E$	6.7×10^{19}	3.8×10^{19}
$(n_e \tau_E)_{\text{ign}} / (n_e \tau_E)$	5.7	6.4

TABLE 3 -4
DTHR MATERIALS TESTING CAPABILITY

TOTAL FUSION POWER	950 MWt
NEUTRON WALL LOADING	2 MW m ⁻²
CURRENT DENSITY OF SOURCE NEUTRONS	1 x 10 ¹⁸ n m ⁻² s ⁻¹
DUTY FACTOR x PLANT AVAILABILITY	0.20

AFTER THREE YEARS OF OPERATION, WILL ACHIEVE:

INTEGRATED WALL LOADING	1.2 MW yr m ⁻²
SS-316 FIRST WALL DAMAGE	
- FAST NEUTRON FLUENCE, E > 0.1 MeV	1.04 x 10 ²⁶ n m ⁻²
- ATOM DISPLACEMENTS	18.3 DPA's
- He PRODUCTION	246 appm He
- H PRODUCTION	862 appm H
MAXIMUM ThO ₂ BLANKET DOSES	
- TOTAL NEUTRON FLUX, E > 0.1 MeV	5.85 x 10 ¹⁸ n m ⁻² s ⁻¹
- NEUTRON FLUENCE, E > 10 MeV	2.65 x 10 ²⁵ n m ⁻²
- NEUTRON FLUENCE, E > 1 MeV	5.36 x 10 ²⁵ n m ⁻²
- NEUTRON FLUENCE, E > 0.1 MeV	9.25 x 10 ²⁵ n m ⁻²

The DTHR duty cycle times plant availability is shown as 0.20 in Table 3-4. This is an average operational factor which assumes a normal duty cycle of about 40 seconds on and 40 off. Operation would normally occur in day-time hours with maintenance and data analysis at night, so the plant availability would be ~40%. However, DTHR systems will be designed for a 70 second on pulse and 15 seconds off so that sustained operations can be tested for days, weeks, or even a month at a time.

The following subsections define the tokamak physical layout and present significant highlights of each of the major component design concepts. The general layout was controlled by the fusion driver parameters and operating modes just discussed. The size of the tokamak was also selected to be consistent with the physical space required to incorporate the bundle divertor plasma exhaust system. A detailed divertor concept was developed simultaneously with the tokamak requiring several iterations to achieve an acceptable system and interfacing of components. The decision to use a bundle divertor in lieu of a poloidal divertor permitted more space within the bore of the toroidal field coils for a fertile blanket. However, since a primary objective of the blanket module design was to use a simple, low cost configuration, this available space was not used advantageously. The toroidal field coils were redesigned from previous concepts to reduce the vertical space between the vacuum vessel and coils. This coil concept, called the compact "D", should reduce the DTHR cost significantly.

The hybrid blanket design, presented in subsection 4.1, was integrated within the space between the vacuum vessel and outer shield which protects the toroidal field coils. The tokamak auxiliary systems, defined in subsection 3.2, also had to properly interface with the tokamak layout. The detailed selection process for most of these components and systems was presented in the previous progress report.⁽³⁻¹⁾

3.1.2 FUSION DRIVER LAYOUT

The fusion driver layout for the DTHR is a typical tokamak configuration with some innovative features. The plan view in Figure 3-1 shows the relative positions of the major components. The 16 TF coils are located on 22° center-lines with respect to each other except for the pair adjacent to the bundle divertor. These two TF coils were opened to 30° to provide sufficient room for the bundle divertor vacuum chamber to protrude to the periphery of the tokamak. The bundle divertor chamber is shown in Figure 3-1 with the cryopanel and shielding removed. The outer shield for the TF coils consists of 32 sectors, one under each coil and one between each TF coil pair. The inner shield consists of 16 sectors, one under each TF coil. The TF coils use a 50% wedging and 50% bucking cylinder load removal system. The nose sections of the TF coils are identical, with the extra gap at the 30° position being filled with an assembly wedge. This wedge can be mechanically expanded to remove all clearances between TF nose sections after assembly. An inner shield assembly block is located radially outward from this wedge. Smaller assembly blocks (not shown) can be located at each TF coil opening to permit ease of inner shield installation, since each shield must be slid under the TF coil prior to vacuum vessel assembly. The assembly and disassembly techniques for the reactor components are discussed in subsection 3.2.6. Two of the neutral beam injectors (NBI) are located adjacent to the bundle divertor to permit common shielding between these components and minimize loss of blanket module positions. The other four NBI's are located symmetrically around the periphery of the tokamak relative to the bundle divertor's location.

Figure 3-2 shows the cross-sectional view of the DTHR. The NBI duct is shown entering on the horizontal midplane in front of the bundle divertor, which is also positioned at the horizontal midplane. The outer poloidal field (PF) coils are positioned between the bundle divertor and the vertical removal envelope for the blanket modules. The support structures are used to position and hold the inner six PF coils as well as the upper headers and the TF coils. The vacuum vessel "floats" on rails from the shield structures, which also support the blanket modules.

Figure 3-1. DTHR Plan View

Figure 3-2. DTHR Cross-Sectional View

The limiters, at four positions inside the vacuum vessel, are supported by the vacuum vessel structural system. All of the components within the TF coils are supported independently from the TF coil support system through saddle girders and vertical posts. These girders "saddle" the TF coils without contacting them.

The shielding system for the TF coils is shown in Figure 3-3. The inner shield and the vacuum vessel support/shield carry the inner components support loads to the saddle girders and vertical support posts.

The relative locations of the ohmic heating (OH) coils, shaping field (SF) coils and bucking cylinder to the TF coils are shown in Figure 3-4. The TF coil nose section, which wedges near the tokamak bore, is shown highlighted from the outer leg of these compact coils. The outer legs of the TF coils are each enclosed in a dewar to maintain cryo-cooling of the superconducting coils. Figure 3-5 shows the dewar systems. The inner legs (nose section) of the TF coils have an inner dewar attached to the plasma side of the coil. The OH coils and bore side of the TF nose section are enclosed in a common dewar that maintains the entire tokamak bore area at cryogenic conditions. This simplifies removal of cables and coolant lines from the OH coils in the bore. The superconducting cables are then terminated and the transition to copper bus lines occurs in the cryo-cooled enclosure below the bore area. This reduces the cryo-system complexity in the reactor compartment.

Descriptions of the coil systems, bundle divertor system, vacuum vessel, shielding and structure are provided in the following subsections. The radial dimensions of the tokamak are presented in Table 3-5, which also defines the major component and cross section thicknesses.

Figure 3-3 DTHR TF Coil Shielding

Figure 3-4 DTHR TF Coils, PF Coils and Bucking Cylinder

Figure 3-5 DTHR Superconducting Coils Cryogenic Cooling System

TABLE 3-5

DTHR RADIAL DIMENSIONS

<u>COMPONENT/LOCATION</u>	<u>THICKNESS, cm</u>	<u>RADIUS, cm</u>
Device Centerline	0	0
Bore		100
OH Coils	25	125
Bucking Cylinder	19	144
TF Coil (Nose)	104	248*
Dewar	15	
Insulation	5	268
Assembly Gap	4	272
Inner Shield	90	362
Assembly Gap	3	365
Vacuum Vessel Wall	5	
Vacuum Vessel Structure	8	
Limiter	2	380
Scrape Off Layer	20	400
Plasma, a	120	
Major Radius, R_0		520
Plasma, a	120	640
Scrape Off Layer	20	660
Limiter Space Allowance	8	
Vacuum Vessel Structure	8	
Vacuum Vessel Wall	2	678
Assembly Gap	10	688
Blanket/Assembly Gap (5-10) Allowance	92	780
Outer Shield/Assembly Gap (5-10)	105	885
Insulation	5	890
Dewar	15	905*
TF Coil	100	1005
Dewar	15	
Insulation	5	1025

*TF Bore 657

3.1.3 TOROIDAL FIELD COILS

The shape of the TF coil has been changed from the constant-tension "D" to what will hereafter be referred to as the compact "D." This design change was conceived as a result of a requirement that the inner radius of the outer leg of the coil be set at any preferred radial dimension without changing the overall inside height of the coil. The compact "D" will accommodate a larger radius of the outer leg which is required for installation of the bundle divertor.

"D" shaped coils characteristically take the shape of flexible coils supported by a restraining cylinder along the inner leg of the coil. The remainder of the coil not pushing against the restraint is unsupported and, consequently, is loaded uniformly in tension. The classical "D" shaped coil is bucked up against a right circular cylinder while the compact "D" is bucked against a curved or hourglass-shaped cylinder. Any number of classical "D" shaped coils can be designed to have their outer leg pass through fixed locations in the mid-plane. Figure 3-6 shows three such options with the outer leg passing through a common point. Coil No. 1 adequately surrounds the design space indicated by the dashed line contour. Coil No. 2 more nearly fits the design space contour line, but the inner leg passes through the contour line. Coil No. 3 closely fits the contour line and has a greatly reduced tension resulting from the reduction of the radius of curvature; however, the inner leg severely violates the requirement that the working space cannot contain extraneous structural elements. If a curved central support structure (shown dotted) is provided, the inner leg may be curved to fit the support structure and, thus, be diverted away from the working space. The unsupported portion has the shape of Coil No. 3 and maintains the reduced tension. The resulting TF coil concept and the bucking cylinder are shown in Figure 3-4. The TF coil nose section is wedged to take half of the radial forces, while the bucking cylinder takes the remaining load.

Figure 3-6. TF Coil Conceptual Design Analysis of Compact D

Several advantages which may result from the adoption of the compact "D" TF coils are:

- The overall height is reduced
 - Less energy will be stored due to the reduced bore volume and improved dissipation on quick shutdown can be achieved.
 - The shaping field coil distance to the plasma is reduced, making it easier to attain equilibrium.
 - Coil perimeter, weight, and cost are reduced.
- Coil tension is reduced
 - A reduction in coil section allows the working volume to increase.
 - Provided that the flux density at the conductor is not a limiting factor, the stress reduction may be exchanged for a greater flux density on axis.

There are 16 TF coils in this reactor design. Each coil is spaced on 22° radial centerlines except the two TF coils adjacent to the bundle divertor which are spaced 30° apart to accommodate the divertor coils. The free vertical bore of the TF coil measures ~9.6 m and the free radial bore is ~6.6 m.

The magnetic field ripple at the outer edge of the plasma will be <1% except in the vicinity of the bundle divertor. Based on present-day theoretical expectations, a ripple <1% is not expected to seriously affect the plasma energy confinement. However, a larger field perturbation occurs in the vicinity of the divertor. Experimental results with tokamaks employing divertors and also with tokamaks not using divertors, but modified to be non-axisymmetric, indicate that the asymmetries have small effects on present day collisional plasmas. However, the effects could become important with reactor-grade plasmas. Studies of particle orbits in the DTHR magnetic fields remain to be performed.

Based on 16 TF coils and 8.92 M ampere-turns per coil, the magnetic induction in the vicinity of the plasma is given by

$$B_t \cong 28.54 / R \quad (\text{tesla})$$

At the center of the plasma $R = 5.2$ m and $B_t = 5.5$ T. At the location of the maximum toroidal field, i.e., at the conductor bore of the inner leg, $R \cong 2.44$ m and $B_t \cong 11.7$ T. Allowing for some high field ripple at the coil, the peak local field at the coil will be somewhat higher (few percent). At this preliminary stage of the design the peak field at the TF coils is assumed to be 12 T for a field on axis of 5.5 T. The TF coil parameters are shown in Table 3-6.

Even though there is a large amount (22.5 GJ) of energy stored in the superconducting TF magnetic field system, the TF coils are not pulsed. Therefore, they may be energized slowly by a very small power supply and maintained at the constant excitation level with the almost negligible amount of power required to sustain losses in the electrical leads.

TOROIDAL FIELD COIL CROSS-SECTION DESIGN

The cross section of the TF coil shown in Figure 3-7 measures 100 cm long in the radial direction by 67 cm thick and is made up of 22 stainless steel bobbins. Eighteen of the bobbins contain ten conductor slots, two contain six conductor slots, and two contain four conductor slots.

A stainless steel triangular wedge is fitted to each side of the vertical section of the inner leg of the TF coil to complete the tapered nose section of the coil and fill the void between adjacent TF coils. Three bolts, not shown in Figure 3-7 are used to hold the bobbins and wedges together.

The centering force of the TF coil is reacted by the vertical face of the bobbins in contact with the bucking cylinder and the wedging action between adjacent TF coils.

Three Nb_3Sn superconducting cables, each with a square cross section of 5 cm^2 (2.23 cm on a side) including the stainless steel sheath, will be installed in each bobbin slot making a total of 600 turns of superconducting cables per TF coil. The cables are cooled by helium coolant flowing through the voids within the conductor. The coolant is fed in and out of the cables at connector joints located at the top or bottom of the "D" shape coil. A liquid nitrogen cooling jacket is carried in the vacuum dewar as a buffer system to minimize refrigeration requirements for the helium cooling system. The preliminary specifications for the superconducting cables and helium coolant are tabulated in Table 3-7.

TABLE 3-6

DTHR TOROIDAL FIELD COIL PARAMETERS
(MARCH 1978)

Parameter	Units	Value
PLASMA MAJOR RADIUS	m	5.2
VERTICAL BORE (INNER)	m	9.6
RADIAL BORE (INNER)	m	6.6
SHAPE	---	COMPACT "D"
CENTRAL FIELD	T	5.5
MAXIMUM FIELD	T	12.0
NUMBER OF COILS	---	16
FIELD RIPPLE (AT PLASMA EDGE)	%	0.8
TOTAL STORED ENERGY	MJ	22,450
STORED ENERGY PER COIL	MJ	1,400
TOTAL AMPERE TURNS	MA-T	142.7
AMPERE TURNS PER COIL	MA-T	8.92
COIL CROSS SECTION	m	1.0 x .67
SUPERCONDUCTOR/STABILIZER	---	Nb ₃ Sn/Cu
CONDUCTOR/SHAPE	---	CABLE/SQUARE
CURRENT PER TURN	kA	15
AVERAGE CURRENT DENSITY: CONDUCTOR, STABILIZER, COOLANT	kA/cm ²	4.1
CONDUCTOR AREA PER COIL	cm ²	3000
STRUCTURE/CONDUCTOR AREA RATIO	---	1.51
COIL CONSTRUCTION	---	PANCAKE
STRUCTURAL MATERIAL	---	STAINLESS STEEL
COOLING	---	FORCED FLOW SUPERCRITICAL HELIUM

Figure 3-7 TF Coil Nose Cross Section

TABLE 3-7

PRELIMINARY SPECIFICATIONS OF Nb₃Sn-Cu SUPERCONDUCTOR FOR THE TF COILS

AVERAGE CURRENT DENSITY*, A/cm ²	4,100
S/C OPERATING CURRENT DENSITY, A/cm ²	80,000
AREA OF CONDUCTOR + He, cm ²	3.65
SUPERCONDUCTOR AREA, cm ²	0.1875
NO. OF STRANDS, ACTIVE (3 x 3 x 3 x 3 x 6)	486
NONACTIVE	81
STRAND DIAMETER, mm	0.88
CONDUCTOR HYDRAULIC DIAMETER, mm	0.66
CONDUCTOR VOID FRACTION	0.3
BOBBINS PER COIL	22
NO. OF SLOTS PER BOBBIN	10 max.
NO. OF CONDUCTORS PER SLOT	3
MEAN CIRCUMFERENCE OF COIL, m	30
HELIUM FLOW RATE PER CONDUCTOR, g/s	3.2
HELIUM INLET TEMPERATURE, K	4.2
HELIUM INLET PRESSURE, Atm	4
REFRIGERATION LOADS, kW at 4.2 ⁰ K	
(all coils)	
NUCLEAR HEATING	33.3
PUMPING POWER (@ 50% Eff.)	3.9
AC LOSSES PLUS DEWAR HEAT LEAKAGE	<u>6.5</u>
TOTAL AT 4.2K:	43.7

*Averaged over conductor, stabilizer and coolant

3.1.4 POLOIDAL FIELD COILS

The poloidal field coils have been located outside the toroidal field coils to provide accessibility, to avoid interference with the blanket, and to prevent radiation heating and damage. Generally, if the shaping field (SF) coils have to be located at distances from the plasma axis which are several times as large as the plasma minor radius, it is very difficult to find SF coil locations which will produce an MHD equilibrium configuration which has an elongation of 1.6 and yet satisfies all the other mechanical constraints, such as sufficient access for neutral beam, blanket, and divertor. The currents and, thus, the energy stored in the SF coil system may become excessively large; the equilibrium state exists only in a very narrow range of vertical field strengths. To ease these difficulties the TF coils were modified to reduce the vertical dimensions and are called compact D TF coils, as discussed in the previous subsection.

ELECTROMAGNETIC DESIGN

Two SF coil systems located outside the compact D TF coil system have been developed. They are designated as SF coil systems I and II, as shown in Figure 3-8. A third system where the SF coils are located closer to the plasma has been calculated for scaling purposes to be used in the COAST code. The relative locations of these systems are compared in Figure 3-9. All SF coils are located in such a way as to give adequate access at the horizontal midplane (about 5 m) for the bundle divertor and neutral beams, and vertically for blanket removal.

The coil parameters are listed in Tables 3-8 and 3-9. The plasma configuration is shown by Figure 3-10. There are no inner SF coils, i.e., coils located at the tokamak bore side of the plasma in system I, which is shown by the upper half of Figure 3-8. System II, which is shown by the lower half of the figure, has the inner coils. System I is chosen as the reference design because its mechanical design is easier. However, System II is more stable because it has higher triangularity toward the symmetric axis.

Figure 3-8 Comparison of SF Coil Systems I and II

Figure 3-9

Relative Locations of SF Coil Systems

TABLE 3-8

DTHR PF COIL SYSTEMS: COIL LOCATIONS AND CURRENTS
(MARCH 1978)

Locations		SF Currents	
Radius <u>R ,m</u>	Height <u>Z ,m</u>	System I <u>I_i (MA-T)</u>	System II <u>I_i (MA-T)</u>
1.05	0.0	NONE	-1.7
1.05	0.8	NONE	-1.7
1.05	1.2	NONE	-0.37
2.2	5.5	5.76	5.90
2.9	5.7	5.76	5.90
3.9	6.2	5.76	5.90
8.9	4.2	-1.80	-1.85
9.6	3.4	-1.80	-1.85
9.7	2.4	-1.80	-1.85
		$\Sigma I_i = 45.4$	52.34
TOTAL OH CURRENTS (AT 50% BIAS)		I _{OH} = 62.0	62.40
TOTAL STORED ENERGY		2.3 GJ	TO BE CALCULATED

TABLE 3 -9

PF COIL SYSTEM III

(Reference Internal Coils for COAST Scaling)

<u>Radius R,m</u>	<u>Height Z,m</u>	<u>Current I_j,MA-T</u>
0.80	0.00	-1.564
0.80	0.50	-1.564
0.80	1.30	-0.310
2.60	4.75	+3.400
3.25	5.00	+3.400
4.10	5.25	+3.400
8.25	3.50	-1.423
9.00	2.50	-1.423
9.2	2.00	-1.423

Figure 3-10. Flux Plot of the DTHR Plasma MHD Equilibrium Configuration

MECHANICAL DESIGN CONSIDERATIONS

The conductors of the poloidal field coils have been designed to be superconducting to minimize energy requirements. Nb_3Sn is the choice for the superconductor material. A cross section of a typical triplex cabled superconductor is shown in Figure 3-11 to illustrate the fabrication technique used to achieve coolant channel voids. Figure 3-12 shows dimensions of the cable proposed in the preconceptual design.

The principal mechanical considerations for the field coils are to provide the physical positioning for the superconducting cables, provide the electrical and thermal insulation, provide the structural support for the forces generated within the coil and those due to interaction with other magnetic fields, and provide the enclosure and support for the cryogenic cooling system components.

OHMIC HEATING COIL(SOLENOID)

The ohmic heating (OH) coils within the device bore form a compact solenoid. A cross section of a vertical stack portion of the solenoid is shown in Figure 3-13. The solenoid consists of a total of 23 vertical stacks. Each vertical stack is made up of 5 radial rows of bobbins, each bobbin containing two rows of conductors, 14 conductors in height. The 28 turns within the bobbin result in a coolant path with an average 184 meter length. This is the coolant path length which can be adequately cooled to prevent the superconductors from going normal during operation. There must be a liquid helium inlet and exit for each radial bobbin row and this is accomplished by using the connector shown in Figure 3-14. The connectors will be circumferentially spaced within the vertical space provided by the rings between stacks.

Each stack can be independently assembled, vacuum impregnated with epoxy and then the entire solenoid can be assembled by bolting the stacks to the spacer rings. All final electrical and coolant connections can be made in the solenoid bore. It is proposed that all superconductor leads would exit the solenoid-toroidal field coil common dewar at the bottom end where the transition to a copper bus would occur. The parameters for the OH coils are summarized in Table 3-10.

CONDUCTOR PRIOR TO COMPACTION
(61% void)

CONDUCTOR FOLLOWING COMPACTION
(40% void)

Figure 3-11

Cross Sections of Typical Triplex Cabled Superconductor
Illustrating the Distribution of Voids

Figure 3-12 Typical Cable Design Cross Section

Figure 3-13 Typical OH Coil Cross Section, Inner Bore Coils

Figure 3-14. Conductor Splice Joint

TABLE 3 -10

DTHR SUPERCONDUCTING OH COIL PARAMETERS

INBOARD COILS

Design Maximum Field, Tesla	9
Inner Radius, m	1.00
Outer Radius, m	1.25
Total Stored Energy, MJ	645 (3)
Total Ampere-Turns, MA-T	51.5
Superconductor/Stabilizer	Nb ₃ Sn/Cu
Conductor/Shape	Cable/Square
Current per Turn, kA	16
Average Current Density: Conductor, Stabilizer, and Coolant, A/Cm ²	4400
Structural Material	Stainless Steel
Coils in Vertical Stack	23
Turns per Stack	140
No. of Cooling Channel per Stack	5
Average Length of Cooling Channel, m	185
Refrigeration Loads, kW @ 4.2K	
Nuclear Heating	Negligible
Pumping Power (@ 50% Eff.)	0.18
Dewar Heat Leakage	0.26
Pulsed ac Losses(1) (Average over an 85 Sec. Cycle)	<u>185.3</u>
	TOTAL: 185.74 kW

OUTBOARD COILS

Design Maximum Field, Tesla	3
Total Stored Energy, MJ	- (3)
Total Ampere-Turns, MA-T	10.5
Superconductor/Stabilizer	Nb ₃ Sn/Cu
Current per Turn, kA	16
Average Current Density: Conductor, Stabilizer, and Coolant, A/Cm ²	4400
Structural Material	Stainless Steel
Total Conductor Length, km	18.7
Refrigeration Loads, kW @ 4.2K	
Nuclear Heating	Negligible
Pumping Power (@ 50% Eff.)	0.17
Dewar Heat Leakage	0.20
Pulsed ac Losses(2) (Average over an 85 (Sec. Cycle)	<u>78.1</u>
	TOTAL: 78.47 kW

NOTES:

(1) Based on bipolar mode of operation with $\frac{dB}{dt} = 100$ T/sec for .005 sec and 10 T/sec for 1.995 sec at the beginning of cycle and reversal at the end of cycle.

(2) Similar to the inboard OH coil but with the corresponding $\frac{dB}{dt}$ as 20 T/sec and 2 T/sec, respectively.

(3) Total outboard and inboard OH coils stored energy is shown under inboard coils heading.

SHAPING FIELD COILS

The SF coils and the balance of the OH coils not incorporated within the solenoid are located outside of the toroidal field coils. The upper and lower SF and OH coils within their dewar systems mount directly to the support structure, while the outer SF and OH coils attach through their dewar system to the TF coil dewars as shown in Figure 3-4.

The arrangement of the turns in the coil will be similar to that shown in Figure 3-13 for the OH inner bore coils utilizing vertical support bobbins in radial rows. The vertical height of the coil will be adjusted so that the coolant path length will meet the requirement to prevent the superconductor from going normal during operation. This will mean that, as a minimum, a coolant connector will be required at both ends of each vertical row of conductors. For the large diameter coils, it may be necessary to arrange the coil in stacks with spacer-rings between stacks to accommodate the coolant connectors. The parameters for the SF coils are summarized in Table 3-11. The preliminary specifications for the superconducting cable design parameters for both the OH and SF coils are shown in Table 3-12.

TABLE 3 -11

DTHR SUPERCONDUCTING SF COIL PARAMETERS

Design Maximum Field, Tesla	2
Total Stored Energy, MJ	1655
Total Ampere-Turns, MA-T	45.4
Superconductor/Stabilizer	Nb ₃ Sn/Cu
Current per Turn, kA	16
Average Current Density: Conductor, Stabilizer, and Coolant, A/Cm ²	4400
Structure Material	Stainless Steel
Total Conductor Length, km	78.4
Refrigeration Loads, kW @ 4.2K	
Nuclear Heating	Negligible
Pumping Power (@ 50% Eff.)	0.7
Dewar Heat Leakage	2.0
Pulsed AC Losses ⁽¹⁾ (Average over 85 Sec Cycle)	<u>55.6</u>
	TOTAL: 58.3 kW

(1) Based on a linear $\frac{dB}{dt} = 1$ T/sec for 2 sec at the beginning and end of cycle.

TABLE 3-12
PRELIMINARY SPECIFICATIONS OF THE SUPERCONDUCTOR
FOR THE OH AND SF COILS

Overall average current density, A/cm ²	4400
S/C Operating Current Density, kA/cm ²	18.5
Area of conductor + He, cm ²	3.61
Superconductor area, cm ²	0.087
Cu - to - non Cu area ratio	5.28
No. of strands (3 x 3 x 3 x 3 x 7)	567
Strand diameter, cm	0.076
Conductor hydraulic diameter, cm	0.049
Conductor void fraction	0.30
Helium flow rate per conductor, g/s	2.0
Helium inlet temperature, K	4.2
Helium inlet pressure, Atm	4

3.1.5 PLASMA EXHAUST SYSTEM (BUNDLE DIVERTOR)

The preliminary conceptual design of a DTHR bundle divertor is discussed in this section. The results of the magnetic design of a Westinghouse proposed ISX-B bundle divertor⁽³⁻⁵⁾ for the experimental ISX tokamak⁽³⁻⁶⁾ were used as the guidelines for the preliminary DTHR concept. The magnetic field and forces for the ISX-B divertor were previously analyzed in detail and the advantages over the DITE-type divertor⁽³⁻⁷⁾ were identified. Figure 3-15 shows a plan view of the ISX-B Bundle Divertor and Figure 3-16 shows the cross-sectional view.

The initial DTHR divertor concept was based on the existing design analyses for ISX-B in order to quickly establish the feasibility of the bundle divertor for a high field DTHR. To minimize computing time, only critical values have been analyzed as a check. The rough iterative calculations were done to determine the optimum value of the critical parameters such as coil current, coil position and angle, the stagnation point, and scrape-off layer. This allows an evaluation of the overall feasibility of the cryogenic, mechanical, particle collection, and vacuum pumping problems before investing time on the detailed component design. Many iterative processes have been carried out and innovative methods have been developed for particle collection and vacuum systems during this period of work. It now appears that the bundle divertor is feasible for the DTHR application.

DTHR BUNDLE DIVERTOR

The preliminary divertor design for DTHR was developed not only to determine the feasibility but also to minimize the complexity. The system is made as compact as possible to keep the size and cost reasonable and acceptable.

The initial magnetic design was scaled-up from the information generated for ISX-B. The mechanical structural design is very conservative because the maximum forces in the DITE case were used, which are three times larger than those in the -ISX-B case.⁽³⁻⁵⁾ Critical values, such as separatrix and scrape-off layer width, were calculated to determine the initial divertor current and conductor locations and size. The cryogenic performance and mechanical layout were evaluated to determine the physical dimensions and restrictions. The coil current,

Figure 3-15

Plan View of the Westinghouse Designed Bundle Divertor for ISX-B
Where the Divertor Coils Are Circular

Figure 3-16

Cross-Sectional View of the Westinghouse Designed
Bundle Divertor for ISX-B

position, and angle were readjusted to find the magnetic flux pattern which would fit the specific physical dimensions and restrictions. The magnetic field and forces were estimated. The key coil parameters are listed in Table 3-13. A tri-metric view of the Bundle Divertor concept is shown in Figure 3-17. The cryogenic and mechanical design issues are described in the following paragraphs.

The superconductors for the bundle divertor were selected to be an Nb_3Sn copper composite operating at a constant magnetic field of 10 tesla. The conductors are to be cooled by forced flow supercritical helium. The total ampere-turns for one side of the divertor coils was taken to be 13.5 MA-T. A sample plot of the thermal performance of the superconducting bundle divertor coil is shown in Figure 3-18. Based on the space available for the divertor coils, the conductor transport current was selected to be 32 kA, the conductor plus helium area 4 cm^2 , and the void fraction 0.3. As seen from the figure, the required refrigeration load for ideal pump work is about 0.9 W/m. With $I = 32 \text{ kA}$, the total number of conductor turns is 432 turns. From the mechanical layout of the coil (Figure 3-19) and assuming a pump efficiency of 50%, the refrigeration load for the pump work was estimated to be about 12 kW at 5 K per coil. The nuclear heating and the other thermal loads would have to be added to obtain the final refrigeration requirement for the divertor coils.

The bundle divertor is located on the horizontal centerline of the reactor between two TF coils that are spaced 30° apart. The wider TF coil spacing was required to provide sufficient room for shielding the divertor coils. The unit consists mainly of two "D" shaped divertor coils encompassing two ducts leading from the vacuum vessel to a burial chamber which houses the collector panel system, as shown in plan view - Figure 3-19 and side elevation view - Figure 3-20. Each "D" shaped bundle divertor coil is comprised of 36 stainless steel bobbins, each 3.1 cm thick. The height and width of the bobbins vary in size, with the smallest bobbin located closest to the vacuum vessel. The average inside dimensions of the bobbins are 1.7 m wide and 2.5 m high. The total number of conductor slots in the 36 bobbins is 216, starting with 3 slots in the smallest bobbins and progressing to 7 slots in the largest, outermost bobbin. Each slot contains

TABLE 3 -13

DTHR BUNDLE DIVERTOR COIL PARAMETERS
(MARCH 1978)

FIELD ON AXIS	$B_T = 5.5 \text{ T}$
ASPECT RATIO	$A = 4$
TWO D-SHAPED DIVERTOR COILS CURRENT/COIL	$= 13.8 \text{ MA}$
TWO VERTICAL AUXILIARY COILS CURRENT/COIL	$= 7 \text{ MA}$
DIVERTOR COIL ANGLE (See Figure 3-15)	$\alpha = 35^\circ$
AVERAGE MINOR COIL DIAMETER (of "D")	$d = 1.7 \text{ m}$
AVERAGE MAJOR COIL DIAMETER (of "D")	$h = 2.5 \text{ m}$
OVERALL WIDTH OF THE COIL	$\ell = 1.10 \text{ m}$
SUPERCONDUCTOR CABLE	$2 \text{ cm} \times 2 \text{ cm Nb}_3\text{Sn/Cu}$
CABLE CURRENT	32 kA
CABLE CURRENT DENSITY	8 kA/cm^2
COIL STRUCTURE	STAINLESS STEEL BOBBINS CONSTANT THICKNESS VARYING WIDTH (18 cm $\rightarrow$ 40 cm)
SHIELDING SPACE BETWEEN CONDUCTOR AND FIRST WALL	$.60 \text{ m}$
HELIUM INLET TEMPERATURE *	4.2 K
PUMPING POWER (THERMAL)	6 MW
COIL INDUCTANCE	126 mh
ELECTRICAL POWER SUPPLY (30 min. CHARGING TIME)	500 kW

*Forced flow supercritical helium cooling at 4 atm inlet pressure.

Figure 3-17 Bundle Divertor Concept

Figure 3-18

Thermal Performance of the Superconducting
Bundle Divertor Coil for DTHR

Figure 3-19 DTHR Bundle Divertor Concept Plan View

Figure 3-20 DTHR Bundle Divertor Concept Side Elevation

two 4 cm^2 conductors for a total of 432 conductor turns per coil. The total thickness of the 36 bobbins equals 111.6 cm. The total coil assembly is suspended inside a common dewar that extends 15 cm beyond the periphery of the assembled bobbins. The bobbin assembly is suspended inside the dewar at two locations; one along the vertical leg of the "D" shaped bobbins, and the other at the outer curved surface of the "D". the clearance between the divertor dewar and the TF coil dewar becomes critical at the upper and lower portions of the "D" as shown in Section BB of Figure 3-20. The edge of the divertor dewar had to be notched to clear the TF coil dewar. Reduction in the tokamak size and/or TF coil outer leg position may not be feasible unless the field strength of the TF coil is also reduced. The smaller field would permit reducing the divertor coil size.

The forces in the bundle divertor will be of significant magnitude that particular attention must be given to provide adequate structural support. The two "D" shaped bundle divertor coils are positioned such that the vertical leg of each coil is adjacent to a common central support structure plate to form an included angle of 110° . The common central support structure plate is notched on both surfaces of the plate to accommodate the individual bobbins. Each bobbin is fitted to the notches and bolted in place (see blow-up view of Figure 3-19). The common central support structure plate is suspended from a structural beam located at the top center of the dewar (see cut-away view, Figure 3-21). The radial outward force of the bundle divertor coil is taken by six rods in tension that connect the common central support structure plate to the structural beam at the top of the dewar. The radial outward forces of the bundle divertor coil are also restrained by seven long bolts that are attached to every fifth bobbin plate on the curved portion of the "D" shaped bobbins. These bolts are attached to a structural beam on the midplane of the dewar. The outward forces of the divertor coils are restrained by additional structure extending from the side and top structural beams to the reactor foundation. These structures have been eliminated from the drawings, for clarity.

The bundle divertor vessel is designed in two parts, which must be completely sealed to maintain the vacuum vessel integrity. The first section of the bundle

Figure 3-21 DTHR Bundle Divertor Coil Cut-away View

divertor vessel contains a circular flange that attaches to the vacuum vessel side opening and is provided with an internal seal which is seal-welded after installation. From the circular flange the section branches into two "D" shaped ovals that extend through the two bundle divertor coils. After the divertor coils and the common dewar are installed over the two branches of the first section of the divertor vessel, a circular plate containing two "D" shaped holes which match the branches of the first section is installed. An internal seal is also provided to assure a vacuum-tight joint between the two branches and the circular plate. The second section of the bundle divertor vessel is a semicircular shaped enclosure that has a mating flange for attachment to the circular plate which is sandwiched between the first and second sections. The semicircular shaped section contains an external seal to complete the vacuum-tight enclosure. A collector panel system (as shown in Figure 3-22) is located inside the semicircular shaped vessel of the bundle divertor. A collector panel system discharge enclosure is located above and below the collector panel as shown in Figures 3-20 and 3-23. While one-half of the collector panel system is located inside the bundle divertor semicircular shaped vessel, the other half of the collector panel is either in the upper or lower collector discharge enclosure. In operation, the portion of the collector panel system in the bundle divertor vessel traps the particles from the plasma that are deflected into the bundle divertor, while the other half of the collector panel located in either the upper or lower collector system discharge enclosure is being regenerated. Gas from the panel is discharged and removed by a pumping arrangement connected to the upper and lower collector system enclosures.

The gas throughput to the divertor depends on the particle confinement time; for the DTHR it would be expected to be ~ 500 torr liter/second. The thermal load to the divertor would be ~ 300 MW. A great deal of effort has been focused upon the collection method such that a compact system can be designed. In the innovative concept illustrated by Figures 3-22 and 3-23, the collection panels consist of tubing of 1 cm in radius which are made of Zr/Al pressed on constantan (copper nickel alloy). The panels can be designed so that a surface heat load of approximately 1 MW/m^2 can be handled. The edge heat load would be about 5 MW/m^2 . Such a heat load is still moderate in comparison with that which has been tolerated experimentally.^(3-8,9,10) The trapping efficiency of hydrogenic

Figure 3-22 Conceptual Design of Collector System for the DTHR Bundle Divertor

Figure 3-23

Side View of a Conceptual Dual Particle
Collection and Regeneration System

ions in Zr/Al is about unity, and trapping capacity of 10^{19} ions/cm² has been reported recently⁽³⁻¹¹⁾. Therefore, a holding time of ~900 seconds, equivalent to ~13 pulses of 70 second duration can be obtained. Assuming that the trapping coefficient for He is zero, approximately 40 m² of cryosorption area are needed to maintain an operating pressure of 10^{-5} torr. The method of regenerating the collection panels and cryosorption panels without affecting the duty factor is described in the paragraph that follows.

As shown by Figures 3-23 and 3-24, both the collection and cryosorption panels are dual units. Figure 3-23 shows that collection unit II is in the lower chamber and is sealed at the top. This unit is warmed up to 600°C to release the trapped particles which are pumped and collected. In the meantime, unit I is collecting particles. Before reaching the capacity of unit I, the whole collection system is lifted up slowly until unit I enters the chamber on the top which is then sealed for regeneration. This cycle is reversed when unit II reaches its capacity. The system is designed to move at ~1 m/sec. For increasing the capacity, each unit can be higher than the actual height required for collection. The dual cryosorption module system is illustrated by Figure 3-24. Since the cryopanel is not directly exposed to the energetic particles, they can be isolated from the chamber by gate valves. The modules are further divided into small units so that reasonable component size and moving mechanisms can be designed. The valves are designed to slide back and forth between two neighboring modules. One module can be closed for regeneration while the neighboring one is open for pumping. This dual particle collection unit and cryosorption module system allows for continuous collection and regeneration simultaneously without affecting the duty factor. Efforts to develop these concepts will be continued.

A preliminary cost model and a detailed mechanical layout has been developed. As shown in Table 3-13, it can also be seen that the thermal pumping power is approximately 6 MW and the electrical power supply for a 30 min. charging time is rated at 500 kW for the divertor coils. These are nominal values. The final detailed magnetic flux and forces computation, detailed analysis, and system design remain to be completed. These efforts will continue, as well as determining the sensitivity of the bundle divertor to the tokamak size and performance parameters, especially the IF coil bore size, location, and field strength.

Figure 3-24

The Illustration of a Dual Cryosorption Module
For Simultaneous Pumping and Regeneration

3.1.6 VACUUM VESSEL

The design requirements for the vacuum vessel (and first wall) of the DTHR are as follows:

- To prevent plasma contamination from plasma-wall, plasma-atmosphere interactions.
- To provide structural integrity for the severe thermal and mechanical stresses imposed upon the structure and the high level of radiation.
- To provide first level containment of the tritium fuel during operation.

The design objective is to cool the vacuum vessel structure to below or near 500° C with minimal pumping power. The 500° C metal temperature is the threshold above which the maximum allowable stress of the vessel's material, 316 stainless steel, deteriorates rapidly.

There are various techniques available that would increase the shell's structural reliability and efficiency in structural mass disposition. The potential structural elements investigated are as follows:

- Sandwich construction
- Conventional rib stiffened
- Integral shell stiffened
- Monocoque

Conventional rib stiffened was the structural method identified as being the most viable for the DTHR plasma vessel. This decision was based upon work which investigated the potential of each structural element.

The vacuum vessel concept is a two component structure. The structural member is a rib stiffened shell that withstands the external static load of one atmosphere. The first wall is a tubular wall which shields the structural wall from the high surface heat flux.

The thin wall shell under external pressure loading was initially sized from buckling strength rather than the stress considerations. The vacuum vessel is constructed from 316 stainless steel. The fabricated vessel is made up of 10 identical modules to form the torus shown in Figure 3-25. The structural wall is approximately 0.40 cm thick and is stiffened at outside poloidal periphery increments of one meter. The stiffeners are designed for minimum mass. Because of the smaller inner equator of the toroidal envelope, the stiffening rib will have a varying area moment of inertia of its centroidal axis parallel to the shell, as it traverses the poloidal periphery of the toroidal shell. The stiffener's area moment of inertia varies from 134 cm^4 at the inner equator to a maximum of 250 cm^4 at the outer equator. The stiffeners are designed to support their own weight, the shell's panel weight, and the weight from the tubular first wall. Each rib is supported by a three point suspension structure. The stiffeners' supports are also designed to serve as coolant manifolds for the tubular first wall. The stiffener-manifold is illustrated in Figure 3-25.

The source of the surface heating is from the alpha particles and the ionization of the neutral beam energy in the plasma. The magnitude and distribution is not fully determined and is a function of a number of variables. This thermal loading is often assumed to be one fourth of the design value of the first wall (neutron) loading (2 MW/m^2 for DTHR). For the initial scoping of the DTHR vacuum vessel wall, the thermal and hydraulic analysis was based upon a very conservative estimate of this thermal load being the full value of 2 MW/m^2 .^{*} In addition to the surface flux heating there is neutron and gamma heating acting throughout the entire vessel structure. The magnitude of this volumetric heating is rather uniform for the first few centimeters of mass, after which it decays exponentially. A reasonable estimate of this neutron and gamma heating is $\sim 56 \text{ W/cm}^3$ based on the blanket front wall power density (Figure 4-18, subsection 4.1.5, at 0.4 MW-yr m^{-2}). Pressurized water was determined as being an effective medium of removing this large thermal load. This cooling is accomplished by circulating

* The divertor heat removal was not included. This would reduce the heat load by $\sim 75\%$.

Figure 3-25 DTHR Vacuum Vessel Concept

pressurized water in the bank of tubes which line the plasma side of the vacuum vessel. The tubes are aligned to run in the toroidal direction with coolant manifolds spaced at no more than every four meters on the outer equator to meet hydraulic limitations (pressure drop and temperature rise). The manifolds may have to be located every two meters from a fabrication/assembly consideration.

Additional analyses need to be performed to determine the most efficient cross-section for the stiffener rib-coolant manifold. Current assessment of the proposed design is summarized in Table 3-14. Alternate coolants, such as helium were considered but required much larger pumping power. The water cooling system will require evaluation for tritium pickup in the water as well as the processing and control requirements for the coolant circulation system.

TABLE 3-14

PRELIMINARY VACUUM VESSEL (FIRST WALL) PARAMETERS
(March 1978)

STRUCTURE	TUBULAR DESIGN WITH RIB-STIFFENED SHELL
MATERIAL	316 STAINLESS STEEL
SHELL PANEL THICKNESS, cm	0.4
STIFFENING RING SPACING, cm	106
AREA MOMENT OF INERTIA OF STIFFENER, cm ⁴	250
COOLANT TUBE OD, cm	0.953
COOLANT TUBE ID, cm	0.810
DESIGN SURFACE HEAT FLUX, MW/m ²	2
DESIGN NEUTRON HEATING RATE, W/cm ³	~56
NOMINAL COOLANT HEAT TRANSFER COEFFICIENT, $\frac{W}{cm^2 \cdot ^\circ C}$	2.36
MAXIMUM TUBE TEMPERATURE, °C	410
MAXIMUM SHELL TEMPERATURE, °C	380
MAXIMUM THERMAL STRESS, MPa	
TUBE OD	- 172.3
TUBE ID	- 206.9
SHELL OUTSIDE WALL	- 17.2
SHELL INSIDE WALL	- 41.4
COOLANT	PRESSURIZED WATER

3.1.7 SHIELDING

This section summarizes the first results of calculations of radiation shielding for the DTHR. The shielding discussed in this section is that mounted around the vacuum vessel and inside the toroidal field coils and does not include specialized shielding arrangements around the bundle divertor, neutral beam injectors, and vacuum pumping ducts.

The calculations were done by means of one-dimensional discrete ordinate transport theory, applied in the P_3, S_8 approximation. A coupled neutron-photon cross section library was used with 26 neutron and 16 photon energy groups.

The neutron and photon fluxes were computed by the ANISN code⁽³⁻¹²⁾ for a fixed plasma neutron source intensity corresponding to an average first wall neutron kinetic energy current of 2MW/m^2 . The heating rates in the shields were computed from the ANISN neutron and photon fluxes by means of the AAP code⁽³⁻¹³⁾ supplied with a suitable kerma factor library.

Two design criteria were used:

- a) total radiation heating load in TF coils ≤ 20 kWt (based on a reasonably sized refrigeration system)
- b) total dose to TF coils $\leq 1 \times 10^8$ rads (based on superconductor insulation damage limits)

A DTHR reactor operating at 0.5 duty factor and 0.4 availability was specified, giving an overall plant factor of 0.2. Various shield compositions will be evaluated. However, the initial shield concept was selected with a simple composition of 65% stainless steel and 35% water loaded with natural boron at a ratio of 1 boron atom per 68 water molecules. The calculations indicate that an inner shield of 95 cm thickness and an outer shield of 76 cm on the outside of the fuel zone will reduce the radiation fluxes to the level where 25 years of operation at 0.2 plant factor will be possible before the dose of 1×10^8 rads is reached.

Figure 3-26 shows the arrangement of the shielding and its dimensions. The volumes of the shield segments numbered A through D in this figure are given in Table 3-15 together with the masses of water and stainless steel constituting each.

Shield Composition: 35% B-H₂O, 65% Stainless Steel.
All Dimensions in Meters.

Figure 3-26 Dimensions for TF Coil Shielding in DTHR

TABLE 3-15
VOLUMES AND MASSES OF MAIN SHIELD SEGMENTS

ZONE	VOLUME (m ³)	MASS (TONNES)	
		Water*	SS**
A Outer Shield	370.	130.	1924.
B Upper and Lower Extensions	37.	13.	192.
C Upper and Lower Inner Shield	81.	28.	421.
D Vertical Inner Shield	134.	47.	697.
E Conical Supplement	33.	12.	172.
TOTALS:	655.	230.	3406.

*35 $\frac{\text{V}}{\text{o}}$, 1. g/cm³ density.

**65 $\frac{\text{V}}{\text{o}}$, 8.0 g/cm³ density.

The resulting shield heating, plotted as a function of distance from the center of the tokamak, is shown in Figure 3-27. The major radius of the tokamak is 5.2 m. From this figure it will be observed that the ThO_2 fuel zone, assumed to contain 2% U-233, provides about one decade of attenuation of radiation fluxes.

Since the fuel covers only 3.7 m of height along the outside equatorial surface, the outside shield thickness above and below the fuel might be increased by an additional 19 cm of shielding. This extra thickness is shown in Figure 3-26 as the two conical shields, E, and its volume is included in Table 3-15. The volumes and masses shown in that table are for uninterrupted shield coverage. In actual practice, they will be reduced by the presence of shield penetrations for the divertor, neutral beam injector, and vacuum pumping ducts.

The maximum and the volume-average volumetric heating rates in the inner and outer shields are given in Table 3-16. The maximum heating rates on the inner surface of the conical supplementary shield located above and below the fuel as part of the outside shield are nearly the same as on the inside surface of the inner shield.

The total power deposited in shield segments A through D is summarized in Table 3-17. The total thermal power in the shields is 412 MW. Thus, based on the total fusion power of 950 MW of which 0.8 is neutron power, an additional 348 MW is deposited in the ThO_2 fuel, over and above the fission power generated in the fuel.

The radiation heating in the dewar of the TF coils is 0.50 kW, in the TF coil itself 1.09 kW, for a total heating load of 1.59 kW. This is well below the 20 kW design limit. Consequently, the total dose criterion is the controlling factor of this design. Additional shielding analyses will be necessary to determine the adequacy of the shield from the point of view of shutdown biological shielding for maintenance purposes and the interaction of the reactor shield and the reactor cell wall thickness design requirements.

Figure 3-27

Power Density in DTHR TF Coil Shielding

TABLE 3-16

VOLUMETRIC RADIATION HEATING RATES IN SHIELDS DURING A PULSE

	POWER DENSITY, W/cm ³					
	Water		SS		Shield Total	
	Avg	Max	Avg	Max	Avg	Max
Inner Shield	0.389	4.39	0.900	10.13	1.29	14.52
Outer* Shield	0.062	0.37	0.089	0.85	0.151	1.22

*At inner surface of conical supplementary shield E, the maximum heating rates equal maximum values of inner shield and fall off exponentially with an attenuation constant $\mu = 0.1307 \text{ cm}^{-1}$.

TABLE 3-17

THERMAL POWER DUE TO RADIATION HEATING IN SHIELDS DURING A PULSE

		(MWt)
Inner Shield	(95 cm thickness)	276.4
Outer Shield	(76 cm thickness)	55.8
Supplementary Shield	(19 cm thickness)	80.4
TOTAL:		412.6

3.1.8 FUSION DRIVER STRUCTURE

The fusion driver support structures are shown in the DTHR cross-section view in Figure 3-2. The support system is similar to that used in the actinide burner⁽³⁻¹⁴⁾ and hybrid reactor⁽³⁻¹⁵⁾ studies. Basically, the major structural member is the inner shield which supports the vacuum vessel, the blanket modules, and the outer shield sectors. The TF coil assemblies are supported by the base support system which also supports the OH solenoid. The peripheral OH/SF coil assemblies are attached to the TF dewars through load-bearing insulation and, thus, their loads are carried to the base support system through the TF dewars and structural supports.

The size and mass of the DTHR components which must be supported present the major design considerations for the support system. However, in addition to these considerations is the fact that there are substantial operating temperature differences between components. These temperature differences must be accommodated without inducing significant stresses in the supporting structure. For instance, the vacuum vessel which will operate at higher temperature than the support structure will be suspended from the upper shield support and will be guided at the lower shield support. These vessel attachments will permit both radial and axial growth of the vessel without inducing associated loads into the shield support arms.

The blanket and outer shield will be supported in a manner similar to the vessel having an attachment to the upper shield support and the upper support tie, respectively. Thus, the components within the TF coil bore are supported independent of the TF coil support system. Also, appropriate expansion systems must be developed for the dewars of the cryogenically cooled coils. While no specific details of these attachment features have been developed, they will be given adequate attention during the next phase of this study.

3.2 TOKAMAK AUXILIARY SYSTEMS

This subsection presents the fusion driver auxiliary systems required to operate the plasma or service the reactor and its waste products. Although many of these systems directly attached to the major reactor components, these systems are, in general, peripheral to the tokamak yet within the reactor containment enclosure.

3.2.1 NEUTRAL BEAM INJECTION SYSTEM

The DTHR Neutral Beam Injection System (NBIS) design is based on a number of requirements and constraints imposed by the anticipated beam-driven mode of plasma pulsing, by the design of the hybrid blanket assembly, and by the impact of the neutral beam ducting (to the torus) on the sizing of the toroidal field coils. The following is a list of the four main requirements and constraints:

- 1) The injection of 150 MW of neutral deuterium beams during the full extent of the plasma pulse steady-state time to allow for a strongly beam-driven mode of plasma operation.
- 2) A neutral particle energy of 200 keV to assure sufficient heating of the central region of the 1.2-m radius plasma which could be operated with a relatively high Z_{eff} ($\sim 2-3$).
- 3) An injection geometry in which the individual beams are aimed at a near radial angle to the plasma center (i.e., near perpendicular injection) and injected in the horizontal midplane to maximize the access space between toroidal field coils required for ducting and to minimize the plasma target thickness as seen by the various beams.
- 4) A neutral beam system involving six (6) individual injector arms to limit the number of ducts penetrating through the hybrid blanket but, at the same time, to maintain a beam size and cross-sectional area which allows the toroidal field coils to be sized without any serious constraints on neutral beam access requirements.

From the requirements on beam energy and beam power it is clear that the NBIS will have a major impact on the reactor size, the reactor cost, and the op-

erating costs. A system which is an efficient convertor of electrical power to neutral beam power is highly desirable. This feature implies a system based on the conversion of negative ions and utilizing direct energy recovery for the charged particle beams not converted to neutral beams. However, a negative-ion system was not selected for the design basis, and direct energy conversion, as of this point in the study, has not been applied to the NBIS.

As for the selection of the ion sources, positive-ion generators were used for the reference system for the following reasons. First, the technology for positive ion sources is clearly at a level which will allow for a reasonable extrapolation to DTHR application. Presently, positive-ion sources at ~ 150 keV and ~ 50 A are in the final stages of development, while negative-ion sources are at the stage where only ~ 1 -A extraction currents have been achieved. By the mid-1980's negative-ion sources will be available only if research and development efforts are increased considerably over present efforts. Secondly, the 200-keV value for the beam energy is believed to be conservative and, after further studies, the selection of a lower required energy (~ 150 keV) may be possible. At 150-keV, positive-ion systems should look more attractive than negative-ion systems.

With respect to the usage of direct energy conversion, the COAST code for costing and sizing of DTHR has an option for sizing such systems, but the development of COAST was not completed to the point where the impact of the direct convertors was studied in detail. Such studies are planned for the next six months.

Based on a positive-ion system of sources and on the above requirements and constraints, the preliminary design for the NBIS was completed and described in the following subsection. A summary of the NBIS design requirements and assumptions is given in Table 3-18.

PRELIMINARY DESIGN OF THE NBIS

A reference design of the NBIS was obtained for the various requirements and constraints and the resulting parameters associated with the source assemblies, the neutralizer ducts, the ion collector systems, the vacuum pumping system, the water cooling system, and the power supplies for the ion sources are presented in this section.

TABLE 3-18

NBIS DESIGN REQUIREMENTS AND ASSUMPTIONS

TOTAL NB POWER TO PLASMA, P_b^*	150 MW _t
INJECTION ENERGY, w_b	200 keV
NB PULSE LENGTH, τ_b	$\lesssim 68$ s
NUMBER OF NB INJECTION ARMS, N_{BI}	6
NB POWER TO PLASMA PER ARM*	25 MW
INJECTION ANGLE AT MIDPLANE, θ_b^{**}	$\gtrsim 16^\circ$
CHARGE STATE OF SOURCE IONS	+1

*AT AN ENERGY $w_b = 200$ keV, FRACTIONAL ENERGY COMPONENTS NOT INCLUDED.

**AN ANGLE $\geq 16^\circ$ REDUCES PROBABILITY OF THE RIPPLE TRAPPING OF THE INJECTED BEAM PARTICLES.

Ion Source Assembly

Each individual positive-ion source is assumed to be a scaled-up version of those to be utilized on TFTR and being developed at LLL/LBL. A schematic of the geometry of the three-source assembly for TFTR application is shown in Figure 3-28. Each assembly has three 60-A sources of 120-keV D^+ , and each beam arm will inject 5 MW (at 120 keV) of D^0 neutral beam power.

The source assembly for DTHR is shown in Figure 3-29. Each of eight ion sources produces 125 A of 200 keV particles (D^+ , D_2^+ , D_3^+) and is sized with dimensions similar to those for TFTR. The width (horizontal dimension) is the same as that for TFTR, but the length (vertical dimension) of each source was increased to take into account the increase in source current (from 60 A to 125 A). Each source is characterized as producing 100 A of D^+ current and 25 A of D_2^+ , i.e., a beam with an 0.8 fraction of atomic particles. A gas-to-ion conversion efficiency of 50% is also assumed, resulting in a required gas feed of approximately 30 torr liter/s. The total power in the 8 source beams is 200 MW.

Neutralizer Ducts

A close-coupled D_2 gas neutralizer duct (0.2 m x 0.9 m x 2.0 m) operating at an average pressure of 3.6×10^{-3} torr without any source gas except that streaming through from the ion source is used. The asymptotic value of the neutralization efficiency of a 200-keV D^+ beam is 20% and, after taking the finite target thickness of the neutralizer duct into account, that efficiency is reduced to a value of 15.8%. For the D_2^+ molecular component the efficiency is 55.6%. Thus, taking into account an estimated loss due to beam divergence of 3%, the neutral beam power emerging from the eight neutralizers is 24.4 MW of D^0 and 21.6 MW of D_2^0 . The power associated with the charged-particle portion is 148 MW total for the eight beams.

Ion Collector System

The ion collector system for each ion source system is based on recent work^(3-16,17) in the design and experimentation of space-charge controlled energy recovery schemes. As of this stage in the design of the NBIS for DTHR, the charged-

ALL DIMENSIONS IN METERS

Figure 3-28. TFTR Ion Source Assembly

ALL DIMENSIONS IN METERS

Figure 3-29. DTHR Ion Source Assembly

particle beams are collected on recovery plates held at high voltage (~ 170 kV) without any energy recovery of that power. The energy recovery aspect of the system will be under study in the next quarter. Collecting the energy at 170 kV and allowing that energy to be drained off in the form of electrical energy greatly reduces thermal loads to the collectors, and the system can operate in steady-state with minimal problems associated with the heating and destroying of collecting surfaces. The ion collector assembly is anticipated to be similar in design to that shown in Figure 4 of Reference 3-17, and to conform to the rectangular geometry of the sources. The assembly is approximately 2-m long with a 1-m ion deflector and electron deflector assembly held at ~ -40 kV. The load on the deflectors due to secondary electrons and divergence losses in the beam are estimated at 2.2 MW per ion beam (8 beams per arm). If the ion collectors were utilized in a direct energy recovery system (with a 90% electrical efficiency) and an ion-source power supply system (with an 80% electrical efficiency), an overall efficiency (D^0 power to plasma divided by electrical power from an external source) of 14.7% is estimated. This calculation accounts for various beam divergence losses, drift tube reionization losses, a D^+ -fraction of 0.8 from each source, and losses due to secondary electrons in the convertor. Without energy convertors, an efficiency of 10% is estimated. If the D_2^+ component could be reduced to zero, the corresponding efficiencies would be 20% and 12.5%, respectively. With near-term technology, the radiation life of the ion source may be the limiting design element of the NBIS. System economics may determine the extent of how much lifetime improvement can be achieved.

Vacuum Pumping System

In-line cryopanel or zirconium-aluminum panels are anticipated for the maintenance of acceptable operating pressures in the source region as well as in the collector-drift region. There is approximately 120 m^2 of surface area available in each arm (a chamber with dimensions of 3.5 m x 4.1 m x 8.0 m). Using a specific pumping speed for either of the above panel types of 10^5 l/s per m^2 of panel, a pumping speed for the 80 m^2 of panel in the source/neutralizer/collector region is $8 \times 10^6 \text{ l/s}$ and for the 40 m^2 of panel in the drift region is $4 \times 10^6 \text{ l/s}$. For a finite conductance between the two regions of $4.5 \times 10^5 \text{ l/s}$, the operating pressures are 4.6×10^{-5} torr and 5.2×10^{-6} torr, respectively. The pressures in these regions are low enough to keep the reionization losses down to a few percent. The total throughput per arm is

calculated to be approximately 400 torr-liter/s where this value takes into account the desorption of gases from those surfaces which stop beam ions. This throughput is very high and results in filling the zirconium-aluminum panels to capacity in approximately one hour. If the cryopanel is used, a period between regenerations, determined by holding the total gas pumped to less than that which would result in an explosive mixture during a vacuum failure, is also approximately one hour. The one-hour period is a serious restriction on the operational aspects of the NBIS, and will be examined more carefully in the following quarter. At present, the problem of panel regeneration appears to be a difficult one to solve. If the present experimental results on $n_e \tau_E$ scaling continue to apply to reactor-grade plasmas, the number of neutral beam arms required for driving the plasma while maintaining a fixed neutron wall loading could be somewhat reduced, allowing for the possibility of pump regeneration during pulsing for those arms not injecting during that particular set of pulses. Otherwise, improved source designs or innovative panel regeneration systems will be required.

Water Cooling System

The surface heat loads appear over the following four components: neutralizer wall, electron suppressor grids, recovery plates in ion collector systems, and defining plates at the wall defining the two main vacuum pumping regions. The respective heat loads to the surfaces are estimated at 0.28 MW/m^2 , 13.50 MW/m^2 , 1.41 MW/m^2 , and 1.77 MW/m^2 , where the suppressor and recovery plates are held at -40 kV and 170 kV , respectively. The electron suppressor grids are in the form of tubes with a 2 cm O.D. through which water passes at a velocity of $\sim 6 \text{ m/s}$ and can be operated in steady-state. The defining plates are assumed to be of a double-wall design with a water cooling velocity of 1 m/s .

Power Supplies

Each individual ion source is assumed to be powered by an independent supply. The supply design is assumed to be similar to that required for TFTR for an ion source with up to four accel/decel grids. The maximum voltage is 200 kV at a current load of 125 A . In addition to the accel/decel grids, supplies for the filaments and arc are required. A detail design specifying regulation, protection circuits, etc. has not been made, but awaits further study. For

the purpose of the conceptual design, an overall efficiency (beam power to electrical power) of 80% was assumed.

A summary of the main design parameters for the NBIS is given in Table 3-19, and a tabulation of the electrical and thermal power loads is given in Table 3-20.

The above description is a brief summary, subsystem-by-subsystem, of the NBIS. To complete the discussion on the NBIS design, a description of the interfacing of the NB arms with the device and plasma is necessary. For purposes of sizing the ducting to the torus, the individual ion sources are assumed to be aimed at a cross-over point midway between the plasma edge and the plasma center. This assumption permits estimating the required dimensions of the vertical and horizontal sides of the clear opening. Taking the finite size and geometry of the sources into account and allowing space for scrape-off, an opening of 1.37 m x 0.83 m was obtained. The overall size of the duct cross section, taking the radiation shielding (0.3 m wide on all sides) into account, is 2.05 m x 1.51 m.

TABLE 3-19
NBIS DESIGN PARAMETERS

CURRENT PER ION SOURCE	125 A
ATOMIC FRACTION OF ION CURRENT	0.8
D ₂ GAS FEED RATE PER SOURCE	30 torr ℓ/s
SOURCES PER NB ARM	8
GAS NEUTRALIZER DUCT LENGTH	2 m
AVG D ₂ GAS PRESSURE IN NEUTRALIZER DUCT	3.6×10^{-3} torr
RESULTANT NEUTRALIZER EFFICIENCIES	<div style="display: inline-block; vertical-align: middle; text-align: center;"> $D^+ \rightarrow D^0$ $D_2^+ \rightarrow D_2^0$ </div> <div style="display: inline-block; vertical-align: middle; text-align: center;"> 17.4% 55.6% </div>
ION COLLECT VOLTAGE ⁽¹⁾	170 kV
ELECTRON SUPPRESS VOLTAGE	-40 kV
OVERALL TRANSMISSION THROUGH ARM	~ 0.88
HIGH VACUUM PUMPING SPEED PER ARM	1.2×10^7 ℓ/s
TOTAL GAS THROUGHPUT TO VACUUM PUMPS IN EACH ARM	400 torr ℓ/s
PUMP PANEL REGENERATION PERIOD ⁽²⁾	~ 1 hour

(1) ASSUMING ION SOURCE AT GROUND

(2) FOR EITHER Zr/AI GETTER PANELS OR CRYOPANELS

TABLE 3-20
SUMMARY OF ELECTRICAL AND THERMAL POWERS FOR NBIS

	PER SOURCE (MW)	PER BEAM ARM (MW)	TOTAL SYSTEM (MW)
REQUIRED ELECTRICAL INPUT POWER*	31.25	250.00	1500.00
LOSSES IN POWER SUPPLIES AND ION GENERATOR	6.25	50.00	300.00
LOSSES IN NEUTRALIZER DUCTS	0.75	6.00	36.00
LOSSES IN ELECTRON SUPPRESSORS	2.20	17.60	105.60
LOSSES IN ION COLLECTOR ASSEMBLY	1.58	12.66	75.96
LOSSES IN ION COLLECTOR POWER SUPPLIES AND RESISTORS	14.57	116.56	699.36
LOSSES IN DUCTING TO TORUS	0.41	3.28	19.68
NB POWER IN D ⁰ BEAM	3.13	25.00	150.00
NB POWER IN MOLECULAR BEAM	2.36	18.88	113.28

*AN OVERALL POWER SUPPLY ELECTRICAL EFFICIENCY OF 80% ASSUMED
WHICH INCLUDES LOSSES WITHIN ACCELERATOR ASSEMBLY.

3.2.2 VACUUM PUMPING SYSTEMS

The total vacuum pumping system for the DTHR consists of the neutral beams pumping system, the divertor pumping system, the torus vacuum pumping system, and individual diagnostic pumping systems. The neutral beam vacuum system is designed to pump the gases added to the system via the ion sources and neutralizers. The design goals are to provide an environment for the beams such that beam losses are small due to atomic interactions, and to maintain the flow of gas into the torus from the beam lines at an acceptably low level. Preliminary considerations of this pumping system have been presented in subsection 3.2.1. Various small scale pumping systems may be associated with various diagnostics; these systems have not been addressed at this stage of the design of DTHR. The remaining pumping systems are addressed below.

The previous quarterly report identified the particle removal problem in the bundle divertor burial chamber as the limiting constraint on the vacuum pumping system. Further analysis showed that the use of getters (Zr/Al or Li for hydrogenic particles and cryopanel for helium) to remove these particles was limited by a saturation constraint if continuous operation for eight hours with a cycle consisting of 70 s "on" and 15 s "off" was conducted during testing.

The imposition of this constraint has led to the recognition that changes in the operating cycle may be needed to alleviate the saturation and regeneration problem. Effort this quarter identified potential scenarios and strategies for solving this problem in addition to the two solutions listed in the previous report.

1. Operate for one hour, followed by one hour off for regeneration of the surfaces for a total of eight hours "on" per day;
2. Utilize a flowing getter instead of a solid getter.

Additional potential solutions are:

3. Devise means for
 - isolating or removing a saturated getter panel while it is being regenerated, and
 - installing a fresh panel in its place;

4. Modify the operating cycle to have a long "off" time, e.g. 30-60 s, to allow short periods of regeneration after a given number of operating cycles at reference conditions;
5. Enlarge the burial chamber to allow enough getter surface area for saturation not to occur;
6. Devise means for accelerating regeneration time.

Each of these six approaches needs to be evaluated in terms of technical feasibility, impact on operating cycle and other parts of the design, extrapolability to a commercial design, and cost. Isolating the getter and cryosorption panels is a particularly attractive approach that may not require changes in the operating cycle and it is being analyzed as the reference design for DTHR (see subsection 3.1.5). The handling of the gas during and subsequent to regeneration is discussed in subsection 3.2.4.

The Torus Vacuum Pumping System (TVPS) for DTHR will maintain the vacuum vessel at a low base pressure ($\sim 10^{-7}$ to 10^{-8} torr) between plasma pulses and will be used in the vacuum conditioning of the torus during any period in which the divertor pumping system is not used. The divertor pumping system and the TVPS are complementary systems in that the divertor is effective during the pulse and the TVPS is effective between pulses when there is no divertor action to aid the pumping of the divertor burial chamber pumps. The divertor is described in subsection 3.1.5.

The reference design for the TVPS consists of six high vacuum pumping modules each utilizing 2-3 m² of cryopanel made, in part, of a molecular sieve and, in part, of bare metal surfaces. The pumps will be required to pump all isotopes of hydrogen as well as the inert gas helium. Such a pump is a compound pump and is located in the reactor cell basement, so that sufficient radiation shielding is provided to avoid any nuclear heating. The pumps communicate with the toroidal vessel via a 0.61 m diameter duct in which is located a large (0.61 m diameter) all-metal gate-valve. Taking the various conductances and pumping speeds into account, the effective pumping speed at the torus per pumping unit is estimated at 10^4 liter/s for hydrogen gas.

The speed for helium is expected to be somewhat less. The main vessel has approximately 400 m^2 of surface area and to evacuate it to a starting pressure after shutdown of $\sim 1 \times 10^{-8}$ torr, six pumping units are assumed such that a total effective pumping speed of $6 \times 10^4 \text{ l/s}$ is possible.

The backing system consists of mechanical pumps as well as Zr/Al getter pumps for use in cryopanel regeneration. The ducting to the torus is via the neutral beam ducts so that the number of penetrations through the blanket is minimized.

There has been no attempt to optimize the design of the TVPS. The design given above represents a starting point for further study and after the size of the plasma and vacuum vessel are firmly established, the design can be iterated upon to provide the speeds and pumping capacities required. A summary of the three vacuum pumping systems is given in Table 3-21.

TABLE 3-21
VACUUM PUMPING SYSTEMS

TORUS VACUUM SYSTEM

Number of Cryopanel Modules	6
Total Effective Hydrogen Pumping Speed	6×10^4 ℓ/s
Base Pressure in Torus	$\sim 1 \times 10^{-8}$ torr
Pressure at End of 15 s Off Time	$\sim 1 \times 10^{-5}$ torr

DIVERTOR BURIAL CHAMBER PUMPING SYSTEM

Zr/Al Getter Target Pump Area	200 m ²
Cryosorption Pumping Panel Area	40 m ²
Operation Pressure in Burial Chamber	10^{-5} torr
Particle Throughput to Target Pump	$\sim 3 \times 10^{22}$ part/s

NEUTRAL BEAM LINE PUMPING SYSTEM

(On a Per Arm Basis)

Area of Pumping Panel (Cryopanel or Zr/Al Getter Panels)	120 m ²
Pumping Speed	$\sim 1.2 \times 10^6$ ℓ/s
Operating Pressure in Neutralizer Ducts	$\sim 3 \times 10^{-3}$ torr
Operating Pressure in Source Region	$\sim 5 \times 10^{-5}$ torr
Operating Pressure in Drift Region	$\sim 5 \times 10^{-6}$ torr
Total Gas Throughput to Panels (Including Desorption From Walls)	400 torr ℓ/s

3.2.3 CRYOGENIC REFRIGERATION SYSTEMS

The cryogenic refrigeration systems for the DTHR consist of liquid helium (LH_e) and liquid nitrogen (LN_2) refrigerators and associated equipment required to cool the superconducting magnetic field coils, the dewars and the cryo-panels of the vacuum pumping systems. The major thermal loads for the systems were estimated based on the present preliminary reference reactor design configuration. The LH_e refrigeration loads are summarized as shown in Table 3-22 and the LN_2 in Table 3-23.

The major portion of the LH_e thermal load is for the superconductor cooling for cryostable operation. The cryostability of the Nb_3Sn superconductors used in the TF coils has been analyzed in detail for the TNS⁽³⁻¹⁸⁾ Nb_3Sn TF coil option. The thermal load for the TF coils shown in Table 3-7 in subsection 3.1.3 was estimated based on the results of Reference (3-18).

The PF coils, unlike the steady state TF coils, are subjected to pulsed current reversal during each "on" cycle of operation. The pulsing induces considerable ac losses in the superconductors. The cryostability of the PF coils is discussed in Appendix C. As noted in the appendix, the coolant flow rate for cryostable operation is about 2 g/sec per conductor. The pumping power requirement at this flow rate is very small compared to the ac losses of the cables. The heat loads due to sources other than the PF or TF coils are small, with the bundle divertor being the next highest with ~11kW.

The LN_2 is required mainly to remove the thermal load in the coil dewars. The LN_2 liquefaction required to operate the LH_e refrigeration system, if any, was not included in Table 3-23.

As noted in the cryostability analysis given in Appendix C, the estimated refrigeration loads are considered conservative, but represent significant amounts of refrigeration. The requirements are larger than any commercial system available today. Even though multiple units can be utilized, extensive research is required to develop units with acceptable efficiency and reliability.

TABLE 3-22

SUMMARY OF LH_e REFRIGERATION LOADS

	<u>Heat Load, kW @ 4.2K</u>
Toroidal field coils	43.7
Poloidal field coils	
OH (in-board)	185.3
OH (out-board)	78.1
SF	55.6
Bundle Divertor	11.1
Cryopanels (@ $\sim 1 \text{ W/m}^2$)	0.8
Coil leads (@ $\sim 1 \text{ W/1kA}$)	1.5
<hr/>	
Total LH_e Refrigeration Load	376.1

TABLE 3-23

SUMMARY OF LN_2 REFRIGERATION LOADS

	<u>Heat Load kW @ 77K</u>
Field Coil Cryostat	
TF Coils	155.0
PF Coils	47.0
Bundle Divertor	3.0
Thermal shields for Cryopanels	73.0
(@ $\sim 100 \text{ W/m}^2$)	
<hr/>	
Total LN_2 Refrigeration Load	278.0

3.2.4 FUSILE FUEL HANDLING SYSTEMS

In the planning and execution of the preconceptual design of the fusile fuel handling complex, an array of design issues and considerations had to be treated dealing with such matters as: 1) form and amounts of on-site standby deuterium and tritium to be stored; 2) disposition of the He-3 created by radioactive decay of the stored tritium; 3) means for refueling the plasma; 4) processes for removing impurities from the exhaust plasma; 5) disposition of the tritium contaminated deuterium generated during operation of the neutral beam injectors that is not injected into the plasma; 6) operations mode for the hydrogen-deuterium-tritium isotope separation complex; 7) disposition of the tritiated water generated by facility operations; 8) safety and operational redundancy features to be provided; 9) throughput basis for design of the fuel handling complex; 10) operations mode for treating the plasma exhaust, batch or continuous or a mixture of these, and whether to perform these operations on-site or elsewhere at a DoE facility. Table 3-24 summarizes the generic specifics of the design issues and considerations.

In general, resolution of these issues was guided by a weighing of advantages and disadvantages of competing approaches to establish the relevant system capabilities and the opted preferred manner for achieving these capabilities. Space limitations do not allow an exposition here of the resolution routines that were applied and which also factored in such matters as existing technology base and conclusions reached in previous system designs for TFTR, TNS, TSTA and the actinide burner and hybrid reactor studies conducted by Westinghouse.

All of these capabilities will be required in future fusion reactors based on magnetic confinement, whether primarily hybrid fissile fuel producers or electrical or process heat generators. Fuel handling system capabilities potentially required for these devices but not provided for in the initial DTHR system include: 1) breeding of sufficient tritium to replace that destroyed by fusion and radioactive decay, fuel processing losses, and depletions via transport into the plant metals and environment; 2) removal and recycle of tritium diffused into equipment, coolants, and power-cycle energy transport media, or exhausted from the plasma.

The initial DTHR fusile fuel handling system is based on a once-through concept wherein the plasma exhaust and the contaminated deuterium generated by the neutral beams would be collected and dispatched to a DoE facility for cleanup, recovery,

TABLE 3-24

DESIGN ISSUES AND CONSIDERATIONS

DTHR FUSILE FUEL SYSTEMS

ON-SITE STANDBY TRITIUM INVENTORY

- Gaseous vs Liquid vs Solidified
 - U vs other getters if solidified
- Operating Days Worth of Inventory - 1 day vs more days
- Single Storage Unit vs Multiple Units

ON-SITE STANDBY DEUTERIUM INVENTORY

- Gaseous vs Liquid vs Generated from D_2O
- Operating Days Worth of Inventory - 1 day vs many days

REMOVAL OF He-3 FROM STORED TRITIUM

- Vacuum Stripping vs Pd Alloy Diffusers
- Second Stage Recovery of Tritium from He-3 Stream
 - Gettering with U vs ZrAl alloy

REFUELING OF PLASMA

- Pellets vs Liquid Jets
- Capabilities for Injection D and T Separately vs D, T and DT
- Method of Fuel Injection

REMOVAL OF IMPURITIES FROM PLASMA EXHAUST

- Chemical vs Physical vs Combinations
 - Hot and cold getter trapping vs selective cryogenic trapping vs selective permeation
 - Gettering with U vs other reactive metals
 - Disposition of non-reactive noble gases

TABLE 3-24 (Continued)

HOW TO HANDLE CONTAMINATED DEUTERIUM TRAPPED BY NEUTRAL BEAM VACUUM SYSTEM

- Recycle without Decontamination up to Limiting Tritium Buildup and then Discard
- Decontaminate and Recycle
- Discard and Replenish from Fresh Stock

CRYOGENIC DISTILLATION FOR ISOTOPE SEPARATION

- Continuous vs Batch Operation
- Treatment of Neutral Beam Contaminated Deuterium Separately vs Combining into Unified Complex
- Product Stream Capabilities - T, D, D-T, D-H, H

DISPOSITION OF TRITIATED WATER GENERATED BY CLEANUP SYSTEMS

- Complete Discard vs Selected Discard vs Recover-Recycle

SAFETY AND OPERATIONAL REDUNDANCY

- Minimal Approach vs Ultra-Flexibility and Flow Path and Equipment Redundancies
- Multiple vs Single Cleanup Systems
- Provision for Trapping of In-Process Tritium in Event of System Malfunctions, Power Failure - as Gas vs Solidified

MATERIALS THROUGHPUT BASIS

- Conservative vs Optimistic Plasma Physics Performance, Running from Intensely Driven Beam (TCT) to Ignition Operation

OPERATIONS MODES FOR FACILITY AND FUEL HANDLING SYSTEM

- Continuous Operation vs Batch Operation
- Extended Periods of Continuous Operation of Facility vs Short Operating Periods with Frequent Outages

separation and recycle back to DTHR. An option that may be added later would provide for reprocessing-recycle of the plasma exhaust on-site as an integral part of DTHR. The principal requirements for this plasma exhaust treatment complex are also described in this subsection in considerable detail.

A most crucial design issue that had to be settled is the basis for establishing the materials throughput for the fusile fuel handling system. This is an important matter in that the materials handling capability that has to be provided impacts very substantially on the size and cost of the hardware and the amount of on-site standby tritium and deuterium storage requirements. Additional adverse consequences of a large tritium inventory requirement are the need for larger tritium replenishments from DoE stocks to supplant material destroyed by radioactive decay (losses amount to 5.45% per year), increased operating costs (mainly for electrical energy), and a larger thermal dump to the facility heat dissipation system.

The materials throughput issue was resolved by asserting that the fusile fuel system has to be able to handle the most demanding load imposed on it, as directed by the machine plasma operating regimes that may be encountered.

Because a 100% beam driven machine operating in the TCT or the near ignition mode is the most demanding from the viewpoint of required input of deuterium and tritium per fusion event, the TCT operating mode was selected as the basis for sizing the fusile fuel handling system. This higher requirement derives from the following effects: 1) the prefill is 100% T, rather than 1:1 D:T for the ignition or near ignition (beam driven thermonuclear, BDTN) modes; 2) The T:D ratio in the plasma, during the steady-state burn portion of a pulse, is higher at $n_T/n_D \approx 2.9$ or $\sim 75\%$ T in the plasma, compared to $n_T/n_D = 1$ for the ignition mode; 3) Injection of neutral beam D persists throughout the entire burn period; 4) The fraction of the fuel injected into the plasma that undergoes a fusion reaction (on the order of 0.3-1%) is small, therefore demanding more reprocessing-recycling; 5) Due to the substantial D_2^0 content of the neutral beam injected material and the long injection period, this imposes a requirement for larger amounts of refueling T in order to sustain the desired n_T/n_D ratio in the plasma; 6) In the current neutral beam injection devices, very

substantial amounts of D have to be fed to the devices in excess of that injected into the plasma, amounting to ~1000% of the D entering the plasma. Because this material becomes contaminated with T, it has to be reprocessed. This contribution to the load of the processing-recovery complex is directly related to the beam-on duration per pulse, and also depends on the beam power and energy level.

For the initial DTHR design collection of the spent material and shipment to the off-site reprocessing facility would proceed coincidentally with the plant operating schedule. The collected residues would be shipped to the DoE facility as either compressed gas or solidified on getters in special containments.

In the case of the on-site plasma exhaust treatment option, the reprocessing portion of the fusile fuel handling system would be expected to operate semi-continuously. The demonstration plant onstream factor of 25% over a calendar year is anticipated; however, it is expected that around-the-clock steady operation periods of several weeks or more duration will occur. Such production type runs are motivated by such incentives as: 1) demonstrating the capability of the facility to operate trouble-free in a sustained mode; 2) exceeding the nominal fissile fuel production target by a significant margin; 3) gathering large amounts of performance data and sustained operating experience afforded by longer term operation subsequent to machine fine-tuning. Fulfillment of these goals dictates a design for the plasma exhaust processing system that can sustain around-the-clock continuous operation for extended periods, including virtual functioning with a 100% plant on-stream factor.

In some respects, the plasma exhaust reprocessing portion of the system for the on-site option would really operate in a batch mode. This will also be the case for eventual fusion power reactor fuel processing systems. This is so because regeneration of the working medium of many of the treatment devices are by nature a batch operation. This applies for example to the cryopanel and getters of the vacuum pumps, the divertor, purification traps, and desiccant beds. Incorporation of accumulators and surge tanks into the equipment array that functions batchwise results in a system that performs as though it were essentially in the continuous operating mode.

During inactive periods of DTHR operations, the cryogenic distillation complex will normally operate under total reflux conditions; however, in some instances during plant outages, it may operate on accumulated feed in a "catch-up" campaign. Continuous operation of this complex obviates the difficulties associated with start-up of multicomponent cryogenic rectifiers such as: 1) long time required to cool down the equipment, build up the concentration gradients, equilibrate the regenerative cold-exchanger maze, and establish the proper thermal balances among the reboilers and condensers; 2) complete loss of column concentration gradients when the complex is shut down; 3) propensity for leak development associated with up and down thermal transients accompanying system complex startup and shutdown.

The configuration for the fusile fuel system depends upon the disposition of the design issues and considerations listed in Table 3-24, the interface with other DTHR systems, the system general capabilities, and the reactor ground rules established. The following paragraphs will discuss the fusile fueling, exhaust handling, safety features, containment and cleanup systems. Parameters for the DTHR fusile fuel handling system will be given.

FUELING

The prefill is gaseous T in amount required to yield a plasma with volume, V_p , and density, n_e , for the reference case. Prefilling is accomplished within 0.2 s after completion of vacuum vessel pumpdown. Capability also exists for prefilling the vessel with gaseous 1:1 D:T.

Refueling is in the form of T solid pellets and neutral beam D for the reference case, in amounts necessary to make up for destruction by fusion and removal by the divertor, and to maintain the desired D^+ and T^+ density in the plasma up to the onset of quench. To provide for operational flexibility, the capability also includes means to inject solid pellets of 1:1 D:T and D.

For the reference case, all of the D refueling is accomplished by neutral beam injection, with a capability to inject with powers up to 150 MW and energies up to 200 keV, using multiple numbers (N_b) of neutral beam arms.

Based on present state-of-the-art for neutral beam devices, their relevant operational characteristics are:

- The ratio of monatomic to diatomic D power (P_b/P_{b2}) delivered to the plasma is 1.14.
- The ratio of atoms of D injected into the plasma to atoms of D required for the neutralizer and subsequently trapped on the cryopanel of the beam vacuum pumps as T contaminated D is 0.0988 for a standard 200 keV neutral beam arm.
- A "standard 200 keV neutral beam arm" contains eight sources, with ~23 MW supplied by the 200 keV monatomic D and ~19 MW supplied by the 100 keV diatomic component.

Exhaust Plasma Trapping and Processing

Primary trapping of the exhaust plasma during the burn period is accomplished by the bundle divertor. Periodic regeneration of the solid getter of the divertor burial chamber reliberates the exhaust plasma components as a gas at ~1070 K which, in the case of the on-site reprocessing option, would be transported to the tritium recovery and reprocessing systems for removal of impurities and reconstitution of the D and T. Trapping of the residual spent plasma in the vacuum vessel after completion of the burn is accomplished by the vacuum system comprising appendage-type compound cryo-pumps using 4.2 K chevrons to pump H-D-T, and 4.2 K molecular sieve panels to pump He. The pumps immobilize and store the trapped components and reliberate them to the tritium recovery and reprocessing systems periodically during regeneration of the pump cryosurfaces at ~40K. The amount of material that escapes from the plasma and enters the vacuum pumping system during the pulse burn period is negligible. Hydrogen transport into the plasma is based on an outgas rate of 10^{-10} torr liter/s cm^3 , and the effective outgas surface is estimated to be ten times that of the calculated smooth internal area of an assumed elliptical-shaped toroid vacuum vessel.

Tritium processing losses (excluding radioactive decay) during recovery-recycle operations on an annual basis would amount to 0.0015% of the T content total plasma exhaust processed per year. Processing-purification of the exhaust plasma and contaminated D generated by the neutral beam injectors is accomplished by combinations of hot-trap gettering, mechanical filtration, cold-trap gettering, cryogenic trapping, cryogenic distillation, catalytic cracking-oxidation, adsorption-desiccation, selective permeation-diffusion, and electrolytic dissociation.

The hot traps utilize depleted U chips as the getter. The design of these units is based on reaction of the trappable impurities to form essentially non-regenerable non-stoichiometric materials with general formula $UX_{2.8}$. The total trapping capability of a unit is equivalent to two calendar years of operation at a 40% plant availability factor, assuming that 0.5 a/o of the exhaust plasma is trappable impurity, with a 100% excess for the depleted U charge per unit above that required to provide the foregoing capability. The bulk density of the loosely packed U chips is 2 g/cm^3 . The internal volume of a hot trapping unit is four times the volume of the U chips, with a right cylindrical configuration having a 2:1 height to diameter ratio. The material of construction for the bodies of the hot trapping units is 316 SS. The nominal operating temperature for the hot traps is 700K.

The cold traps for H-D-T pickup from the plasma exhaust and contaminated D pickup from the neutral beam injectors both utilize depleted U as the getter. While functioning as cold traps, the operating temperature is $\sim 320 \text{ K}$; regeneration is conducted at 700 K. The design of these units is based on formation of $UH_{2.8}$, $UD_{2.8}$ and $UT_{2.8}$. The total trapping capability of a H-D-T exhaust plasma cold trap unit is the equivalent of seven full days of operation in the beam driven mode, with an allowance for 30% excess U over the stoichiometrically required loading. The total trapping capability of a contaminated D neutral beam cold trap is the equivalent of five full days of operation in the TCT reference mode, with an allowance for 30% excess U over the stoichiometrically required loading. The bulk density of the loosely packed U chips is 2 g/cm^3 .

The internal volume of a cold trapping unit is four times the volume of the U chips, with a right cylindrical configuration having a 2:1 height to diameter ratio. The material of construction for the bodies of the cold trapping units is 316-SS.

The cryogenic distillation complex has capability for producing adequately pure product streams of T, D, D-T and an H-HD waste stream from the H-D-T and contaminated D feeds originating from the exhaust plasma and cryoadsorbent panels of the neutral beam vacuum system. For the reference operating mode, the principal product streams are T and D. For operations involving ignited plasmas, the principal products include T, D and D-T. The design must include flexibility to operate in either mode.

The upper limit for loading Zr-Al getter cartridges (SAES C-500 units) with T is 2500 torr liters per cartridge. Nominal operating and regenerating temperatures are ~ 430 K and ~ 1000 K, respectively. No more than six cartridges are to be housed in a single enclosure canister.

General specifications for some of the other equipment are: Normal operation on the upstream side of the palladium-silver alloy diffusers is not to exceed 650 K and 1.5 MPa (~ 28 atm); the maximum pressure for in-process gas holdup T containing storage tanks is 0.2 MPa (~ 4 atm); the nominal capacity of the tritiated water electrolyzer is 10 kg of water per full operating day.

Redundancy and Principal Safety Features

In the case of the on-site reprocessing option, redundancy would be provided for gas holdup tanks, liquid and surge tanks, in-process transfer lines, isolation-control valves, circulating and transfer pumps and compressors for matters of safety, ensuring operational continuity and to facilitate alternate routing of materials. Duplicate equipment is provided for such crucial items as fuel pellet fabricators and injectors, cryogenic distillation columns, diffusers and T supply receiving stations.

Thick-wall double jacketing is specified for the three T storage-generator units, with 3.0 cm thick cylindrical walls and 5.0 cm thick heads, and a 4.0 cm annular clearance between the interior and outer bodies. The T storage-generator units are housed in separate sealed, inerted, steel-lined (2.0 cm thick lining) concrete (30 cm thick walls) cubicles. Specifications regarding the cubicles are: Rectangular shape, with 1.0 m clearance on all sides between the outer body of the storage-generator unit and the cubicle interior wall; the atmosphere in the cubicles is Ar, with provision to circulate this inert containment gas through a detritiation system in closed loop recycling mode through any one or a combination of cubicles; each cubicle is outfitted to undergo emergency rapid cooling-quenching, with the refrigeration for this duty supplied by a pair of closed cycle package-type coolers, using cryogenic Ar as the coolant to cool any one or a combination of cubicles. The T storage-generator units are supplied with a set of porous metal particulate filters to prevent U fines carryover and diffusers to assist removal of radioactive decay generated He-3 after any long-term storage. Usually, evacuation of the U beds before regeneration will provide adequate He-3 removal.

All equipment utilized in making gaseous prefills involving T is housed within a glove box. All of the hot and cold U traps are housed in separate sealed, inerted, steel-lined concrete cubicles. Specifications for these are identical to those cited for the T storage-generator units. Timing of regeneration of the cryoadsorption panels of the vacuum systems is such as to limit the maximum hydrogen isotope partial pressure in the associated volumes under full regenerating conditions to less than ~ 10 torr. This pressure is approximately one-fourth of the lower combustibility limit for hydrogen, thereby providing a 300% safety factor in the event of air inleakage into the vacuum equipment. An array of depleted U traps are included at strategic locations in the system, sized to be capable of trapping the entire contents of the cryoadsorption panels and cryogenic distillation system in the event of auxiliary equipment or complete electrical power failure.

Tritium Containment and Cleanup

All equipment in which processing of T bearing materials is conducted is doubly enclosed and the volumes contained within these jackets are to be continuously flushed by a circulating inert buffer gas, which is treated by a cleanup system to maintain a low cover gas T level. Pumps for T processing operations, in general, are not to have rotating mechanical seals. If this criterion cannot be met, complete containment of the pump and driver within a separate sealed enclosure is required.

Tritium cleanup systems are provided for process streams, containment sweep streams and atmospheres, with adequate processing rates in closed-cycle mode through the cleanup systems. He sweep streams of T through the annuli of the storage-generator units and hot and cold U trapping units need a flow adequate to provide complete volume turnover in one hour or a cleanout time of one day in the event of a major release occurrence. Ar sweep streams through the cubicles of the T storage-generator units and the hot and cold U traps need a flow sufficient to provide 25 complete volume turnovers within two days. He sweep stream of the T and D-T prefill glove box needs a flow that provides 25 complete volume turnovers within one day. Ar sweep stream through the vault that houses the T and D-T prefill glove box needs a flow that accomplishes 25 complete volume turnovers within two days. Off-gas stream (principally He and H-D) prior to discharge to the environment, needs a flow capability of $\sim 0.12 \text{ STP m}^3/\text{min}$. He sweep-purge stream circulated through the secondary containment system (including pump enclosures, double pipe jackets) needs a flow capability of $4 \text{ m}^3/\text{min}$. Ar back-fill atmosphere inserted in the vacuum vessel to facilitate detritiation prior to opening the vessel for maintenance, needs a flow adequate to achieve ~ 30 complete volume turnovers in one day. Air atmosphere of the reactor cell needs a flow rate that provides ~ 30 complete volume turnovers in five days in connection with detritiating the cell following a major release into the cell.

FUSILE FUELING SYSTEM PARAMETERS

The magnitude of various fusile fuel handling system quantities for DTHR operating in the 100% beam driven mode are determined based on the relevant parameters shown in Table 3-25. Table 3-26 presents an overall summary of the fusile fuel system parameters and operating quantities. Table 3-27 furnishes additional specifics regarding the makeup of the exhaust plasma, feed to the cryogenic distillation complex, and processing related tritium losses. In all instances, the numbers displayed in Tables 3-26 and 3-27 are for a full 24-hour operating day, corresponding to 1016 pulses/day.

A major point of concern regarding DTHR operation is the size of the on-site standby tritium inventory, which in the "example case" was set at one full operating days worth. The concern is accentuated by the circumstances that: 1) a substantial tritium loss is inevitable due to the 5.45% per year depletion via radioactive decay irrespective of whether or not the facility is operating; 2) initially, DTHR will not include tritium breeding, but will obtain this fuel from other sources. The tritium replenishment required to maintain even a modest amount of tritium in reserve is shown in Figure 3-30. Not providing a modest inventory would result in complete shutdown of operations in the event that a breakdown occurs in a vital component of the plasma exhaust processing system, particularly the cryogenic distillation complex. Such a crucial situation could occur through such an apparently innocuous malfunction as a ~15 minute power outage, which would lead to remixing of the contents of the columns, resulting in minimally a several day lag before satisfactory separative ability is restored.

A related issue is the requirement on the reserve tritium needed if DTHR were designed to be self-sufficient. Figure 3-31 shows the effect of plant availability factor (fraction of plant on-stream time) and inventory on the breeding that would be required. The plot highlights the severe impact that even a modest reserve fuel supply makes on tritium breeder blanket design, if tritium self-sufficiency is a requirement.

TABLE 3-25

RELEVANT PARAMETERS FOR FUSILE FUELING REQUIREMENTS IN DTHR
BEAM DRIVEN OPERATING MODE

<u>Item</u>	<u>Magnitude</u>
Plasma minor radius (a)	1.2 m
Plasma major radius (R)	5.2 m
Plasma elongation (δ)	1.6
Plasma volume (V_p)	236.5 m ³
Electron density (n_e)	$1.5 \times 10^{20}/\text{m}^3$
Tritium ion density (n_T)	$1.11 \times 10^{20}/\text{m}^3$
Deuterium ion density (n_D)	$3.9 \times 10^{19}/\text{m}^3$
Fusion power (P_F)	950 MW
Neutral beam energy (E_b)	200 keV
Monatomic neutral beam power contribution (P_b)	150 MW
Diatomic neutral beam power contribution (P_{b2})	132 MW
Limiter-scrapeoff layer radial increment (Δ)	0.2 m
Number of neutral beam arms (N_b)	6
Ratio of vacuum vessel surface to plasma volume (ψ)	$1.55 \text{ m}^2/\text{m}^3$
Plasma burn time per pulse (θ_b)	70 s
Vacuum vessel pumpdown time per pulse (θ_p)	15 s
Lag time for initiating tritium pellet injection (θ^*)	0.85 s

TABLE 3-26

DTHR FUSILE FUEL SYSTEM PARAMETER SUMMARY

(BEAM DRIVEN OPERATING MODE)

TRITIUM INVENTORY

On-Site Standby Inventory	
Form	Solidified, UT _{2.8}
Number of Storage-Generator Units	3
Number of Full Operating Day Worths	1
Quantity	
kg	13
M Ci	126
In-Process Inventory	
Cryogenic Distillation Complex	
kg	0.5
M Ci	4.8
Other Processing Equipment	
kg	1.1
M Ci	10.4
Total On-Site Average Inventory	
kg	68
M Ci	645

DEUTERIUM INVENTORY

On-Site Standby Inventory	
Form	Compressed Gas
No. of Storage Units-On-Site/In-Transit	2/1
No. of Full Operating Day Worths	14
Quantity	
kg	436
STP m ³	2,428
In-Process Inventory	
Cryogenic Distillation Complex, kg	1.2
Other Processing Equipment, kg	2.6
Total On-Site Average Inventory, kg	440

FUELING METHODS

Intensely Beam Driven	
Gaseous Prefill	100% T
Deuterium Supply	100% D via neutral beams
Refuel Tritium	100% T pellets
Near Ignition (Beam Driven) Mode	
Gaseous Prefill	100% T
Deuterium Supply and Refuel	Neutral beam
Refuel Tritium	T pellets

TABLE 3-26 (Continued)

Ignition Mode Gaseous Prefill Deuterium Supply and Refuel Refuel Tritium		DT Neutral beam, DT pellets T, DT pellets	
FUELING QUANTITIES (BDTN/TCT REFERENCE MODE)		Per Pulse	Per Full Operating Day
Gaseous Tritium Prefill, g		$1.777 \cdot 10^{-1}$	$1.806 \cdot 10^2$
	Ci	$1.704 \cdot 10^3$	$1.732 \cdot 10^6$
Tritium Pellet Refueling, g		$1.274 \cdot 10^1$	$1.295 \cdot 10^4$
	Ci	$1.221 \cdot 10^5$	$1.241 \cdot 10^8$
Total Tritium, g		$1.291 \cdot 10^1$	$1.313 \cdot 10^4$
	Ci	$1.283 \cdot 10^5$	$1.259 \cdot 10^8$
Deuterium Fueling, g		3.025	$3.075 \cdot 10^3$
Deuterium Trapped by Neutral Beam Vacuum System, g		$2.764 \cdot 10^1$	$2.810 \cdot 10^4$
Total Deuterium, g		$3.066 \cdot 10^1$	$3.117 \cdot 10^4$
FUEL DEPLETIONS VIA FUSION			
Tritium, g		$1.182 \cdot 10^{-1}$	$1.202 \cdot 10^2$
	Ci	$1.134 \cdot 10^3$	$1.153 \cdot 10^6$
% of Matl. Injected into Plasma		$9.157 \cdot 10^{-1}$	$9.157 \cdot 10^{-1}$
Deuterium, g		$7.896 \cdot 10^{-2}$	$8.026 \cdot 10^1$
% of Matl. Injected into Plasma		2.611	2.611
% of Total Matl. Fed to Neutral Beam Machines		$2.575 \cdot 10^{-1}$	$2.575 \cdot 10^{-1}$
FUEL RESIDUALS TO PLASMA EXHAUST PROCESSING			
Tritium, g		$1.280 \cdot 10^1$	$1.351 \cdot 10^4$
	Ci	$1.227 \cdot 10^5$	$1.247 \cdot 10^8$
% of Matl. Injected into Plasma		$9.908 \cdot 10^1$	$9.908 \cdot 10^1$
Deuterium, g		$3.058 \cdot 10^1$	$3.109 \cdot 10^4$
% of Matl. Injected into Plasma		$1.011 \cdot 10^3$	$1.011 \cdot 10^3$
% of Total Matl. Fed to Neutral Beam Machines		$9.974 \cdot 10^1$	$9.974 \cdot 10^1$

TABLE 3-26 (Continued)

DEPLETED URANIUM QUANTITIES*	<u>Metric Tons</u>	<u>Full Days Capability</u>
Tritium Storage-Generator Units (3)	3.3	5
Hot Traps (2)	2.9	730
Cold Traps for Plasma Exhaust (2)	9.0	7
Cold Traps for Neutral Beam Contaminated Deuterium (2)	15.4	5
Emergency Cold Traps		
Cryogenic Distillation Complex Dump (1)	0.07	-
Liquid Fuel Feed to Pelletizers (1)	0.03	-
Total	30.7	-

CLEANUP UNITS FOR FUSILE FUEL HANDLING SYSTEM*	<u>Carrier Gas</u>	<u>Flow m³/min</u>
Tritium Storage-Generator Units		
Annuli	He	0.42
Cubicles	Ar	5.1
Hot and Cold Uranium Traps		
Annuli	He	0.42
Cubicles	Ar	5.1
Tritium and Deuterium-Tritium Gas Prefill		
Glove Box	He	0.08
Vault	Ar	0.15
Secondary Containments	He	4.0
Off-Gases to Environment	He, H-D	0.12
Vacuum Vessel	Ar	6.1
Reactor Cell	Air	260

* Sized to accommodate the future addition of an on-site plasma exhaust reprocessing option.

TABLE 3-27

PROCESSING SYSTEM SPENT FUEL QUANTITIES*
Beam Driven Operating Mode

<u>Item</u>	<u>T</u>	<u>D</u>	<u>H</u>	<u>He-4</u>
Exhaust Plasma, Divertor & Vacuum, Decay				
g atoms/pulse	4.242	1.463	$3.665 \cdot 10^{-5}$	$3.920 \cdot 10^{-2}$
kg/pulse	$1.280 \cdot 10^{-2}$	$2.946 \cdot 10^{-3}$	$3.694 \cdot 10^{-8}$	$1.569 \cdot 10^{-4}$
Ci/pulse	$1.227 \cdot 10^5$	-	-	-
g atoms/day	$4.312 \cdot 10^3$	$1.487 \cdot 10^3$	$3.726 \cdot 10^{-2}$	$4.046 \cdot 10^1$
kg/day	$1.301 \cdot 10^1$	2.994	$3.753 \cdot 10^{-5}$	$1.619 \cdot 10^{-1}$
Ci/day	$1.247 \cdot 10^8$	-	-	-
Contaminated D from NB Injectors				
g atoms/pulse	-	$1.372 \cdot 10^1$	-	-
kg/pulse	-	$2.714 \cdot 10^{-2}$	-	-
g atoms/day	-	$1.395 \cdot 10^4$	-	-
kg/day	-	$2.809 \cdot 10^1$	-	-
Composite Feed to Cryogenic Distillation				
g atoms/day	$4.312 \cdot 10^3$	$1.544 \cdot 10^4$	$3.726 \cdot 10^{-2}$	-
kg/day	$1.301 \cdot 10^1$	$3.109 \cdot 10^1$	$3.753 \cdot 10^{-5}$	-
Ci/day	$1.247 \cdot 10^8$	-	-	-
Atom fraction in feed	$2.183 \cdot 10^{-1}$	$7.817 \cdot 10^{-1}$	$1.887 \cdot 10^{-6}$	-
Processing Losses				
g atoms/day	$6.468 \cdot 10^{-2}$	$2.316 \cdot 10^{-1}$	-	-
kg/day	$1.951 \cdot 10^{-4}$	$4.664 \cdot 10^{-4}$	-	-
Ci/day	$1.871 \cdot 10^3$	-	-	-
Radioactive Decay Losses**				
g atoms/day	$7.001 \cdot 10^{-1}$			
kg/day	$2.112 \cdot 10^{-3}$			
Ci/day	$2.025 \cdot 10^4$			

* Based on on-site plasma exhaust reprocessing option.

** Does not include T radioactive decay losses for on-site standby inventory, amounting to $1.879 \cdot 10^4$ Ci/day.

Figure 3-30

Effect of Tritium Inventory and Plant Availability Factor on Tritium Replenishment Requirements

Figure 3-31 Dependence of Required Tritium Breeding Ratio on On-Site Standby Tritium Inventory and Plant Availability Factor

Table 3-28 scopes the electrical power and thermal heat dumps associated with operation of the fusile fuel handling system, broken down by subsystem for the example case. In comparison to the loads of other major DTHR systems, the fuel handling system requirements are very modest.

PROCESS FLOW SCHEMATICS

A number of "black-box" type flow schematics for the overall fusile fuel handling system and some of its subsystems and related operations were developed, primarily to facilitate making hardware take-offs in connection with generating system cost models and algorithms. Some of these are displayed in Figure 3-32 through Figure 3-34. Figure 3-32 shows the basic character of the overall system in terms of identifying the major components and their configuring. Not shown are duplicate units, pumps, contents of the black-boxes, etc. Figure 3-33 indicates the nature of the hookup required for the tritium storage-generator units. Figure 3-34 elaborates on the black-box displays of Figure 3-32 regarding configuring of the plasma exhaust, off-gas and secondary containment sweep gas cleanup subsystems.

CLEANUP SYSTEMS

For a design that includes an on-site plasma exhaust reprocessing option, ten cleanup (detritiation) subsystems are required. Of these, eight are integral members of the fusile fuel handling complex operation on a continuous basis. The remaining two are operated relatively infrequently in connection with reactor vessel maintenance activities and reactor cell atmosphere cleanup. Table 3-29 lists the basic features of the ten subsystems with regard to system involved, gas to be cleaned, flow rate, number of system volume turnovers deemed required to achieve cleanup, utilization of carrier techniques to enhance decontamination, and whether or not recovery-recycling of the tritium is to be practiced.

The essentials of the makeup of the eight cleanup subsystems associated with the fusile fuel handling and their integration into the complex is shown in Figure 3-34.

Figure 3-35 is a flow schematic of the vacuum vessel (torus) cleanup system. The function of this system is to clean up the torus atmosphere and its surfaces

TABLE 3-28

BREAKDOWN SUMMARY OF ELECTRICAL AND THERMAL LOADS
DTHR FUSILE FUEL HANDLING SYSTEM*

(Beam Driven Operating Mode)

<u>Subsystem or Component</u>	<u>kW Electrical</u>	<u>kW Thermal Dump</u>
Tritium Storage-Generator Units	92.1	96.6
Tritium Pellet Fabrication and Injection		
Refrigeration	103.4	133.4
Injection	273.0	-
Exhaust Plasma Processing		
Divertor Product Cooling	-	0.4
Hot Uranium Traps	101.3	92.1
Hot Uranium Trap Product Cooling	-	0.5
Cold Uranium Traps		
Regeneration	122.8	-
Cooldown	102.3	460.5
Recovery-Recycle of Contaminated D from Neutral Beams		
Cold U Trap Regeneration	399.4	-
Trap Cooldown	331.7	1498
Gas Cooldown	-	1.2
Cryogenic Distillation		
H-D-T Liquefaction	26.2	43.7
D Liquefaction	73.1	122.9
Refrigeration for Condensers	1083	1823
Catalytic Equilibrator	15.2	0.7
Cleanup Systems		
Tritium Storage-Generator Cubicles	4.3	1.9
Cold & Hot Uranium Trap Cubicles	52.8	23.6
T and D-T Gas Prefill Box	0.8	0.4
Gas Prefill Vault	1.6	0.7
Off-Gas	1.2	0.5
Secondary Containment	41.0	18.1
Torus**	(2250)	(1004)
Reactor Cell**	(3174)	(1681)
Molecular Sieve Regeneration	3.3	0.3
Pumps and Compressors		
Tritium Makeup Diffuser	1.4	-
Neutral Beam Contaminated D	0.6	-
Divertor and Vacuum Pump H-D-T	0.3	-
Cryodistillation Complex Liquid Pumps	0.1	-
Contaminated Water Electrolyzer	3.3	-
Miscellaneous	200	60
Total Continuous Loads	<u>3034</u>	<u>4379</u>

* Based on an on-site plasma exhaust reprocessing option.

** Operate intermittently

Figure 3-32. Simplified Flow Schematic - Preliminary DTHR Fusile Fuel Supply and Treatment

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Figure 3-34. Flow Schematic for Processing Plasma Exhaust, Contaminated Deuterium and Secondary Containment Sweep Gas

TABLE 3-29
TRITIUM CLEANUP AND DECONTAMINATION SYSTEMS

	<u>Carrier Gas</u>	<u>Number of Volume Turnovers for Emergencies</u>	<u>Flow Rate m³/min</u>	<u>Controlled Additions</u>		<u>Recover- Recycle T*</u>
				<u>0</u>	<u>H</u>	
T Storage-Generator Units						
Annuli	He	24	0.42	Yes	No	Yes
Cubicles	Ar	25	5.1	Yes	Yes	Yes
Hot and Cold U Traps *						
Annuli	He	24	0.42	Yes	No	Yes
Cubicles	Ar	25	5.1	Yes	Yes	Yes
T and D-T Gas Prefil						
Glove-Box	He	25	0.08	Yes	No	No
Vault	Ar	25	0.15	Yes	No	No
Secondary Containments*	He	--	4.0	Yes	No	Yes
Off Gases to Environment*	He, H-D	--	0.12	Yes	Yes	No
Vacuum Vessel	Ar	30	6.1	Yes	Yes	No
Reactor Cell	Air	30	260	No	Yes	No

* Based on an on-site plasma exhaust reprocessing option.

Figure 3-35. Torus Cleanup System Flow Schematic

from adsorbed layers containing tritium to the extent practicable, prior to opening it up and performing maintenance. Prior discharge cleaning as now performed in PLT and planned for TFTR may help removal of adsorbed tritium held up on the interior surfaces. There will be tritium diffused into the vessel walls and this will tend to counter-diffuse back to the inner surfaces under sustained vacuum conditions and elevated temperature. Simultaneously, previously transported-in tritium will also be driven toward the outer surface. The layout of this system is similar to that shown in Figure 3-34 for the secondary containment cleanup system, with the torus replacing the secondary containment site in the flow schematic. The intention is to use the carrier technique, adding oxygen and hydrogen ahead of the catalytic oxidizer and water ahead of the second dryer, with argon as the carrier-backfill gas. After repeated closed cycle operation has yielded the desired decontamination level, the detritiated argon would be dispatched to the stack.

The atmosphere cleanup system for the reactor cell is expected to be similar to that previously conceived for TNS and TFTR. The schematic layout is shown in Figure 3-36. The basis is catalytic oxidation of the tritium followed by desiccation, operating the cell and cleanup equipment in a tight, closed cycle mode until the tritium concentration attains an acceptable level. To achieve good decontamination: 1) the operation and hookup must be close coupled to the cell ventilation system and means to achieve intimate air mixing must be provided; 2) surface treatment of the cell wall surfaces is required to minimize tritium adsorption and moisture transpiration into the cell from the walls; 3) the cell construction must be such as to provide a tight structure, with very minimal air leakage; 4) after cleanup of the atmosphere has been substantially effected ($\sim + 99.9\%$), closed cycle additional operation with moisture injection into the cell atmosphere with collection of the condensate (drips off the air conditioning coils) can assist cell wall and equipment surface detritiation. The condensate can be dumped if sufficiently below mpc, or may have to be disposed otherwise.

Figure 3-36. Schematic of Reactor Cell Cleanup System Integrated with Cell Ventilation System

3.2.5 VACUUM VESSEL AND SHIELD COOLING SYSTEMS

The vacuum vessel of the DTHR is of a tubular design consisting of tube banks which are supported by a shell and stiffening rings. The tubes are cooled by pressurized water. The general design description of the vessel structure is given in subsection 3.1.6. The cooling tubes are arranged in the toroidal direction with the manifolds arranged in the poloidal direction. Commercial grade 316 stainless steel tubing with 0.953 cm (3/8") O.D. is used as the cooling tubes.

The cooling system design objective is to cool the tubes and the shell to or below the allowable working temperature of the material with minimal pumping power. The working temperature limit of the 316 stainless steel is about 450°C, above which the maximum allowable stresses of the material drop off rapidly. The thermal heat flux used in the design calculation was taken to be 2 MW/m² and the nuclear heating rate was 36 W/cm³. The heat flux considered in the analysis is extremely conservative, since the neutron wall loading of the DTHR is no higher than 2 MW/m². Two different cooling water pressures were studied for the cooling system. The results are summarized in Table 3-30.

Good design practice is to allow for a hot channel safety margin ($\sim 20^\circ \text{C}$) between the saturation temperature and the coolant outlet temperature. As can be noted from Table 3-30, further design iteration would be required to achieve this for both the conditions analyzed. Nonuniformities in incident flux and heat generation were accounted for in analyzing the maximum tube and shell temperatures.

It can be seen from the table that operating the cooling system at a higher pressure would require less coolant flow and pumping power. In addition, the maximum material temperature is closer to the selected allowable limit with water pressure at 6.89 MPa. A cooling system pressure of 6.89 MPa was therefore selected.

Cooling with helium gas was also investigated. Because of the low density of the gas, in order to have a reasonable pumping power requirement, the gas pressure should be higher than 6.89 MPa and the channel length be kept small.

TABLE 3-30

SUMMARY OF VESSEL FIRST WALL COOLING PARAMETERS

	<u>6.89 (1000 psi)</u>	<u>3.45 (500 psi)</u>
Inlet pressure, MPa		
Channel Length, m	4	4
Outlet pressure, MPa	6.85	3.38
Channel ΔP , MPa	0.04	0.08
Inlet temperature, C	74	74
Outlet temperature, C	283	232
Saturation Temperature at Channel Exit, C	284	240
Total flow rate ⁽¹⁾ , kg/hr.	39.8×10^5	58.9×10^5
Maximum tube temperature, C	427	344
Maximum shell temperature, C	394	343
Pumping power ⁽²⁾ , kW	126	234

Notes:

(1) Based on 360 m² vessel surface area and a total of 9000 cooling channels.

(2) Based on 50% pump efficiency.

With a helium inlet pressure at 6.89 MPa, channel length of 1.5 m and the same size of tubes, the pumping power requirement with helium is several hundred times greater than with water. The trade-off between pumping power of helium with the neutronic and activation effects of water would have to be evaluated. To reduce the release of possible impurities to the plasma from the stainless steel tube surface, a low Z material coating of the tube outer surface, such as beryllium, may be required.

The vessel first wall cooling system will be separated from the blanket and shield cooling systems because of the pressure difference. The vessel wall cooling system would consist of a closed primary loop to isolate the activated water. This loop would contain the necessary pumps, clean-up and filtering equipment including the system for removal of tritium. A buffer loop would connect the primary loop to the forced draft heat cooling towers, if cooling towers are used.

SHIELD COOLING CONSIDERATIONS

For the shield cooling a low pressure water system can be used. It is assumed that the shield design consists of stainless steel balls which are cooled by borated water in one flow pass with a void fraction of 0.35. Based on the heat depositions in each shield region given by the neutronic calculation (subsection 3.1.7) and assuming a steady state condition (most conservative assumption for long duty cycle operations) the required water flow rate and pumping power requirements were estimated. The results are shown in Table 3-31. The total water flow rate is about 5×10^6 kg/hr and the total pumping power is about 0.43 MW.

Similar to the vessel first wall cooling system, a primary loop and a buffer loop would be needed before the heat is dissipated to the cooling system.

TABLE 3-31

SUMMARY OF SHIELD COOLING SYSTEM PARAMETERS

	<u>Inner Shield</u>	<u>Outer Shield</u>	<u>Supplementary Shield</u>
Heat deposition, MW	276.4	55.8	80.4
Water flow rate, kg/hr	3.35×10^6	0.68×10^6	0.98×10^6
Inlet pressure, MPa	0.7	0.7	0.7
Inlet temperature, °C	74	74	74
Outlet temperature, °C	145	145	145
Pressure drop, MPa	0.138	0.009	0.037
Pumping power (@ 50% Eff.), MW	0.38	0.01	0.04

3.2.6 REMOTE MAINTENANCE SYSTEMS

The basic concept for assembly and servicing of the reactor can be divided into three areas of considerations: (1) initial assembly; (2) reactor servicing; and (3) blanket refueling. The initial assembly will be a hands-on operation while the reactor servicing and blanket refueling operations will require remote handling of the components. Processing and storage of the hot fuel in the blankets will be done in water pits adjacent and integral with the reactor compartment of the reactor building. Since the initial assembly will be a hands-on operation, further discussion of implementing this phase will not be covered; however, facilities needed for initial construction or assembly of components will be identified.

It should be noted that the design of remote handling and maintenance equipment must be a closely integrated part of the tokamak reactor design and construction to assure that proper handling and servicing of the reactor component can be executed with a maximum degree of reliability and simplicity.

To provide specifications for remote servicing areas, facilities and remote handling equipment needs, the general serviceability concepts for the major components in the reactor are as follows:

- Lower Support Structure - 16 structures (one under each TF coil) assembled with lower PF coils, coolant headers, and TF coil support cradles with frames are attached to the floor.

Facilities - The facilities must be designed such that the lower, outer PF coils can be assembled and located on the floor for later positioning to the TF coil structure.

Remote Servicing - Remotely operated jacks or a multipoint lift spider attached to an overhead crane is required to raise or lower the PF coils. A vehicle-shielded cab manipulator is required to remotely attach the lift spider or jacks to the PF coils and to perform the mechanical operations of removing or installing the electrical leads, coolant lines, and structural

connections which fix the PF coils to the TF coil structures. The PF coils mounted in the support structure are lowered into slots and a hold-down plate bolted over them. Standard remote tools should suffice for those operations.

- Inner Bore OH Coils - Assemble lower OH coils with electrical and coolant connection systems to the lower support structure. Connect coolant and cable systems to appropriate systems located at the subfloor.

Facilities - OH coils are installed inside the bucking cylinder and will not directly affect the facilities except for connections to the subfloor level.

Remote Servicing - No remote handling or servicing equipment will be required as these coils will not be activated or accessible for servicing or maintenance unless a total disassembly of the reactor is initiated.

- Toroidal Field Coils - Bobbin machining, cable winding, coil assembly, and dewar attachment may be accomplished at the plant site. Install the 16 TF coils onto support structures. Connect lower coolant/cable systems and attach coils to support frames. Install upper TF coil support frames.

Facilities - A manufacturing and coil winding area may be required on site. Equipment for testing the coil electrical system, cooling system, and dewar integrity will also be required. Overhead cranes and rail-wheeled transporters will be required to transport the TF coil assemblies from the manufacturing area to the reactor site. Because of the hourglass-shaped bucking cylinder, the TF coils must be moved radially and cannot be raised or lowered in the process.* Rail-wheeled transporters must have the capabilities of moving and positioning these coils onto the support structures around the reactor. A rail system will have to be integrated into the structural floor of the reactor building with interconnecting rails to maintenance and storage facilities.

*An alternative would be to design the upper hourglass portion of the bucking cylinder to be removable to permit vertical entry of TF coils.

Remote Handling - No servicing would be planned for the TF coils after initial startup of the reactor; however, backup measures would have to be considered. To do any major repair or service work on the TF coil, a vehicle-shielded cab manipulator which has the capability of remotely removing structural bolts, electrical and coolant systems connections, and provide cutting and welding capabilities at any degree of latitude would be required. The rail-wheeled transporter would have to be equipped with a shielded cab or have the capability of being operated remotely for handling the TF coils at end of life or for any major repairs or replacement that would be performed during scheduled shutdowns.

- Inner Shield and Support System - Comprised of lower support tie, subheaders, inner shield sectors, lower shield vacuum vessel support rail, upper support tie, and upper shield vacuum vessel hanger.

Facilities - No additional facility beyond the reactor building would be required. Once these components are installed, no major maintenance/servicing is planned for these components. Some minor servicing such as rail or vessel hanger adjustments may be necessary.

Remote Handling - Minor servicing and adjustment could require the use of the overhead crane and a vehicle-shielded cab manipulator for making adjustments to header connections and structural supports such as vacuum vessel support rails, hangers, and shield supports.

- Upper Support Structures - The upper PF coils, headers, and TF coils support cradles are mounted to the upper support frame. The completed subassembly is mounted on the TF coils and the inner bore structure. Assembly is completed by connecting the inner shield, TF, and PF coil-cooling systems.

Facilities - The servicing of these components is considered to be minimal and no additional facilities are required.

Remote Handling - Disassembly of these components would be performed by the vehicle-shielded cab manipulator and the overhead crane.

- Vacuum Vessel Assembly - The inner vacuum vessel shell sectors are installed on a rail and hanger support passing between TF coils and welded together. The inner liner plates are installed to the inner walls of the inner vacuum vessel shell. The outer vessel shell sections with preassembled limiters and leak-detection system are installed and seal-welded. Connection of the leak-detection system and cooling system completes the installation of the vacuum vessel.

Facilities - Periodic replacement of the outer shell of the vacuum vessel should be a design capability. This work could be done during a blanket module replacement. A hot cell is required for final processing or repairing of the outer shell.

Remote Handling - Disassembly of the outer shell of the vacuum vessel will require an overhead crane with shielded cab and a vehicle-shielded cab manipulator. The manipulator must have the capability of cutting seal welds, removing structural bolting, and disconnecting of coolant lines and leak-detection systems. In reassembling a repaired or new outer shell, the manipulator must have the capability of seal-welding and implementing the leak-detection system.

- Neutral Beam Ducts - Six neutral beam ducts and liners are attached to the vacuum vessel and shielding is installed from blanket penetration areas outward.

Facilities - Periodic replacement of the neutral beam ducts and liners should be designed for during a major maintenance period. A hot cell is required for handling these components.

Remote Handling - Disassembly and assembly of these components will require the same remote handling equipment needed for the vacuum vessel.

- Blanket Modules - Straight sections of blankets will be installed near the outer surfaces of the vacuum vessel. Periodic replacement of the blanket sections will require on-line servicing.

Facilities - Periodic refueling (replacement with alternate module sets) will require additional facilities and portable decay heat removal systems. Hot cells will be required for service on the blanket fuel elements and water pits will be required for storage and disassembly of the fuel elements from the blanket modules.

Remote Handling - In addition to the overhead crane and vehicle-shielded cab manipulator needed for removing the blanket modules from the reactor, additional shielded cranes and manipulators will be required in the hot cell and water pits.

- Outer Shield Sectors - The outer shield sectors are installed under TF coils and between adjacent TF coils. These shields require coolant piping connections to coolant headers.

Facilities - Since a number of outer shield sectors must be removed for blanket replacement and vacuum vessel servicing, a temporary storage facility is required.

Remote Handling - Handling of the outer shield sectors can be done with the overhead crane. The vehicle-shielded cab manipulator will be used to disconnect the coolant piping. In addition, transporters for moving the outer shields from the reactor room to a location in the hot cell or water pit will be required.

- Outer Poloidal Field Coils - The PF coils located below the horizontal centerline will be raised and attached to the TF coil dewars; those above the centerline will be lowered and attached to the TF coil dewars. Completion of connections of the cryogenic systems and power cables to power supply lines will be required.

Facilities - The PF coils will require raising or lowering for temporary storage during shield, blanket, or vacuum vessel servicing. The PF coils will be stored on the floor or above the reactor during a servicing operation.

Remote Handling - Raising and lowering of the PF coils can be accomplished by the overhead crane equipped with a multipoint lift spider to support the coils evenly over the circumference

of the coil. The vehicle-shielded cab manipulator will be needed for disassembly and reassembly of the electrical systems, the coolant piping systems, and the structural bolting arrangements.

- Neutral Beam Injection System - Six neutral beam injection systems will be required. These systems will have to be removed for vacuum vessel servicing. The neutral beam injection systems will require servicing of the cryopanel, liners, and ion sources.

Facilities - Servicing and testing of the neutral beam injection systems will require a separate room where the pumping systems (cryopanel and mechanical pumps) can be tested and necessary servicing performed. The ion sources will require adjustment of the electrical supply and proper mechanical positioning to assure maximum efficiency. The neutral beam vessel will require vacuum testing to locate and repair leaks. To accomplish this, the service room will have to be equipped with high voltage power supplies and vacuum systems.

Remote Handling - To service the neutral beam injection system, the vehicle-shielded cab manipulator will be required to disengage the neutral beam injection system from the neutral beam ducts and disconnect the electrical and helium coolant piping systems. Transporting of the neutral beams from the reactor room to the designated servicing facility can be performed by the overhead crane and rail-wheeled transport carts. It may be advantageous to build small hot cells with access to the injectors for removal and replacement of the short-lived grid (ion source) systems.

As noted from the general serviceability concepts outlined above, servicing and/or replacement of the vacuum vessel wall, limiters, and neutral beam liners will be coordinated with the blanket module replacements. This will necessitate the need for spare replacement components of these items with nearby storage and maintenance facilities. The periodic blanket replacement with alternate module sets may not require repeated removal and installation of the neutral beam injection systems, but will require removal of some of

the outer shield sectors and associated parts. Because of the repetition of remote handling work in this area, the blanket refueling cycle will be of keen interest to plant maintenance and servicing and will require remote handling tools of extreme reliability, repeatability, and simplicity comparable to mass production and assembly line techniques.

3.2.7 RADWASTE HANDLING SYSTEM

The primary function of the Radwaste System and its associated equipment is to serve as a controlled area collection station for receiving and handling contaminated and activated materials generated by DTHR operations, and arranging for their shorter range storage and ultimate disposal.

It is anticipated that the array of hardware items that will be provided for conducting the radioactive waste handling operations will, for the most part, be state-of-the-art equipment.

Five broad categories of operations will be provided for: short term and long range holdup of contaminated liquid; handling, storing, and packaging of contaminated solids; processing to decontaminate radioactivity bearing liquids; solidification and packaging of highly contaminated liquids for off-site disposal; collecting radioactivity bearing gases followed by removing the radioactivity or preparing them for ultimate disposal. General characteristics, some relevant specifics regarding these operations, and listings of the required hardware to conduct these operations are presented in summary form in this subsection.

The amount of radioactive liquid and gaseous waste generated by the DTHR operation is expected to be relatively small. No liquids are used directly in the fusion process or come in direct contact with tritium or the thorium-uranium in the fissile fuel breeding material during normal operation of DTHR.

The current preliminary design concept utilizes high purity (demineralized) moderate temperature water to cool the vacuum vessel and the fissile fuel breeding modules; but in these applications the water is contained in primary closed loops, with secondary coolant loops serving to dissipate waste heat to the environment. This same situation applies for the demineralized water used to enhance attenuation of the neutron-gamma field and abstract the heat deposited in the steel-boron carbide shield materials.

In general, closed loops are used for cooling systems where contamination potential is present. The closed loop demineralized water streams that cool the vacuum vessel, shielding and fuel breeding modules are maintained at high purity by continuous demineralization of auxiliary closed loop side-streams, equivalent to turn-over of about 50% of the primary loop contents per day. It is anticipated that the concentration of the neutron activated corrosion products (mainly Co-60) in the loop streams will be no more than 1×10^{-6} $\mu\text{Ci/ml}$ or ~ 0.02 of maximum permissible level for discharge to off-site unrestricted areas. Coolers used in the tritium gas prefill supply system are small closed loop water systems in which the heat is removed from the device by package type refrigeration units with room air cooling of the condensers.

Cooling of the tritium storage-generator units and the uranium traps is accomplished by closed loops that utilize helium (or argon) as the coolant, which in turn dissipate the heat to secondary closed loop package cold-exchange units whose refrigeration condensers are serviced by the facility general purpose cooling water system. Cleanup of the inert gases of the loops via side-stream detritiation limits the transport of tritium into the circulating water of the secondary heat exchange water system.

The facility is to be provided with a general purpose low level radioactive waste system which collects all potentially contaminated liquids from the drains of the reactor cell, hot cell, and service areas devoted to servicing-maintaining the DTHR device, as shown in the schematic (Figure 3-37). The three tanks are adequate to hold the entire contents of the coolant loops for the vacuum vessel, fissile fuel breeding modules and shield modules in the event of accidental piping ruptures.

Figure 3-37. General Purpose Low Level Liquid Waste System

The liquids collected in the holdup tanks periodically will be sampled to verify that the activity is acceptable, and then discharged. If necessary, additional treatment or holdup to permit activity decay will be employed to ensure that the release levels are as low as practicable, with a target of no higher than 10% of current permissible levels defined in ERDA 0524.

Small amounts of liquid may be generated during the servicing and maintenance of equipment that is neutron activated or comes in contact with tritium. Such liquids may be organic as well as aqueous. The organics will be collected separately and if the activity of the aqueous liquids is unacceptably high to be routed to the low level waste system, this material will be dispatched to the moderate level collection tanks. Subsequently, these liquids will be solidified (concreted) or collected on an absorbent and packaged as solid material for off-site disposal.

Radioactive solid waste materials will arise from a number of sources, such as: (1) spent deionization resins; (2) debris, grindings, scrap, hardware segments generated during maintenance operations on contaminated or activated equipment; (3) spent desiccant and molecular sieve materials; (4) particulates trapped by the micropore process filters and ventilation exhaust HEPA filters; (5) spent uranium removed from the hot uranium traps; (6) miscellaneous trash. All of this material will be collected, classified as to level and type of activity, and packaged in containers suitable for off-site shipment to ultimate disposal.

Specifics regarding the handling of contaminated materials by the Radioactive Wastes Treatment System and the principal hardware to be provided are summarized in the following listings.

CONTAMINATED LIQUID HOLDUP AND LONG RANGE STORAGE

- 3 equipment cleardown, low level activity water tanks, each with a capacity of 10m^3 (~ 2600 gal).

- 2 reactor cell air conditioning condensate tanks, each having a capacity sufficient to hold up the condensate generated by the dehumidifiers of the reactor cell air treating package over a two day operating period under the worst ambient conditions, corresponding to 3.5m^3 (~ 900 gal) each.
- 3 general purpose, low level aqueous waste tanks, each having a capacity of 60m^3 ($\sim 16,000$ gal).
- 4 moderate reactivity level ion-exchange effluent tanks for holdup of liquids resulting from regeneration of ion exchangers of the primary closed loop high purity cooling water subsystems, each having a capacity of 2m^3 (~ 500 gal).
- 2 tanks for organic wastes, each with a capacity of 1m^3 (~ 250 gal).
- 3 large storage tanks for the primary loop coolants for the vacuum vessel ($290\text{m}^3 \sim 77,000$ gallon), fissile fuel blanket ($380\text{m}^3 \sim 100,000$ gallon) and shield ($160\text{m}^3 \sim 42,000$ gallon). These volumes are based on the equivalent of a once-through pass with a 30 K temperature rise (for energy of 27.5 MeV per fusion of which 3.5 are due to alphas, 17.5 are from fusion neutrons and 6.5 are due to fissions in the blanket, with 35% going into the vacuum vessel, 19% in the shield, and 46% in the blanket), a reference design plasma volume of 236.5m^3 , a 70 s plasma burn duration that results in 950 MW of fusion power (2.36×10^{22} fusions per pulse). These tanks serve as temporary storage of the coolants when the device is drained for maintenance or emergency reasons. Additionally, pumps, lines and valving are required to restore the fluids to the coolant loops.
- A large tank or lagoon is required to hold-up or long term store the buffer water of the heat dissipation system and the aforementioned primary coolants, all of which may become contaminated in the event of leak developments. Based on buffer volumes double those for that of the primary coolants, and allowing for an 100% overage, the lagoon capacity is set at 5100m^3 ($\sim 1,400,000$ gallon). It is anticipated that the storage reservoir will be an open lagoon (concrete walls

coated with bitumastic, 0.3m thick concrete walls, 2m deep liquid, 1m extra height length: width ratio $\approx$ 3:1.

- Some of the tanks will be outfitted with mixers, agitators, monitors and samplers. These are required to insure that the sampling is really representative of the vessel contents being monitored and that particulates are removed prior to dumping.

TREATMENT OF CONTAMINATED SOLIDS

As indicated previously, a variety of contaminated solids will be generated by maintenance and routine operations, with the radioactivity resulting from neutron activation, or diffused-in or surface held tritium. The miscellaneous trash items alluded to include such things as vacuum cleaner cruds, contaminated clothing and cleaning materials, contaminated pipe and fittings, contaminated tools, manipulator gauntlets, etc.. Breaking these down to manageable sizes for packaging for burial disposal will require an array of equipment as listed below:

- Packaging station comprising segmentors, compactors, scales, lifts, sealers, balers, hoists, vacuum cleaners, supply of containers.
- Monitors and survey meters.
- Calorimeter tritium assay setup.
- Multichannel gamma ray spectrometer and accessories.
- Large shielded shipping casks.
- Lift truck, crane.
- Portable shielding.
- Accountability station equipment.
- Health physics and safety equipment.

LIQUID DECONTAMINATION PROCESSING EQUIPMENT

Conduct of operation for decontaminating liquids and reducing their volumes will require the following array of equipment.

- Filters and centrifuges.
- Precipitators, flocculators, mixers.
- Ion-exchange columns and accessories.
- Associated pumps, piping, valves, tanks, instruments.
- Evaporator-thickener.

HIGH-MODERATE-LEVEL LIQUID SOLIDIFICATION AND PACKAGING

In connection with solidification of higher level liquid wastes and packaging them for disposal the kinds of requisite equipment and facilities are:

- Assay setup.
- Solidification station, involving concreting, drumming, overpacking.
- Handling equipment for transport, loading.

3.3 ELECTRICAL POWER AND ENERGY STORAGE SYSTEMS

The electrical systems for the DTHR draw power from two commercial 500-kV transmission lines, transform to lower voltages for distribution and utilization at the loads, and store and convert the energy to meet the DTHR operational requirements. The electrical facilities are divided into the systems illustrated in Figure 3-38, which provide the following functions:

- The Transformer Yard and Distribution System changes the incoming 500-kV power to 220-kV, 69-kV, 13.8-kV, and 4.16-kV, and distributes the power to the various DTHR loads.
- The Toroidal Field Coil Supply and Distribution System takes ac power from the 69-kV bus in the transformer yard and converts it to controlled dc current for the superconducting toroidal coils.
- The Poloidal Field Coil Supply and Distribution System takes ac power from 69-kV bus in the transformer yard through rectifier-inverter sets that restore the losses of the four generator-flywheel sets by running them as motors to restore the initial speed over the duration of a reactor cycle.
- The Divertor Field Coil Supply and Distribution System takes ac power from the 69-kV bus in the transformer yard and supplies dc current to the divertor field coils through a rectifier.
- The Neutral Beam Power Supply and Distribution System takes ac power from the 220-kV bus in the transformer yard and supplies the neutral beam lines and distribution system. It also supplies the load smoothing resistor assemblies that minimize the neutral beam load swings on the 500 kV electric utility power system.
- The Control Systems also receive power from the transformer yard 69-kV bus through unit substations that provide ac power to the various control systems required at the site.
- The Class IE Electrical Systems provide redundant electrical supplies to safety related loads in the plant. These systems normally operate from the main 500-kV bus, and provide power at 4.16-kV for large motors, and 480/240/120 V for other ac loads. Dc loads are supplied at 125 V through rectifiers. If the 500-kV bus is lost, deisel generators and batteries supply the critical loads.
- The Energy Storage System is part of the Poloidal Field Coil Supply and Distribution System supplied from the 69-kV bus in the transformer yard, and requires special design consideration to provide the large fluctuating load requirements of the poloidal fields, and thus minimize load and voltage fluctuations on the electric utility supply system.

3.3.1 TRANSFORMER YARD/DISTRIBUTION SYSTEM

Electrical power and energy requirements for operating the DTHR facility are obtained from the local electric utility system through the transformer yard and distribution system as shown in Figure 3-39. The transformer yard takes the electric power from the utility system at an extra-high-voltage (EHV) of 500 kV and transforms it to appropriate lower voltages and power levels which are supplied to the various loads of the DTHR through the distribution systems.

DESIGN CONSIDERATIONS

The electrical power requirements of the DTHR must be supplied from a large electric utility system at an extra-high-voltage (EHV) level of 500 kV. The Transformer Yard/Distribution System (TYDS) receives the power at this EHV level and transforms it to suitable lower voltage levels for distribution to the various loads of the DTHR. Since the major loads of neutral beam lines and the poloidal fields (OH and SF) are pulsed loads, load smoothing resistor banks for the neutral beam lines and energy storage and release systems for the OH and SF poloidal coils are required to minimize the load swings and voltage fluctuations on the electric utility system. The TYDS must also be designed to insure the safety of personnel, protection of the equipment from off-normal conditions, and provide a continuity of service for essential loads when loss-of-power from the electric utility system occurs.

A large generating-capacity electric utility system with an EHV transmission system of 500 kV is required to supply the large power requirements of the DTHR. A concentrated load of 2000 MW, such as the DTHR, is a large load compared with the normal loads of most electric utility systems. Only a few systems have 20,000 MW to 30,000 MW generating capacity, and the DTHR load approaches 10% of the capacity of these systems. Such a load cannot be pulsing on such a supply system, and the rate of load pickup and dropping must be controlled to occur over 5 to 10 minutes. In addition, since 20,000 MW generating capacity systems generally have their generating stations distributed over a large area (one or two states), EHV transmission lines of 500 kV or higher are required for interconnecting the

generating stations and for supplying the major load centers of the electric utility service area. Adding a DTHR 2000 MW concentrated load to such systems requires power delivery at 500 kV to distribute the load over most of the generating capacity of the system. Ties to adjacent systems will also supply some of the power requirements.

Some estimate of electric utility power transmission capabilities may be obtained by examining Table 3-32, which shows the MW that may be transmitted by single EHV transmission lines (considering stability) for distances of 100 miles, 200 miles, 300 miles. A DTHR load $\approx$ 2000 MW requires a minimum of one 500-kV line less than 100 miles long. Two 500-kV lines are recommended, so faults on one line will not cause loss of total power to the DTHR loads and permit maintenance of 500-kV line terminal equipment. Two lines are necessary to support the Class 1E system.

To limit voltage fluctuations on the 500-kV transmission system to about 1% for fluctuating loads such as presented by the DTHR (1500 MW total for neutral beams at 95% power factor) requires connecting the DTHR load to a 500 kV bus with a short circuit capacity of about 52,000 MVA. Not only are such 500 kV buses unavailable, but present 500 kV circuit breaker interrupting capacity is limited to 35,000 MVA. If the neutral beam load power factor were to be corrected from 0.95 p.f. to 0.988 p.f. (requiring a 270 MVAR shunt capacitor bank), the neutral beams could be switched on and off a 500 kV system with a short circuit capacity of 25,000 MVA. Even 500 kV buses capable of delivering this short circuit MVA are limited to a very few in the USA. Discussions with TVA systems engineers last year revealed that they (and other large utilities) are finding evidence of incipient fatigue failure in turbine-generator shafts due to normal transient loadings. The NB load for DTHR, if switched on and off once per minute, would far exceed the present frequency of transients. The TVA engineers stated that they could not accept large loads of this type. Therefore, a load-smoothing resistor bank with continuous rating approaching 1500 MW is necessary to maintain load and prevent voltage fluctuations on a typical 500 kV system when the neutral beams are switched off for 15 seconds during the DTHR load cycle. The resistor bank must also be switched on and off in increments of about 100 MW to

TABLE 3-32

ELECTRIC UTILITY EHV TRANSMISSION LINE CAPABILITIES

<u>Operating Voltage in Kilovolts</u>	<u>Single-Line Power 100 Miles</u>	<u>Transmission 200 Miles</u>	<u>Capability in MW* 300 Miles</u>
345	840	590	420
500	1750	1225	875
765	4100	2870	2050

*Transmission line loading based on system stability considerations.

150 MW when the DTHR is first started up and later when it is shut down, to minimize load swings and voltage fluctuations on typical 500 kV electric utility systems.

While the total power and energy requirements of the DTHR are supplied from an electric utility power system at the 500-kV level, the power is stepped down in voltage through transformer banks to voltages of 220 kV, 69 kV, 13.8 kV and 4.16 kV that are suitable for power distribution to the DTHR loads. Using this distribution system minimizes the voltage fluctuations that one type of load may impose on another load to that which occurs in the common 500 kV system source reactance. The selection of the voltage level for each type of load is based on available circuit breaker current ratings as shown in Table 3-33. The thermal load-carrying capability of the circuit breakers is expressed in MVA at rated voltage and current. Nominal interrupting capacity in MVA is also shown for guidance in selecting the voltages suitable for power distribution to each type of load.

The 220-kV voltage level was selected to supply the neutral beam rectifiers directly, possibly without the need for an interposing rectifier transformer. A 220-kV circuit breaker has adequate load-carrying capability, as illustrated in Table 3-33, if two circuits are used, and voltage regulation will be nominal for the short transmission distances to the neutral beam loads.

For power supply to the PF energy storage systems, the TF and bundle divertor fields, and auxiliary unit substations in the DTHR, a 69-kV voltage level was selected for each of the 150 MVA transformers supplying these loads. The 69-kV circuit breakers have adequate load-carrying capability and voltage regulation also is nominal for the short transmission distances to these loads. The poloidal fields energy storage systems obtain variable frequency power from the 69-kV buses through inverters, and use the power at a 15-kV voltage level. The auxiliary system unit substations also take power from the 69-kV buses and transform it to 4.16 kV for supplying auxiliary loads at various centers of the DTHR.

TABLE 3-33

CIRCUIT BREAKER LOAD-CARRYING CAPABILITY
(BASED ON RATINGS GIVEN IN ANSI C37.07-1971)

<u>VOLTAGE RATING IN KILOVOLTS</u>	<u>CONTINUOUS CURRENT RATING IN AMPERES</u> (Indoor Oilless)	<u>LOAD CARRYING CAPABILITY IN MVA</u>	<u>INTERRUPTING CAPACITY MVA (NOMINAL)</u>
4.16	3000	21.6	350
7.2	2000	25.0	500
13.8	3000	72.0	1000
(Outdoor Oil)			
14.4	1200	30.0	500
23.0	1200	47.8	500
34.5	1200	71.7	1500
46.0	1200	95.6	1500
69.0	1200	143.4	2500
(Outdoor Oilless)			
34.5	1200	71.7	1500
69.0	2000	239.0	5000
(Outdoor Circuit Breakers)			
115.0	3000	598.0	12,500*
138.0	3000	717.0	19,121*
161.0	2000	558.0	13,943*
230.0	3000	1195.0	25,000*
345.0	3000	1792.0	23,900*
500.0	3000	2600.0	34,640*
700.0	3000	3975.0	48,500*

*For guidance only; nominal interrupting capacity not applicable.

For critical class 1E loads and others needing an uninterruptible power supply, power is transformed in duplicate systems from the 500-kV buses to 4.16-kV to supply these loads. The 4.16 kV buses and critical loads also are backed up by uninterruptible power supplies (UPS) and complementary diesel-generator sets that maintain a reliable power supply in the event of complete loss-of-power from the 500-kV ac electric utility power system.

3.3.2 TF FIELD COIL SUPPLY AND DISTRIBUTION

The TF electrical system will provide the power for charging the TF coils to the required current. Since superconductors are used the coil can be charged over a prolonged period, thus reducing the size of the supply. The electrical stored energy of the coil is high enough to require a coil dumping system to minimize damage for various failures. Other electrical design considerations are system isolation, coil protection, and equipment failure protection.

THE POWER CONVERSION SYSTEM

The power conversion system will be a rectifier-transformer which will draw power from the external 60 hertz supply and provide dc current for the TF coil. The transformer will provide isolation of the TF coil circuit and step-down the voltage. A breaker on the input will be for normal switching and also to interrupt the ac supply for various faults. The transformer will require RC networks to absorb its excitation energy during an ac interruption in order to prevent destructive voltage surges. The rectifier will be a three-phase thyristor bridge which will control the normal charging and discharging of the TF coil. The normal charge-discharge time will be about a half an hour and is too long to remove the coil energy under fault conditions. Consequently, a coil dump system will be required. During normal operation, the TF coil operates in steady state. Figure 3-40 shows the system schematic.

RATINGS:

TF Coil

180 H

16 kA

22.5 GJ

DC Power Supply Output

- Total of 30 MW for 30 minute coil charging time
- Two 16 kA supplies at 1 kV
- One supply for each set of eight coil segments

Figure 3-40. Schematic for Toroidal Field Power Conversion System

DUMPING SYSTEM

The dumping system has a resistor in parallel with the TF coil and an interrupting switch which forces the coil current into the resistor. Normally the resistor current is insignificant. During dumping the coil stored energy will be dissipated as heat in the resistor. The resistance is sized to discharge the coil current rapidly and yet stay within acceptable voltage limits. This peak dumping voltage will probably be the main consideration in determining the peak voltage stress in the coil. It can be reduced if the dumping time is increased. A trade-off will be necessary here.

The interrupting switch forces the coil current to transfer from the power supply to the dump resistor. Normal switches do not have the capabilities required for this application. A commutating circuit aids the switch during the current interruption. Once open the switch must be locked out because its reclosing would apply destructive voltages to the rectifier.

DISCONNECT SWITCHES

Good safety practice dictates that the TF coil be completely disconnected from the electrical system for inspections and/or maintenance. This is accomplished with the disconnect switches. With these switches open the TF power supply can be energized and/or tested while personnel are around the grounded de-energized TF coil.

3.3.3 POLOIDAL FIELD COIL SUPPLY AND DISTRIBUTION

The poloidal electrical system will provide the power for controlling the SF and OH coil currents. Since the peak powers are not an acceptable utility load, a set of poloidal energy storage generators will provide the peak power requirements. Coil energy will be returned to the generators whenever a coil is discharged. A transformer-rectifier with a variable frequency inverter will be used to start the generators and supply the system losses.

TRANSFORMER-RECTIFIER WITH A VARIABLE FREQUENCY INVERTER

This subsystem basically supplies the losses of the poloidal system and will be controlled to normally have a relatively constant power output. The inverter frequency will be matched to that of the energy storage generators on its output. This subsystem will also have the capability of starting the generators and bring them up to speed. The peak power rating of the inverter is considerably less than that of either the energy storage generators, the shaping field power conversion system, or the OH field power conversion system. However, an inverter output breaker will be required that is sized for the generator short circuit current. Figure 3-41 describes the poloidal energy storage system.

THE POLOIDAL ENERGY STORAGE GENERATORS

The generators will supply the peak SF and OH loads. The power of these loads is both positive and negative so that the generators will slow down and speed-up during each cycle. Each generator will have a breaker for protection against a short on the output. The generators will be protected against internal shorts by killing their fields. Figure 3-42 presents a plot of the power, energy, and volt-amperes that the generator supplies to the poloidal system. Four machines similar to the Grand Coulee 615 MVA generators will be used.

THE SF POWER CONVERSION SYSTEM

A transformer-rectifier-supply will be connected to the energy storage generators. The transformer isolates the two subsystems and prevents destructive interaction of voltages during ground faults. The rectifier controls the flow of power from the generators during plasma initiation. The rectifier becomes an inverter during shutdown thereby returning the coil energy to the generator. A feeder breaker on the transformer input will be sized to disconnect the SF subsystem for various faults. Figure 3-43 shows the schematic of the

Figure 3-41. Poloidal Field Coil Energy Storage System Arrangement

Figure 3-42. Poloidal Energy Storage Generator Requirements

Figure 3-43. Schematic for the Shaping Field Power Conversion System

SF power conversion system and Figure 3-44 is a plot of the power and volt-ampere input requirements of the system. A start-up capacitor will supply the peak SF power required during plasma initiation. An array of bypass diodes will carry the SF coil current once the capacitor discharges. A dump resistor and interrupting switch will be required for the superconducting SF coil. Its purpose and operation will be the same as that of the TF coil.

THE OH POWER CONVERSION SYSTEM

A transformer-dual-rectifier-supply will be connected to the energy storage generators. The transformer isolates the two subsystems and prevents destructive interaction of the voltages during a ground fault. The dual rectifier controls the flow of power from the generators. It also controls the polarity of the current in the OH coil. The rectifiers become inverters when the coil energy is being fed back into the generators. Figure 3-44 gives the plot of the power and volt-ampere input requirements of the system and Figure 3-45 is the schematic of the OH power conversion system.

One resistor will serve for plasma initiation and dumping. Ignitron switches with commutating circuits will be used for the associated interruptions. One for each direction will be required because of the OH current reversal. The dumping is similar to that of the TF system, but the dump resistor cannot be permanently connected across the coil. A minor difference is that the rectifier thyristors (or bypass thyristors) will carry the decaying coil current since these thyristors must be in series with the dump circuit.

DISCONNECT SWITCHES

Good safety practice dictates the use of disconnect switches as a means of preventing the accidental endangering of personnel by energizing a circuit. The disconnect switches will be opened before inspections and/or maintenance of equipment. This eliminates the possibility of equipment being remotely energized while personnel are around it.

There will be disconnect switches for the OH and SF coils. With these open, personnel can be at the coils while the power conversion systems are energized for any kind of tests.

b

Figure 3-44 PF Coil Power and Voltage Input Requirements

Figure 3-45. Schematic for OH Power Conversion System

There will be disconnect switches on the inputs to the OH and SF power conversion systems. These will be opened for the inspection and/or maintenance of the equipment.

3.3.4 DIVERTOR FIELD COIL SUPPLY AND DISTRIBUTION

The divertor electrical system will provide the power for charging the coil to the required current. Since superconductors are used, the coil can be charged over a prolonged period. The electrical stored energy of the coil is high enough to require a coil dumping system to minimize damage for various failures. Other electrical design considerations are system isolation, coil protection, and equipment failure protection.

THE POWER CONVERSION SYSTEM

The power conversion system will be a rectifier-transformer which will draw power from the 60 hertz supply and provide dc current for the diverter coil. A breaker on the ac input will be provided for normal switching and to interrupt the ac supply for various faults. The transformer will step down the voltage and isolate the divertor coil from other systems. The transformer will require RC networks to absorb its excitation energy during ac switching, thereby preventing destructive voltage surges. The required dc voltage is unusually low so that the power supply losses will be large with respect to the load. Circuit No. 43 of "United States Standards and Requirements for Rectifier Circuit" was selected because it has lower losses when the output voltage is low. The rectifier will control the normal charging and discharging of the coil. The normal charge/discharge time of the coil, ~30 minutes, will be too long to remove the coil energy under fault conditions. Consequently, a coil dump system will be required. Figure 3-46 presents the system schematic.

RATINGS:

Divertor Coil

0.126 H

32 kA

DC Power Supply Output (with 10 volt bus drop to coil)

500 kW

32 kA at 15 V dc

Figure 3-46. Schematic for Divertor Field Coil Electrical System

DIVERTOR COIL PROTECTION REQUIREMENTS

A divertor coil cooling system failure, electrical grounds, loss of ac input, etc., are types of failures which will require dumping, i.e., the fast discharging of the coil current. This is done to prevent damage such as could occur if the coil went normal after loss of cooling. Without dumping there would be a coil-damaging temperature rise.

DUMPING SYSTEM

The dumping system has a resistor in parallel with the divertor coil and an interrupting switch which forces the coil current into the resistor. The resistor is in the circuit continuously but its normal current is insignificant. During dumping the coil stored energy will be dissipated as heat in the resistor. The resistance is sized to discharge the coil current rapidly and yet stay within acceptable voltage limits.

Normal switches do not have the interrupting capabilities required for this application. Therefore, a switch and a commutating circuit will be required. The commutating circuit aids the switch during the current interruption. Once open the switch must be locked out because its reclosing would apply destructive voltages to the rectifier.

DISCONNECT SWITCHES

Good safety practice dictates that the divertor coil be completely disconnected from the electrical system for inspections and/or maintenance. This is accomplished with the disconnect switches. With these switches open the divertor coil power supply can be energized and/or tested while personnel are around the grounded de-energized coil.

3.3.5 NEUTRAL BEAM POWER SUPPLY AND DISTRIBUTION

The principle features of the neutral beam power conditioning system are the high total power requirements and the necessity for steady-state capability, coupled to the ability to protect the utility line from large power transients during each machine cycle. Advanced neutral beam circuit designs, which are already under development, can be scaled to provide the high operating power. A simple ac switch and resistive load dump is selected as the least expensive method of smoothing the utility line load.

The estimated steady-state loads on the neutral beam subsystems are shown in Table 3-34. Overall circuit efficiencies of 80% were selected as a design goal. The gradient grid or grids are supplied by a passive voltage divider connected to the accel grid supply. Although there are many significant consumers of electrical power in the DTHR (see subsection 3.3.7), the neutral beam power conversion system, along with its associated line load-smoothing circuit is the dominant source of energy loss, constituting a steady-state drain of 1,547 MW during periods of plant operation.

The most expensive and complex of the neutral beam power conditioning systems is the accel grid power supply circuit. A schematic of the shunt regulator circuit selected for DTHR is shown in Figures 3-47. Although this circuit is less common than series regulator circuits, it is being demonstrated on the Neutral Beam Test Stand Facility (NBSTF) at the Lawrence Livermore Laboratory. A simplified schematic of the NBIS power conversion system is shown in Figure 3-48.

The principal reason for selecting a shunt regulator is that it permits the use of either of the TFTR developmental regulator tetrodes without the need for another tube development program. The non-linear resistors in series and shunting the regulator tube prevent the tube from sustaining more than 70 kV at minimum regulating current and 20 kV at maximum plate current. They are only required to carry the full 150 A for a few hundred microseconds following

TABLE 3-34

STEADY-STATE ELECTRICAL LOADS--NEUTRAL BEAM POWER
CONVERSION SYSTEMS

SYSTEM	NO. OF UNITS	CURRENT	VOLTAGE	PEAK LOAD POWER	PEAK LINE POWER
ACCEL SUPPLY	48	125 A	200 kV	1,200 MW	1,500 MW
ARC SUPPLY	48	7,680 A	30-60 V	22.1 MW	27.6 MW
FILAMENT SUPPLY	48	11,540 A	0-13 V	7.2 MW	9.0 MW
DECEL SUPPLY	48	10 A	20 kV	9.6 MW	12.0 MW

Figure 3-47. Schematic of NBIS Shunt Regulator Circuit

Figure 3-48. Schematic of the Neutral Beam Power Conversion System

Figure 3-49. One-Line Diagram of the Neutral Beam Load-Smoothing System.

Figure 3-50. Schematic of Alternate Neutral Beam Supply and Load-Smoothing System

conditioning faults. Since zinc-oxide varistors are expected to be qualified by increased use in lightning arrestors and by the TFTR ohmic heating experimental program within the next few years, they are tentatively selected over silicon-carbide thyrite non-linear resistors. Zinc oxide is characterized by higher nonlinearity and a lower temperature coefficient than silicon carbide.

The electrical loads on major components in the neutral beam accel grid supply circuit are shown in Table 3-35. This table shows the feasibility of using this circuit under all operating conditions and can also be used as a basis for costing of component systems.

During a reference pulse period, the neutral beam sources are on for 68 seconds and off for 17 seconds. However, design is also predicated on the realization that physics uncertainties may constrain the steady-state time between pulses to be as long as a minute. Based on present-day utility policies, it was also assumed that, during relatively frequent scheduled plant shutdowns, ten minutes would be required to completely remove a 1,500 MW load from a utility line. This implies that any passive energy storage system, such as a motor-generator-flywheel or an external superconducting coil used to smooth the load during the short time between pulses, would require well over 100 GJ of stored energy capacity. Therefore, a simple ac load dump was selected, as shown in Figure 3-49. Each of the 48 neutral beam sources has an ac load dump circuit associated with it, which turns on when the source turns off. The neutral beam system is actually turned on and off during a few 60 Hz cycles, module by module, in order to avoid any large voltage spikes caused by timing inaccuracy. Each ac load dump switch is a three-phase integral half-cycle switch with no phase control. The DTHR reference ac switch and resistor system has been costed at under \$9M, which is considerably less than the cost of any conceivable temporary energy storage system.

An alternative circuit is shown in Figure 3-50. This circuit is almost identical to the reference circuit. The only difference is that the inductance of the current-limiting reactor is increased sufficiently to allow it to store energy between neutral beam pulses and that the shunt regulator can carry steady-state load current. Since the reactor is already an

TABLE 3-35
PRINCIPAL COMPONENT LOADS AT DIFFERENT PERIODS
OF NEUTRAL BEAM OPERATING CYCLE

	NORMAL STEADY-STATE OPERATION			A FEW MICROSECONDS AFTER A FULL-POWER CONDITIONING FAULT			IMMEDIATELY AFTER COMMUTATION OF S1 CROWBAR			AFTER SCHEDULED NEUTRAL BEAM TURN-OFF		
COMPONENT	V	I	t	V	I	t	V	I	t	V	I	t
T2, Regulator Tube	30 kV	15 A	63 s	0 V	0 A	100 ms	50 kV	180 A	100 μ s	0 kV 70 kV	0-150 A -0 A	100 ms 1 s
S2, Series Switch	0 V	150 A	63 s	0 V	0 A	100 ms	200 kV	0 A	100 μ s	0 kV 220 kV	0 A 0 A	100 ms 1 s
S1, Shunt Crowbar Switch	200 kV	0 A	63 s	0 V	165 A	100 ms	200 kV	0 A	100 μ s	0 kV 220 kV	150 A 0 A	100 ms 1 s
L1, Current-Limiting Reactors	0V	165 A	SS	200 kV	165 A	100 ms	20 kV	180 A	100 μ s	220 kV 20 kV- 0 kV	150 A 500 mA	100 ms 1 s

integral part of the neutral beam power supply, this use of inductive energy storage could be much less expensive than previously proposed circuits, which require dual multi-phase convertor sets to transfer energy from the storage inductor to the load.

CONCLUSIONS

- Neutral beam power conversion circuit ratings have been specified for normal operation, conditioning and scheduled shut-off.
- An economical and efficient accel grid power supply has been selected.
- AC switches and dump resistors were selected as the least expensive method of load smoothing.
- An alternative inductive storage system was selected, which should be capable of higher system efficiency than the resistive load dump.

3.3.6 CENTRAL INSTRUMENTATION AND CONTROL SYSTEM

The Central Instrumentation and Control System (CICS) provides central control, timing and monitoring, safety and protection, data handling and display, and radiation and tritium monitors. The CICS provides capability for operation of the DTHR facility from a central control station. The reference system is described in the following paragraphs.

CENTRAL CONTROL AND MONITOR SYSTEM

The Central Control and Monitor System (CCMS) is a digital computer-based system operated from a central control location to provide the control and monitoring functions for the numerous DTHR systems remotely located throughout the DTHR facility. The CCMS is shown in the functional block diagrams on Figures 3-51, 3-52 and 3-53. These diagrams indicate the functional interfaces between the CCMS and the other DTHR systems with physical separations also indicated. Central Control and Monitoring functions include the following:

Figure 3-51. Central Control and Monitor System

Figure 3-52. Central Control and Monitor System for Gas and Tritium Clean-up Systems

Figure 3-53. Central Control and Monitor System for Torus Vacuum, Reactor Area Water Cooling, and Auxiliary Facility Systems

-
- Electrical Power Control and Monitor
 - Neutral Beam Control and Monitor
 - Tritium Gas Control and Monitor
 - Non Tritium Gas Control and Monitor
 - Tritium Cleanup Control and Monitor
 - Torus Vacuum Control and Monitor
 - Blanket Cooling Control and Monitor
 - Reactor Area Water Cooling Control and Monitor
 - Cryogenic Cooling Control and Monitor
 - Auxiliary Facility Services Control and Monitor

Control modes and system operating parameters are selected by the operator(s) at the control consoles in the central control room. The CCMS will operate in an automatic mode to provide the desired control and timing demand signals to each of the DTHR systems as preprogrammed functions of the operator inputs and the measured parameters and status signals. Control and data channels are provided for routing these signals between the central control station and the local stations. A central/local control and monitor interface is located at each of the DTHR system local stations.

TIMING SYSTEM

The high resolution timer shown in Figure 3-54 operates as a peripheral to the minicomputer system and includes special hardware. The timer settings are loaded into the timing system which produces the required timing signals without further intervention once the start signal is given. The timing system provides multiple high resolution timer channels, which produce pulse outputs suitable for synchronization and control of other equipment.

The timing system also provides multiple, lower resolution channels. These channels provide relay outputs suitable for control of other equipment.

The basic timing is obtained from a high frequency oscillator and timing chain. The individual timer channels utilize a content addressable memory to generate a pulse at the time stored in the memory. Although some of the hardware must be special, a large part is available as standard modules.

When a time setting is entered into the system, the value entered may be checked against limit values and any calculations necessary to determine the time setting may be made. Thus, when a time is set with reference to a value other than the reference time, the computer determines the appropriate timer channel setting. The computer then loads the proper content addressable memory and the channel is ready for operation.

The high resolution timer hardware may be expanded in a modular fashion.

PLASMA POSITION CONTROL SYSTEM

Initial operation of the DTHR may utilize programming of OH and SF currents. Provisions will be available to permit the addition of automatic feedback controls when the requirements and methods are developed. Proposed control circuits for both horizontal and vertical plasma position controls are described in the system description.

SAFETY AND PROTECTION SYSTEM

The Safety and Protection System (SPS) will be a hard-wired logic system designed to minimize situations which are hazardous to personnel or equipment. The SPS monitors the conditions which exist and, in the case of undesired conditions, causes the equipment to revert to a safe condition and sounds an alarm.

DATA-HANDLING SYSTEM

The Data Acquisition and Storage System (DAS) is a computer-based system used to collect data during the course of the experiment and store it for later analysis and display. The DAS is comprised of standard minicomputers and their associated peripherals and includes all required interfaces to the main data-reduction computer.

HIGH-SPEED DATA ACQUISITION

A requirement which has been given particular attention is the rapid collection of data. A schematic for a special high-speed data-acquisition channel is shown in Figure 3-55. Information may be loaded into a high-speed disk store. In some cases it may be preferable to use digital-storage oscilloscopes to obtain direct readings of raw data which may later be entered into the data-analysis system.

DATA REDUCTION AND DISPLAY SYSTEM (DRDS)

The DRDS is a medium-scale computer system used to analyze and display plasma experimental data and for storage/display of operating data in a time frame suitable for determining the results of one plasma-pulse operation prior to the next plasma-pulse operation. A typical block diagram for this system is shown in Figure 3-56.

The peripheral equipment supplied with the DRDS computer system is completely software supported by the monitor and provides flexibility for expansion of the system. Each peripheral device is interfaced to function under interrupt control with the priority of each interrupt specified under program control.

The DRDS includes computer display terminals with each terminal mated to a hard-copy unit. Four of these display terminals are used for plasma experimental data, while the remaining two terminals are used for display of important operating data, e.g., neutral beam data, etc.

RADIATION AND TRITIUM MONITORS

The radiation and tritium monitoring system has not yet been defined.

Figure 3-55. High Speed Data Acquisition System

Figure 3-56. Data Reduction and Display System

3.3.7 ENERGY STORAGE SYSTEMS

The DTHR requires a substantial supply of both pulsed and steady-state electrical power. The principal goal of an integrated energy storage system design is to define an economical means of presenting a smooth, unpulsed load to a utility line, under all operating conditions. Each of the energy storage subsystems necessary to attain this goal has been previously discussed. A brief overview of the continuous and pulsed energy requirements is presented in this section.

The peak and continuous electrical power requirements are shown in Table 3-36. No attempt at optimization has been made and the potential for considerable reduction in electrical power requirements is apparent. However, it should be noted that 72% of the energy requirements are associated with the neutral beam supplies, which cannot be substantially lowered without direct energy recovery or a major change in the overall reactor physics objectives. The greatest potential for improvement lies in the poloidal field system. The electrical energy losses associated directly with the poloidal field system and indirectly with its cryogenic refrigeration system are high because of the high field ($\pm 9T$) requirements on the compact OH coil, by the need to provide equilibrium and stability for an elongated plasma with external poloidal field coils, and by the relative frequency ($\sim 2/\text{minute}$) of poloidal field pulses. An 80% pulse duty cycle and 25% plant availability were assumed for this table.

A simplified block diagram of the energy storage and load-smoothing system is shown in Figure 3-57. The neutral beam load is smoothed between pulses on the unbuffered side of the ac energy storage system by the resistive load dump. The poloidal field system line load is smoothed by motor-generator set buffering. All other electrical loads are continuous during periods of plant availability. Refrigeration and cooling loads become negligible during plant down-time.

TABLE 3-36
TABLE OF ELECTRICAL ENERGY FLOW

<u>SUBSYSTEM</u>	<u>PEAK POWER</u>		<u>AVG. POWER/CYCLE</u>		<u>AVG. POWER/YR*</u>	
NEUTRAL BEAM SUPPLIES	1,547	MW	1,237	MW	309	MW
POLOIDAL FIELD SUPPLIES	(4,500)	MW	281	MW	70	MW
LOAD SMOOTHING SYSTEM	1,547	MW	309	MW	77	MW
COOLING SYSTEMS	261	MW	261	MW	65	MW
TRITIUM PROCESSING	3	MW	3	MW	1	MW
MISCELLANEOUS	10	MW	<u>10</u>	<u>MW</u>	<u>10</u>	<u>MW</u>
TOTAL			2,101	MW	532	MW

* 25% OPERATION

FIGURE 3-57. DTHR REFERENCE ENERGY STORAGE AND LOAD SMOOTHING SYSTEM

The poloidal field system has a peak volt-ampere requirement over 5,000 MVA, at the poloidal field buffer generator (Figure 3-42). Assuming that this is reduced drastically during steady-state burn, through the use of buck-boost rectifier-inverters or rectifier dc bypass legs, the generators can be specified for pulsed operation at $\sim 1,250$ MVA apiece. Four Westinghouse 615 MVA, salient-pole generators, as shown in Figure 3-58, could be adapted to this application. Each of these generators has a stored energy at rated speed of ~ 3000 MJ. By running at a modest overspeed of $\sim 16\%$ they can provide an available, deliverable stored energy of $\sim 1,000$ MJ apiece. The total of 4,000 MJ deliverable to the poloidal field coils makes it unnecessary to use an external flywheel for mechanical energy storage. During quiescent periods, the energy storage generators regain mechanical stored energy by being run as motors by a rectifier-inverter system from the ac line. Since total losses during an 85 s pulse cycle in the poloidal field system are estimated to equal 23.9×10^9 J, the rectifier-inverter system is rated at an ac output of 281 MW.

Net power requirements versus time are shown in Figures 3-59 and 3-60 for the cases of steady-state operation, temporary loss of enable, scheduled start-up and shutdown, and unscheduled shutdown. Temporary loss of enable assumes that if all system enables are not set after the 15 seconds between poloidal field pulses, a diagnostic internal self-check is conducted by the operational logic. Fifty seconds are allocated to the self-check. If all enables are not set after the self-check, unscheduled down-time begins. Therefore, there is a maximum time of ~ 60 s between scheduled pulses. The graphs in Figures 3-59 and 3-60 illustrate how easily ac switches can adjust the line loading over time for any mode of operation, without increasing the requirements of any other part of the energy supply system.

DIAMETER OVERALL	72 FT
DIAMETER ROTOR	60 FT
HEIGHT OVERALL	20 FT
SPEED 60 - 70 Hz	72 - 84 RPM
WEIGHT	7.5×10^6 LB
STEADY STATE RATING	615 MVA
PEAK RATING	1230 MVA
ENERGY STORAGE (AVAILABLE)	1000 MJ

Figure 3-58. Energy Storage Generators

Figure 3-60. Energy Storage and Load Smoothing
Circuit-Power Versus Time

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4.0 HYBRID BLANKET AND SYMBIOTIC FUEL CYCLE CONCEPTS

The versatility of the hybrid reactor as a nuclear power plant fuel source or as a fusion power technology base is demonstrated schematically in Figure 4-1. One of these applications was considered in this study; that of providing ^{233}U fuel for light water reactors (LWR's), which form the bulk of the present and certainly near-future U. S. nuclear capacity. This section defines a preliminary conceptual design of the DTHR blanket, provides the performance characteristics of the blanket, defines an initial fuel cycle scenario for the hybrid and PWR reactors, and evaluates the PWR performance with this fuel selection.

An extensive assessment of the application of fission technology to the hybrid reactor blanket was made. Several concepts were evaluated and a prime reference concept using thorium (ThO_2) fuel rods clad in zircaloy based on the LWBR (Shippingport) blanket fuel rods was selected. The module design consists of a simple, rectangular two-zone zirconium structure about 4m long. Even with a modest placement of fertile fuel, a fissile production rate exceeding 200 kg per year at concentrations of ^{233}U greater than 0.2% was achieved. The performance with metallic thorium fuel rods was ~20% higher. The coolant was selected as low temperature and pressure water in line with the low cost, present technology goal of the DTHR. Performance with helium as coolant indicated a potential for 20% improvement in fissile production if the blanket thickness was increased from the 25cm used as the reference case. A simple extrapolation of the DTHR concept to a commercial application was made, which indicated that one hybrid could provide make-up fuel for over 5 present day type PWR's (1000MWe per unit) per year using the chemically separable $\text{Th}-^{233}\text{U}$ fuel cycle. If metallic fuel and helium cooling were used, the hybrid fissile production rate would be sufficient to supply 6 to 8 PWR's (1000MWe) per year, depending on the assumed fuel cycle. It was determined that by replacing the burnable pyrex poison pins with LiF pins in the PWR's supplied by the hybrid, the PWR's (~ 7, 1000MWe) on these fuel cycles could produce as much as 38% of the tritium burned by the hybrid. This combined with the projected 0.77 tritium breeding ratio in the hybrid would result in an adequate

Figure 4-1. Versatility of the Hybrid Reactor

tritium fuel supply. One on-site PWR could probably supply the electrical power required by the hybrid, if the hybrid does not produce its own power.

Preliminary core calculations and safety analyses have been carried out to evaluate the practicability of utilizing thorium fuel enriched with hybrid-bred ^{233}U in a standard pressurized water reactor (PWR) utilizing the oxide fuel form. In general, the results are:

- Highly satisfactory performance is predicted.
- The makeup fissile fuel required is less than for the standard fuel cycle.
- No safety, performance, or capital cost penalties have been identified to date.

These results, although preliminary, are extremely encouraging and indicate the potential of an in-place market for hybrid-produced fissile material. In these calculations, the PWR has not been modified in any way to accommodate the thorium fuel. Significant improvement in the required makeup fissile fuel should be realized with modifications that would be possible in future LWR core designs.

4.1 HYBRID BLANKET CONCEPTUAL DESIGN AND PERFORMANCE ASSESSMENT

Many hybrid blanket design studies in recent years have evaluated fuel systems with the perspective of maximum performance regardless of development needs and proven experience with the respective concept selected. The hybrid blanket design described in this section has been selected with the perspective of maximizing use of the current state-of-the-art technology and fabrication experience. An extensive assessment of the application of fission reactor fuel/clad/coolant technology to the hybrid reactor has been made. Criteria relating to operational reliability, fabrication development requirements, design characteristics and applicability to commercial operations were established. The leading concepts were rate, weighted and ranked. Nominal fuel element dimensions were established and lattice concepts developed. Component stresses and thermal and hydraulic conditions were analyzed. Neutronic analyses of several alternatives were evaluated and projected as to their potential performance in future applications.

4.1.1 HYBRID BLANKET DESIGN OBJECTIVES AND ENVELOPE

The DTHR blanket concept selection was based on the following design objectives:

- Good neutronic performance
- Proliferation resistant fuel form
- Minimal development required
- Maximum operational reliability
- Simple mechanical design
- Low fabrication cost
- Applicability of technology to commercial use
- Minimal remote handling
- Blanket envelope flexibility for alternate concepts

Of these considerations, the neutronic performance, minimal development requirement, simple mechanical design, low fabrication cost, minimal remote handling and blanket envelope flexibility for alternate concepts most affected the envelope selection and are discussed in the following paragraphs. The remaining considerations are discussed in subsequent subsections.

The DTHR blanket concept plan view in Figure 4-2 shows the toroidal coverage of the blanket around the vacuum vessel and plasma. The inner side of the torus was not used for fertile blanket positions. This area could be used for tritium breeding blankets in subsequent testing in DTHR. Initially, only neutron fluence would be measured at the inner shield, which prevents radiation damage to the toroidal field coils. As shown in Figure 4-2, 27 blanket modules are located in groups of 3 or 6 between neutral beam injection (NBI) ducts. The areas above and below the NBI ducts were not used because these partial blankets would represent a unnecessary cost and design complication for DTHR. Thus, a simple design approach has been adopted using 9 of the 16 TF coil openings for blankets, leaving 6 for NBIs and one for the bundle divertor. This concept achieves 56% toroidal coverage, although 4 optional module positions are available near the NBI penetrations. These positions are less accessible because they are completely underneath a TF coil and hard to

Figure 4-2. DTHR Blanket Concept Plan View

reach from an unobstructed TF coil opening. Use of the optional positions would achieve ~65% toroidal coverage. It is estimated that maximum blanket coverage for a 6 NBI, one bundle divertor tokamak would be ~86% of the torus.

Figure 4-3 shows an elevation view of the DTHR blanket concept. This figure shows that a simple, vertical blanket module with a fuel height of ~3.7 m can be installed between the outer shield and vacuum vessel outside envelope. The vertical blanket concept minimizes development of fuel rods and remote handling equipment by maximum use of existing fission reactor technology. Blanket modules can be removed by vertically lifting the module after disconnection from the coolant lines and removal of the shield access plugs. The modules under TF coils will have offset coolant lines and can be rotated from under the coil and lifted at the center blanket module position. The modules can then be moved to a water pit for fuel assembly transfer.

Each module is supported by a hooked arm at the upper support structure and a piloted cylinder at the bottom support structure. The support structures can include reflector surfaces on their plasma side. The blanket concept lends itself to subsequent module designs which are thicker, and could consist of three straight modules, with two of the modules positioned where the present structures are located. Although the poloidal coverage of the simple vertical module concept seems small, it actually intercepts over 40% of the poloidal distribution of neutrons generated in the plasma.

The toroidal geometry of a tokamak results in most of the neutrons passing through the outer half of the vacuum vessel. Figure 4-4 illustrates the leakage of neutrons from an isotropic point source located in the plasma and the wall surfaces they will pass through. Over half of the outer torus wall surface can "see" the source, while only about 25% of the inner wall will "see" the source. Figure 4-5 shows the fraction of fusion neutrons striking the various surfaces in the poloidal direction in the area surrounding the plasma. It can be seen from this figure that ~43% of the available neutrons will strike the DTHR blanket. If the entire outer surface of the plasma were surrounded by blanket, ~67% of the neutrons would be intercepted for fuel production. Only about 25% of the neutrons enter

Figure 4-3 DTOR Blanket Concept Elevation View

Figure 4-4 Neutron Leakage Characteristics at the Plasma Surface

Figure 4-5 Fraction of Fusion Neutrons Striking DTHR Blanket Modules

the inner shield area and ~4% pass through the top and bottom surfaces of the plasma. Table 4-1 shows the fraction of neutrons striking various wall segments around the plasma. The inner surface areas may be better used for breeding tritium in a commercial application and greatly reduce the need for access during blanket refueling operations. The more productive and accessible outer surfaces are available for fissile fuel production. However, this decision depends on the blanket fuel form and its impact on accessibility. The areas above and below the NBI ports and bundle divertor could also be used for tritium breeding in a commercial application.

4.1.2 BLANKET CONCEPT SELECTION

The selection of an optimum blanket concept for DTHR or for a commercial application will require extensive study and evaluation of the numerous alternatives. As a starting point for DTHR, those fuel systems with the greatest development and operating experience in fission reactors were selected. The proliferation resistant fuel objective led to selecting thorium fuel forms. Experience exists with oxide pellets or microspheres, and metal alloy fuels clad in zircaloy or stainless steel. The most experience in fuel/clad form has been with rods. The coolant utilized with these fuel forms has been water in most cases, with some experience using helium. Thus, the initial studies for the DTHR fuel/clad system have concentrated on thoria (ThO_2) or metallic thorium fuels clad in zircaloy with low temperature water coolant. Comparisons have been made with stainless steel clad and structures, helium coolant and uranium oxide fuels for evaluation purposes.

The DTHR must provide the technical foundation for key blanket design features which are essential in first generation commercial hybrid reactors. This subsection identifies leading blanket concepts, rates their potential for commercial applications, and defines by technical issues requiring early attention by designers.

IDENTIFICATION OF LEADING BLANKET CANDIDATES

Conceptually, there are large numbers of possible combinations of fuel form, cladding, and coolant systems that can be considered for the DTHR blanket.

TABLE 4-1

FRACTION OF FUSION NEUTRONS STRIKING VARIOUS WALL SEGMENTS

<u>Wall Section</u>	<u>Height Above or Below Midplane, Z (m)</u>	<u>Percent of Fusion Neutrons</u>
A	0.5	15.6
B	1.0	14.7
C	1.5	13.3
D	2.0	12.8
E	2.12	10.8
F	2.12	8.3
G	2.0	7.2
H	1.5	6.0
I	1.0	5.7
J	0.5	<u>5.6</u>
TOTAL		100.0

Many of the possible combinations had been studied and compared in the Actinide Burner Study⁽⁴⁻¹⁾ as well as the Fusion-Driven Hybrid Reactor Study⁽⁴⁻²⁾. The outcomes of blanket selections in those studies were based on specific design goals and requirements unique to those studies. Since the goals and requirements of the DTHR are different, a fresh look at potential blanket concepts for the DTHR was taken.

DTHR BLANKET CONCEPT SELECTION CRITERIA

Based on the general DTHR design objectives specific blanket concept selection criteria were established as follows:

- 1) Fissile production must be greater than or equal to 100 kg/yr.
- 2) The blanket concept selected must be fabricable consistent with a minimum cost DTHR.
- 3) Developmental risks must be minimized, consistent with the goal for a minimum cost DTHR program.
- 4) Power production and tritium breeding are not required for the initial blanket demonstration.
- 5) Extrapolation to commercial operations is desirable.

PRELIMINARY SCREENING OF BLANKET CONCEPTS

Based on the previous studies, it became apparent that one of the principal factors that rendered many blanket concepts unattractive is their low attainable fuel loadings. The amount of fuel loadings or the volume fraction of fuel in the blanket can provide an excellent indication of the capability for fissile breeding and power production. It was noted that circulating fluid fuels contaminate the entire coolant system and that fixed fuel concepts have much more applicable experience in fission reactors. As a consequence, the fluid-fueled concepts with their limited attainable fuel loadings (where the fluid fuels were circulated continuously in and out of the blanket) were rejected in favor of fixed fuel blanket concepts.

Among the possible coolants for fixed fuel blankets, water was not considered in the Actinide Burner and the Hybrid Reactor studies because of the difficulty with the high pressures required for efficient heat removal and power conversion. However, since electrical power production is not a specific DTHR design goal, low coolant temperatures and low pressures clearly render water a feasible blanket coolant for the DTHR. Early neutronic scoping calculations using water as the coolant-moderator, performed in this study as well as elsewhere⁽⁴⁻³⁾, showed encouraging fissile breeding characteristics. This fact, coupled with its state of the art technology, provided incentives for serious consideration of water-cooled, fixed fueled blankets as potential candidates.

Concern for being able to extrapolate to commercial hybrid reactors led to the consideration of alternate blanket concepts that utilize pressure tubes, although they may not be necessary. Two types of pressure tubes were included in the preliminary screening: a conventional pressure tube design similar to those of the Carolina Virginia Tube Reactor⁽⁴⁻⁴⁾, the CANDU⁽⁴⁻⁵⁾, the SGHWR⁽⁴⁻⁶⁾, and an "advanced" pressure tube concept where the pressure tube consists of concentrically extruded thorium-Zr alloy metal and Zircaloy clad forming a central coolant channel⁽⁴⁻⁷⁾. Cross sections of the two pressure tube concepts are illustrated in Figure 4-6.

A new type of fluid-fueled blanket was also conceived. This consists of either a stagnant or slowly flowing fluid fuel cooled by separate, high-pressure coolant channels. This concept is depicted in Figure 4-7. It differs from other fluid-fueled blanket concepts in the following ways:

- It is different from the fluid-fueled blankets considered in the Actinide Burner and Hybrid Reactor studies and the University of Wisconsin concept⁽⁴⁻⁸⁾ in that the fluid fuel does not circulate in and out of the blanket continuously and that it does not serve as the coolant. However, feed and bleed provisions are included to control the fuel constituents and afford continuous removal of fissile fuel.

Figure 4-6. Schematic Diagram of Pressure Tube Concepts

Figure 4-7. Advanced Fluid Fueled Blanket Approach

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- Unlike the blanket cassette concept for a pure fusion reactor conceived by ORNL for an EPR⁽⁴⁻⁹⁾, the blanket module is a segment that needs to be removed only in case of vacuum vessel wall or blanket service/replacement.

The fluid fuel systems that can be considered are compared in Table 4-2 together with the estimated potential blanket compositions. This table compares blankets where the fluid fuel also serves as the coolant by circulating it continuously in and out of the blanket (last column in the table) to a system using a separate coolant. From Table 4-2, it becomes evident that the attainable fuel loadings (v/o fuel) for molten salts actually decreased with a separate coolant system. However, for the other blanket concepts, fuel loadings can be improved significantly using a separate cooling system, except for the liquid fuel concept (No. 5). For liquid-fueled concepts, theoretically attainable fuel loadings are exceptionally high (66 to 86%) whether the fluid fuel serves as the coolant or not. One such fluid fuel is U-Zr-Fe eutectic which melts at $\sim 715^{\circ}\text{C}$.

Five candidate blanket concepts were therefore considered in a preliminary screening of blanket concepts. The blanket modules utilizing these concepts are shown schematically in Figure 4-8. The five concepts are the following:

- 1) LWBR-type fuel rods in an LWBR-type fuel assembly
- 2) Thorium fueled balls in a packed bed configuration
- 3) Fuel rods in a conventional pressure tube configuration
- 4) Advanced pressure tube concept
- 5) Advanced fluid fuel blanket concept

All other potential blanket concepts were discarded for most of the same reasons discussed in previous studies. The high pressure, helium-cooled, TZM clad fuel rod concept selected in the Actinide Burner and Hybrid Reactor studies as the reference blanket concept was excluded here because of the higher costs associated with fuel rod fabrication and development anticipated with the use of TZM clad material.

TABLE 4-2

COMPARISON OF BLANKET COMPOSITIONS AND MAXIMUM FUEL LOADINGS
(BLANKET STRUCTURAL MATERIALS NOT INCLUDED)

No.	Description	Separate Coolant System				Fluid Fuel in the Coolant
		v/o Carrier Fluid	v/o Fuel	v/o Structure	v/o Coolant	v/o Fuel
1	Aqueous Slurry	26	40	14	20	3.4
2	Organic Slurry	26	40	14	20	5.0
3	Liquid Metal Slurry	26	40	14	20	33 ⁺
4	Molten Salt	54	12	14	20	18.8*
5	Liquid Fuel	0	66	14	20	86.0

⁺Based on Th in Bi (solution) plus suspended ThBi₄.

*Based on LiF-BeF₂-ThF₄ system.

Figure 4-8 Alternate Blanket Module Concepts

The characteristics of the five blanket concepts considered here are compared in Table 4-3. A comparison matrix was set up to select a reference blanket for the DTHR. The selection criteria, their respective weighting factors, the blanket ratings and final rankings are summarized in Table 4-4. It is seen that, not surprisingly, the LWBR-type fuel rod assembly ranked first, followed closely by the conventional pressure tube concept. While the advanced fluid fuel concept has much higher potentials for fissile production and commercial applications (as a result of potentials for efficient power production and tritium breeding), it suffers from high developmental risks. The fueled ball, packed bed concept ranked last because it has relatively low ratings for all four selection criteria. Other than the fuel form, the characteristics of the fueled ball concept (from the standpoints of thermal hydraulic and mechanical design limitations) are fundamentally the same as the fuel rod concept. Consequently, it is retained for further detailed comparisons and final blanket selection.

It should be pointed out that the blanket selection criteria and the weighting factors were selected based on the design goals for a DTHR. For a commercial tokamak hybrid reactor, these factors can be substantially different and could lead to the selection of a different blanket concept. A final selection of a reference DTHR blanket concept was made based on the two basic blanket concepts retained and among different types of fuel and fuel forms which consist of thorium pellets, thorium microspheres, and thorium metal alloy (alloyed with up to 10% Zr).

4.1.3 FUEL ELEMENT SELECTION AND MODULE DESIGN

Previous experience with proliferation resistant fuels such as thorium was reviewed to determine candidate fuel/clad/coolant combinations that could be used for the DTHR fuel elements. Significant experience exists for both thorium (ThO_2) and metallic thorium fuels clad in stainless steel or zircaloy and water cooled. These applications are summarized in Table 4-5. Limited experience exists with thorium microspheres and some experience with spherical clad uranium fuels (balls - AVR Germany). From these experiences, five concepts, defined in Table 4-6, were then judged based on the following criteria:

TABLE 4-3

CHARACTERISTICS OF SOME ALTERNATE HYBRID BLANKET CONCEPTS

BLANKET CONCEPT DESCRIPTION (Refer to Figure 4-8)	FUEL LOADING CAPABILITY (~ FUEL v/o)	THERMAL- HYDRAULICS	POWER CONVERSION EFFICIENCY POTENTIAL	RELATIVE EASE OF FUELING/REFUELING/ FUEL SHUFFLING/ REMOTE HANDLING	STATUS OF TECHNOLOGICAL DEVELOPMENT	MECHANICAL DESIGN
FUEL RODS IN LWBR TYPE ASSEMBLY	HIGH (0.57)	LOW TEMP & LOW PRESSURES	LOW	OFF-LINE REFUELING/ FUEL SHUFFLING; TYPICAL FISSION REACTOR DOWNTIMES	STATE OF THE ART	LWBR TECHNOLOGY
FUEL BALLS (PEBBLE BED)	MEDIUM (0.41)	LOW TEMP & LOW PRESSURES	LOW	CONTINUOUS ON-LINE FUELING/REFUELING/ FUEL SHUFFLING POSSIBLE	RELATIVELY LITTLE TECHNOLOGICAL BASE	DIFFICULT DUCTING PROBLEMS
FUEL RODS IN PRESSURE TUBE	LOW (0.35)	HIGH TEMP & HIGH PRESSURES	HIGH	OFF-LINE REFUELING/ FUEL SHUFFLING/ TYPICAL FISSION REACTOR DOWNTIMES	WELL DEVELOPED	NO SERIOUS DIFFICULTIES
ADVANCED PRESSURE TUBE CONCEPT	HIGH (0.37)	HIGH TEMP & HIGH PRESSURES	HIGH	OFF-LINE REFUELING/ FUEL SHUFFLING/ TYPICAL FISSION REACTOR DOWNTIMES	SOME DEVELOPMENT	NO SERIOUS DIFFICULTIES
ADVANCED FLUID FUEL CONCEPTS	HIGH (0.66)	HIGH TEMP & HIGH PRESSURES	HIGH	CONTINUOUS ON-LINE REFUELING/ FUEL SHUFFLING	LEAST DEVELOPMENT	SPECIFIC PROBLEMS DEPEND ON FLUID FUEL SYSTEM AND AUXILIARIES

TABLE 4-4

PRELIMINARY COMPARISON OF BLANKET CONCEPTS FOR A DTHR

Weighting Factor	Selection Criterion	Relative Ratings of Alternate Blanket Concepts				
		Fuel Rods in LWBR-Type Assembly	Fueled Balls in Packed Bed Module	Conventional Pressure Tube	Advanced Pressure Tube	Advanced Liquid Fuel Blanket
.25	Fissile Yield Potential	.057	.046	.044	.042	.061
.25	Potential For Low Cost DTHR	.060	.050	.055	.045	.040
.25	Low Developmental Risk Potential	.075	.040	.065	.045	.025
.25	Extrapolatability to Commercial Scales	.045	.035	.050	.055	.065
Overall Relative Rating		.237	.171	.214	.187	.191
Relative Ranking		1	5	2	4	3

TABLE 4-5
APPLICATIONS WITH THORIUM FUEL EXPERIENCE

<u>Reactor</u>	<u>Fuel/Clad/Coolant</u>
Indian Point (First Core)	$\text{UO}_2\text{-ThO}_2\text{/SS/H}_2\text{O}$
Savannah River/Hanford Production ^(a)	$\text{ThO}_2\text{-/H}_2\text{O}$
LWBR at Shippingport	$\text{ThO}_2\text{/Zr/H}_2\text{O}$ $\text{UO}_2\text{-ThO}_2\text{/Zr/H}_2\text{O}$
Sodium Reactor Experiment (Core)	Met. Th/SS/Na
HWCTR at Savannah River (Test Elements)	Met. Th/Zr ^(b) /D ₂ O
ETR (Test Elements)	Met. Th/Zr ^(b) /H ₂ O

(a) Production of ^{233}U for LWBR - Shippingport.

(b) Coextruded metallic thorium and zirconium.

TABLE 4-6

DTHR BLANKET FUEL CONCEPTS

Concept No.	<u>1</u>	<u>2</u>	<u>3</u>	<u>4</u>	<u>5</u>
Fuel Material	ThO ₂	ThO ₂	Thorium Metal (5-10% Zr)	ThO ₂	Thorium Metal Alloy
Fuel Form	Pellets	Micro- spheres	Rods	Balls	Balls
Bond	He	He	---	He	Be
Clad	Zirc.	Zirc.	Zirc.	Zirc.	Zirc.
Clad/Fuel Form	Rods	Rods	Co- Extruded Rods	Balls	Balls
Coolant	H ₂ O	H ₂ O	H ₂ O	H ₂ O	H ₂ O

-
- Operational Reliability
 - Minimize fuel/clad failure potential
 - Minimize thermal/mechanical stresses
 - Technological Development
 - Minimize fuel/clad fabrication development
 - Minimize material property development
 - Minimize remote handling development
 - Minimize fuel reprocessing development
 - Design Considerations
 - Minimize fuel/clad/module complexity
 - Minimize energy attenuation in structure
 - Minimize component costs
 - Maximize blanket envelope versatility
 - Maximize reactor safety
 - Maximize data base development
 - Fissile Fuel Production Potential

GENERAL COMPARISONS

All concepts considered are based upon the use of Zircaloy-4 for fuel cladding and module components. Although extensive knowledge of fabrication technology and irradiation effects is available relative to Zircaloy's use in LWR's, little is known about its expected behavior in the high-energy neutron environment and the cyclic operating conditions associated with the hybrid reactor blanket. Therefore, all concepts have a similar level of design risk as a result of the lack of an appropriate data base in this area.

Zircaloy is fully compatible with water coolant to temperatures well beyond those being considered. No cladding or module component corrosion problems are anti-

icipated for any of the concepts. However, fretting due to cycling and/or vibration could be a problem, if the components are not properly designed for this phenomenon.

In all concepts, the burnup (fission density) will be small in comparison to that obtained in LWR's. Since fission gas generation, fission gas release and fuel swelling will be low, these effects should not evolve as major considerations in design selection. It is to be noted, however, that detailed design analyses may indicate differing degrees of conservatism in this assumption.

Table 4-7 provides an assessment of the major factors influencing the concept selection, state-of-the-art, and relative advantage or disadvantage of each concept. A general discussion of each concept is provided in the following paragraphs. All module concepts will share the common concern of designing for cyclic operation in a high-energy neutron environment.

CONCEPT 1 (THORIA PELLETS, RODS)

This concept makes maximum use of LWR technology. The use of pellet fuel provides a maximum assurance against loss of fuel in the event of cladding failure. Fabrication technology has been fully developed for LWR's which have used thorium based fuel. If LWR's were designed to accept "seed-blanket" cores, there is some potential that rods from the hybrid reactor could be used directly in these reactors, thus bypassing the expensive chemical reprocessing step. Blanket rods would be fully amenable to reprocessing in a "standard" LWR $\text{ThO}_2\text{-UO}_2$ fuel reprocessing plant.

CONCEPT 2 (THORIA MICROSPHERES, RODS)

The use of microspheres rather than high density pellets (Concept 1) offers a greatly enhanced possibility of LWR use without chemical reprocessing; i.e., it may be possible to remove the microspheres from the cladding, blend with microspheres of higher enrichment made from reprocessed LWR fuel to achieve the desired fissile content, and refabricate as LWR fuel. Microsphere fuel has the comparative disadvantages of possibly higher fission gas release and greater probability of fuel washout in the event of cladding failure. The cost of providing microsphere fuel may be higher and achievable smeared density may be slightly lower than for solid pellets. Large scale production of microspheres has not been demonstrated. Microsphere fuel is fully compatible with a "standard" LWR $\text{ThO}_2\text{-UO}_2$ fuel reprocessing plant.

TABLE 4-7

DTHR BLANKET FUEL/CLAD COMPARISON

Weighting Factor			FUEL MATERIAL AND FUEL/CLAD FORM				
	Consideration		RODS			BALLS	
			ThO ₂ Pellets	ThO ₂ Micro- spheres	Met. Th 10% Zr*	ThO ₂	Met. Th 10% Zr
		Concept No.	<u>1</u>	<u>2</u>	<u>3</u>	<u>4</u>	<u>5</u>
	<u>Operational Reliability</u>						
3	Fuel Swelling	-	1.0	0.8	0.5	0.7	0.5
3	Gas Release and Control	-	1.0	0.8	0.7	0.7	0.7
4	Thermal/Mech. Cycling on Bond	-	1.3	1.3	0.7	1.3	0.7
10	Thermal Fatigue	-	0.3	0.3	0.3	0.3	0.3
13	Resistance if Clad Failure	-	4.3	3.7	1.3	2.6	1.3
33			<u>7.9</u>	<u>6.9</u>	<u>3.5</u>	<u>5.6</u>	<u>3.5</u>
	<u>Fuel Fabrication Development</u>						
2	Purity Requirements - Fabrication Process	-	0.3	0.2	0.3	0.3	0.3
4	Alloy/Compound Fab Process Options	-	0.7	0.4	0.7	0.2	0.7
11	State of the Art	-	1.9	1.0	1.9	1.0	1.9
17			<u>2.9</u>	<u>1.6</u>	<u>2.9</u>	<u>1.5</u>	<u>2.9</u>
	<u>Clad/Fuel Fabrication Development</u>						
4	Clad/Fuel Bonding	-	0.7	0.7	0.7	0.7	0.4
4	Clad Weld Joint Integrity	-	0.7	0.7	0.6	0.3	0.3
9	State of the Art	-	1.5	1.5	1.5	0.2	0.2
17			<u>2.9</u>	<u>2.9</u>	<u>2.8</u>	<u>1.2</u>	<u>0.9</u>
	<u>Module Design Considerations</u>						
2	Design Experience	-	0.16	0.12	0.12	0.08	0.08
2	Fuel Shuffling Control	-	0.16	0.16	0.16	0.08	0.08
2	Remote Handling Requirements	-	0.08	0.08	0.08	0.16	0.16
2	Wall Support from Fuel Assemblies	-	0.08	0.08	0.16	0.04	0.04
8			<u>0.48</u>	<u>0.44</u>	<u>0.52</u>	<u>0.36</u>	<u>0.36</u>
25	<u>Fissile Fuel Production</u>	-	<u>5.0</u>	<u>4.8</u>	<u>6.3</u>	<u>3.5</u>	<u>5.3</u>
100							
		<u>Weighted Rating:</u>	<u>19.18</u>	<u>16.64</u>	<u>16.02</u>	<u>12.16</u>	<u>12.96</u>
		<u>Weighted Rank:</u>	<u>1</u>	<u>2A</u>	<u>2B</u>	<u>3B</u>	<u>3A</u>

*Coextruded.

CONCEPT 3 (METALLIC THORIUM - 5 TO 10% ZIRCONIUM ALLOY, COEXTRUDED RODS)

The thorium alloy provides a maximum density of thorium atoms within the blanket rods. The fabrication technology has been developed and demonstrated with Th-U alloys. A good metallurgical bond is achieved during coextrusion though some degradation may occur during irradiation. A major disadvantage of this concept is corrosion and loss of fuel over an extended period following fuel pin failure. The presence of 5 to 10 percent zirconium in the alloy greatly reduces the corrosion rate compared to unalloyed thorium. A fabrication cost advantage might also be achieved if large tubular elements rather than rods were utilized. Swelling, though small, must be accommodated by cladding strain (high cladding stresses). The head-end of the fuel reprocessing plant would need to be modified to provide for dissolution of the thorium alloy.

CONCEPT 4 (ZIRCALOY CLAD, THORIA BALLS)

The principle advantages of this concept (over Concept 1) are ease of charging/discharging of the blanket material and adaptability to complex geometry. The concept has the disadvantages (relative to Concept 1) of requiring extensive fabrication development, especially the cladding process, probable increased costs, and the need for modification of the head-end of the fuel reprocessing plant to provide for dissolution of the clad material. Gas release may be an additional problem because of the lack of gas plenum. It is probable, however, that released gases can be accommodated by the use of a lower density fuel.

CONCEPT 5 (ZIRCALOY CLAD THORIUM ALLOY BALLS)

This concept has the same relative advantages described for Concept 4. Additionally, the cladding can be metallurgically bonded to the alloy ball and there is no gas release from the thorium alloy. The concept requires extensive process development, especially in the area of cladding of the alloy balls. The disadvantages are increased fabrication costs, corrosion following cladding failure, and modified head-end of the reprocessing plant to provide for dissolution of the thorium alloy. Swelling of the alloy will increase stress (and strain) on the cladding.

SPECIFIC COMPARISONS

The considerations in Table 4-7 resulting in different ratings are discussed below, with the major reasons for lower ratings identified.

- Fuel Swelling - Microspheres were rated below pellets because of less experience even though they should have less swelling. The thoria ball concept was rated lower because of plenum design problems. Metallic thorium was rated lower because it has more swelling than thoria.
- Gas Release and Control - Microspheres have larger surface area than pellets and all gas is released. Metallic thorium retains gas with its matrix (cannot design a plenum for it). Thoria balls cannot provide a good gas plenum.
- Thermal/Mechanical Cycling on Bond - Metallic thorium has additional stress bonds that thoria will not have.
- Release if Clad Failure - Microspheres have a greater washout problem than pellets. Thoria balls have smaller amounts of fuel, but more balls. Metallic thorium has high corrosion (washout) once clad fails.
- Purity Requirements - Microspheres require a purer atmosphere during fabrication than do the pellets, rods or balls.
- Alloy/Compound Fabrication Process Options - Pellets are standard; microspheres have never been done commercially and will be more complex. Thoria balls will be hard to encapsulate; no experience.
- Fuel Fabrication State-of-the-Art - Most experience has been with pellets; adequate experience with metallic thorium coextruded rods exists and metal stamped balls; little experience with microspheres; no experience with thoria balls although powder metallurgy has made significant advances in recent years.
- Clad/Fuel Bonding - Metallic thorium balls will require a bonding agent such as beryllium. Metallic thorium coextruded zirconium rods require no bonding agent. Thoria bonding with a helium atmosphere has considerable experience.
- Clad Weld Joint Integrity - Metallic thorium rods require end machined concave surface to allow for welding. Ball geometries are the hardest to get "clean" welds.
- Clad/Fuel Fabrication State-of-the-Art - Considerably less experience exists for balls as compared to rods.

- Module Design Experience - Considerable experience exists with pellet/rod fuel assemblies, followed by coextruded rods. Ball loaded modules have the least experience.
- Fuel Shuffling Control - Rods are precisely positioned in controllable locations as opposed to balls which can only be compartmentalized.
- Remote Handling - Balls can be poured out of the modules while rods must be removed as fuel assemblies and individual rods.
- Wall Support - Metallic thorium rods can provide support to the module walls. Pellet loaded rods may provide some wall support at grid locations. Balls provide no wall support.
- Fissile Fuel Production - Metallic thorium produces at least 20% more fissile fuel than thoria. Microspheres have a maximum packing density of 88% of the pellet/rod fuel volume fraction. The balls (of a single diameter similar to the rod concept diameters) have a fuel volume fraction of 40% as compared to 57% for the rod geometry considered. Thus, if Concept 3 (metallic thorium rods) achieved 240 kg/yr fissile fuel production, Concept 1 (thoria pellets) would achieve ~200, Concept 2 (thoria microspheres) ~180, Concept 4 (thoria balls) ~140, and Concept 5 (metallic thorium balls) ~200 kg/yr.

Operational reliability was given the heaviest weighting in Table 4-7, since a fuel element failure could shut down the DTHR testing and severely jeopardize the success of the entire hybrid program. Fissile fuel production was rated next heaviest, because this is the major objective of DTHR. Minimizing fuel and clad/fuel fabrication development were equally weighted next in magnitude to reflect the desire to pick state-of-the-art fuel and fuel/clad combinations. The module design was weighted the least, since it was conceived to be a simple, low cost concept with the ability to establish a good data base for material cyclic performance rather than representing an advanced commercial concept. The weighted ratings were totaled at the bottom of each concept column. The thoria pellets had the highest weighted rank based on the more extensive experience that exists for this concept. The thoria microspheres and metallic thorium rods ranked a close second to the thoria pellets. Both ball concepts ranked lower because of the lack of experience, reliability or fissile fuel production criteria. Since rods clearly outranked balls, the initial fuel element geometry was selected as pellets in rods.

Figure 4-9 shows the DTHR fuel element conceptual design. The fuel rod concept is based on the LWBR blanket fuel rod design except for using a longer rod. The LWBR blanket fuel rods were about 310 cm overall length with a 263.4 cm fuel section. The plenum length required for the DTHR concept has not been analyzed yet. The thermal and mechanical stresses for this fuel element concept are discussed in subsection 4.1.4.

3.3.2 BLANKET MODULE DESIGN

The DTHR blanket module conceptual design is shown in Figure 4-10. The module is nearly rectangular with a slight taper outward from the plasma side. It is fabricated from zirconium, although the upper plenum and lower base components could be made of stainless steel. Coolant lines are flanged at each end for removal of the module from the reactor. The orifice plates are located in the upper plenum and lower base to provide the required coolant flow velocities to the blanket zones. The top and bottom base plates are used to mount the fuel rods. Grids are located at various heights in the module to position the rods and support them from bowing. The module cross section showing the fuel lattice configurations is shown in Figure 4-11. Zone 1 contains a tighter pitch and higher velocity coolant flow than Zone 2. The advantages of the two-zone concept are discussed in subsections 4.1.4 and 4.1.5. The thermal and mechanical stresses in the module walls are evaluated in subsection 4.1.4. Further refinement of the stress analyses may enable deletion of the rib members supporting the plasma-side wall. Design optimization of the rib locations, fuel rod diameter and pitch were not attempted at this point in the design study. Neutronic evaluation of the zone thickness and a graphite reflector or third fuel zone were investigated. A third zone identical to Zone 1 was considered to enable fuel shuffling by rotating the module 180° after each year of operation.

The advantages of the simple rectangular module are ease of manufacture, low cost, and significant fatigue stresses to establish materials data base in a short term operation. The latter item will provide the data base needed for more refined module designs, since these modules will complete their fissile production objective within the first year or two and can be removed for early metallurgical examination. The disadvantage of the design is the limitation to low coolant pressures, which restricts the choice of fluids and/or operating conditions.

BOTTOM MOUNTED RODS

TOP MOUNTED RODS

Figure 4-9. DTHR Fuel Element Concept

Figure 4-10. DTHR Blanket Module Concept

ZONE	ROD DIA	PITCH	TOTAL RODS
1	1.45 cm	1.6 cm	252
2	1.45 cm	1.95 cm	288
			<u>540 TOTAL</u>

Figure 4-11. DTHR Blanket Lattice Concept

4.1.4. BLANKET THERMAL/MECHANICAL DESIGN CONSIDERATIONS

The blanket cooling system design was based on a number of considerations that included the following:

- Maximizing fissile yield
- Minimum cost DTHR
- Blanket thermal-hydraulic and structural design constraints

In order to maximize the fissile yield, blanket structural materials must be minimized. However, this would impact structural design constraints. A minimum cost DTHR goal suggests that the simplest blanket cooling system be adopted (i.e., a once-through system) for the initial reference in order to obviate the need for and the associated costs of a cooling tower. This in turn required that the heat rejection temperature be kept low. For low coolant operating temperatures, the blanket pressures can be reduced and still avoid coolant boiling. The blanket module wall thicknesses can also be minimized. It becomes evident that low temperature and low pressure coolant operating conditions are consistent with all of the major DTHR design goals.

In order to establish a mechanical-structural blanket design and to determine coolant operating conditions, parametric analyses were carried out. The analyses involved the blanket module wall thickness, fissile yield, and coolant operating conditions.

The blanket module is essentially a pressure vessel. For a given module life and wall thickness, there is a maximum coolant design pressure allowed. Nuclear heat is deposited in the module wall, developing thermal stresses. Thus, there is a maximum wall thickness allowed based on thermal fatigue considerations. Following LWBR technology, Zircaloy-4 was selected as the fuel clad as well as the module wall material. O'Donnell and Langer⁽⁴⁻¹⁰⁾ studied experimental fatigue data on irradiated and unirradiated Zircaloy 2, 3, and 4 and presented recommended design curves for Zircaloys (reproduced in Figure 4-12). These design curves contain a safety factor of two on the stress amplitudes or 20 on the allowed cycles, depending on whichever is limiting. The design stress

Room temperature to 600°F. Total irradiation exposure 5.5×10^{21} nvt fast flux (>0.625 eV)

Figure 4-12. Design Curve for Irradiated Zircaloy -2, 3, 4

limits at 10^6 cycles are compared with other mechanical properties of Zircaloy⁽⁴⁻¹¹⁾ in Table 4-8. From the magnitudes of the allowed design stresses, it becomes apparent that the module, fuel clad designs are controlled by the fatigue failure limits. By taking 10^6 cycles as the reference DTHR blanket life, the design stress limit is $5.5 \times 10^7 \text{ N/m}^2$ on local mean stress. The mean stress consists of the mean alternating thermal stress and the bending stress on the wall. During the power-off period, heat deposition in the module wall is negligible. Consequently, the mean alternating thermal stress, to a good approximation, is one-half of the maximum thermal stress.

Scoping parametric analysis was performed to evaluate the effect of the ratio of mean thermal stress to the wall bending stress on the maximum pressure difference across the module wall allowed for different separations between module inner walls (ribs at 10 to 20 cm). The results presented in Figure 4-13 showed that for any separation distance (a), the maximum allowed pressure difference across the module wall is obtained with a mean thermal stress-to-bending stress ratio of unity. This optimum ratio is independent of the separation distance, although the higher the pressure differences the smaller is the allowed separation between ribs. In the interest of maintaining the capability for blanket temperatures and pressures as high as possible, a 10 centimeter separation was selected as a reference. The effect of the ratio of the mean stress to the bending stress on the module front wall thickness is shown in Figure 4-14. The calculations were based on the peak thermal stresses.

Figure 4-14 shows that the optimum stress ratio (unity) corresponds to a wall thickness of 1.47 cm. However, blanket neutronic performance requires the minimization of the module wall thickness. Based on this consideration, a module front wall thickness of 0.5 cm was selected. This leads to a maximum allowed alternating thermal stress of $6.48 \times 10^6 \text{ N/m}^2$, a bending stress of $5.19 \times 10^7 \text{ N/m}^2$, and pressure difference across the wall of $4.76 \times 10^5 \text{ N/m}^2$. By taking an ambient pressure (external to the module) of 1 Atm., the maximum allowed internal coolant pressure is $5.8 \times 10^5 \text{ N/m}^2$. With this limit and a maximum clad surface temperature not to exceed surface boiling, the maximum

TABLE 4-8
MECHANICAL PROPERTIES OF ZIRCALLOY

PROPERTY	VALUE FOR IRRADIATED MATERIAL	ACCEPTABLE DESIGN VALUE*
ULTIMATE STRENGTH	$4 \times 10^8 - 4.8 \times 10^8 \text{ N/m}^2$	$3.6 \times 10^8 - 4.3 \times 10^8 \text{ N/m}^2$
CYCLIC YIELD STRENGTH	$2.3 \times 10^8 \text{ N/m}^2$	$1.56 \times 10^8 \text{ N/m}^2$
ENDURANCE UNIT	$1.7 \times 10^8 \text{ N/m}^2$	$8.6 \times 10^7 \text{ N/m}^2$
MIN. ALTERNATING LOCAL STRESS TO PRODUCE FATIGUE FAILURE FOR 10^6 CYCLES		$9.6 \times 10^7 \text{ N/m}^2$
MIN. LOCAL MEAN STRESS TO PRODUCE FATIGUE FAILURE FOR 10^6 CYCLES		$5.5 \times 10^7 \text{ N/m}^2$

*Based on LWBR Design Practice.

Figure 4-13

Effect of Ratio of Allowed Thermal Stress to Bending Stress on Allowed Pressure Difference Across Module Wall

Figure 4-14. Effect of Ratio of Allowed Thermal Stress to Bending Stress on the Module Front Wall Thickness Required

inlet coolant temperature allowed was calculated as a function of the coolant temperature rise for different levels of blanket mean power density (in zone 1 of the two-section module).

It was assumed conservatively (See Figure 4-18, subsection 4.1.5) that the peak-to-average power ratio is a constant at 1.33. The results of this parametric analysis are presented in Figure 4-15. Similar calculations for a maximum coolant pressure of $2 \times 10^6 \text{ N/m}^2$ were made and noted in the figure for illustrative comparisons. It is seen that the maximum allowed coolant inlet temperature decreases with increasing power density (or increasing burnup and increasing neutron exposure). For a given mean power density, there is an optimum maximum inlet coolant temperature to avoid surface boiling. The results of the analysis clearly suggest that there is a trade-off between fissile yield and potential power conversion. With thicker module walls, the fissile yield would be reduced; however, the coolant pressure and temperatures allowed can be increased, thereby increasing the capability for power conversion.

Since power conversion is not a primary design objective for the DTHR, fissile yield was maximized and moderate blanket operating conditions were selected, as summarized in Table 4-9. These operating conditions were specified based on a once-through cooling system with the total rejection of blanket thermal power as waste heat.

Thermal fatigue was determined to be the blanket component life-limiting factor. However, under the moderate blanket operating temperatures and pressures, an infinite number of cycles can be sustained for 0.4 MW-yr/m^2 blanket neutron exposure. Higher exposures are possible without changes in the blanket operating conditions.

The maximum fuel clad temperature history for each cycle was calculated and shown in Figure 4-16. The temperatures are normalized to the peak clad to coolant inlet temperature difference. It can be seen that steady-state temperatures are attained in less than 10 seconds after the initiation of plasma burn. During the power-off period, cooldown is achieved in less than 5 seconds, neglecting decay heat.

Figure 4-15 Effect of Coolant Temperature Rise in the Front Section on the Maximum Inlet Coolant Temperature Allowed

TABLE 4-9
SUMMARY OF DTHR BLANKET OPERATING CONDITIONS
AT 0.4 MW-YR/m²
(~ 1 year of operation at reference conditions)

●	MODULE POWER	21.6 MW
●	MEAN POWER DENSITY (FRONT SECTION)	51 W/cm ³
●	PEAK-TO-AVERAGE POWER RATIO	1.33
●	MAXIMUM LINEAR POWER RATING	19 kW/m
●	INLET COOLANT TEMPERATURE	44.4°C
●	AVERAGE COOLANT ΔT	27.8°C
●	MAXIMUM SUBCHANNEL COOLANT ΔT	36.7°C
●	MAXIMUM LOCAL COOLANT TEMPERATURE	81.7°C
●	MAXIMUM CLAD SURFACE TEMPERATURE	108.9°C
●	PEAK CLAD TEMPERATURE	116.7°C
●	PEAK FUEL CENTERLINE TEMPERATURE	393°C
●	INLET COOLANT PRESSURE	2.24 Atm.
●	COOLANT ΔP	0.09 MN/m ²
●	TOTAL FLOWRATE PER MODULE	7.2 x 10 ⁵ kg/hr
●	RATIO OF COOLANT PUMPING POWER TO THERMAL POWER OVER ENTIRE MODULE	10 ⁻³

Figure 4-16. Zircaloy Clad Temperature History During Each Cycle

Based on the reference operating conditions, other mechanical-materials conditions such as fuel clad thermal and hoop stresses, fuel elongation, etc. were evaluated. The results are tabulated in Table 4-10. It becomes apparent that under the moderate blanket operating conditions the stresses and strains of all blanket components are relatively moderate. The relatively small rod elongation and deflection are not expected to cause clad fretting problems. This mechanism for clad fretting is unique to a tokamak blanket. While actual analyses have not yet been performed, materials swelling and creep induced by radiation are judged to be negligible based on previous studies.

From the results of the preliminary thermal and structural analyses, it is concluded that there do not appear to be any issues that could render the DTHR blanket technically infeasible. In fact, there is considerable latitude in the blanket operating conditions due to the conservative design guidelines adopted.

COOLING SYSTEM

The once-through cooling system consists of tapping cooling water from an appropriate river. Standard treatment of the water is provided. This includes the following:

- Ferrofloc precipitation
- Sand filtering
- pH control

The water is then passed through the blanket to remove the blanket thermal power produced. The water leaving the blanket is dumped directly back to the river at a location downstream from the inlet port. The tritium concentration in the once-through water system is estimated to be well below the allowable level of $3 \times 10^{-3} \mu\text{Ci/cm}^3$.

TABLE 4-10

OTHER MECHANICAL-MATERIALS CONSIDERATIONS

FOR DTHR BLANKET

● Fuel clad thermal stress amplitude	$1.18 \times 10^7 \text{ N/m}^2$
● Fuel clad hoop stress at 0.4 MW-Yr/m^2	Negligible
● Maximum alternating rod elongation	1.45 mm
● Maximum rod deflection	0.16 mm
● Dead Weight Stress	Within LWBR Design Constraints
● Materials swelling due to Neutron Irradiation	Expected to be negligible at the operating conditions
● Radiation induced creep	Same as above
● Radiation induced flaw growth rates	No significant difference from LWBR experience expected.
● Creep in clad or module wall	Negligible

The blanket cooling system is shown schematically in Figure 4-17 along with the coolant state points and flowrate. The water inlet temperature (to the blanket) of 44.4°C was selected as the worst case (the maximum water temperature possible) to determine the maximum heat rejection temperature. Under most normal operating conditions, water temperatures are expected to be tens of degrees lower. The low heat rejection temperatures avoid the use of cooling towers. The beginning of life (BOL) coolant temperature is given in parentheses.

It is noted that the total blanket thermal power after one year is ~600 MW. In case the waste heat of this magnitude cannot be rejected to the river for reasons of environmental considerations, then a cooling tower with a water to air heat exchanger would be required. The inclusion of a forced draft air-cooling tower would have an impact on the blanket design if a minimum cost DTHR is the overall design goal. This is due to the fact that in order to reduce the size of the water to air heat exchanger and the size of the cooling tower, blanket exit temperatures appreciably higher than 72.2°C would be needed. This in turn would require higher blanket operating pressures, which affect the blanket module design. It is clear that based on the cooling system requirements and constraints there can be trade-offs between cooling system costs and fissile yield if blanket operating pressures affect the module wall thickness and the blanket composition.

Figure 4-17. Schematic Diagram of Blanket Cooling System

KEY TECHNICAL ISSUES

A number of key technical issues with respect to blanket engineering feasibility were identified. Those that are of particular concern are the following:

- Cyclic reactor operation
- Nonuniform radial power distribution
- Steadily increasing power generation with time

The cyclic reactor operations can lead to thermal fatigue of blanket components and fretting of the fuel clad that limit the blanket life. The nonuniform power distribution and the increasing blanket power production with time can constrain the possible operating conditions of the blanket from the standpoints of coolant boiling constraints and pressure stress limits. The preliminary analyses performed to evaluate these limits have shown that under the moderate operating conditions selected for the DTHR, none of the blanket operating characteristics would limit the component life for the limited neutron exposure considered. However, if neutron exposures appreciably greater than say 1 MW-yr-m^2 were to be considered, then blanket design and coolant operating conditions may have to be modified. Among the possibilities is the use of a pressure tube concept, discussed previously.

For potential scale-up to a commercial tokamak hybrid reactor (CTHR), a great deal depends on the economics of a symbiotic CTHR-LWR economy and on whether a CTHR can (economically) or should be a dedicated fissile breeder. If power self-sufficiency or substantial power production are necessary constraints (from economic considerations) on a CTHR design, then an alternate blanket concept, such as a pressure tube or an advanced fluid-fueled blanket, may have to be considered.

The blanket operating conditions were selected based on end-of-life blanket power densities. The significant changes in blanket power production over the blanket lifetime is not a problem for a once-through cooling system; however, for a CTHR, it could present a complex problem for system operation. One possible solution is to introduce a staggered blanket module refueling scheme so that blanket power variations can be reduced.

4.1.5 NUCLEAR PERFORMANCE CHARACTERISTICS

In this section the nuclear performance of the blanket concept for the DTHR is described in detail, including the effects of various exposure times. Also described are the results of a parametric blanket neutronics survey which investigated the performance of Th metallic fuel vs. ThO_2 ; He cooling vs. water cooling; depleted UO_2 blankets; and depleted UO_2 multiplier zones in front of a ThO_2 zone. However, the main emphasis has been on water cooled, zircaloy clad ThO_2 blankets, including variations of the moderator-to-fuel ratio in various zones of the blanket.

The neutronics calculations have all been performed with the one-dimensional ANISN neutron transport code.⁽⁴⁻¹²⁾ A vertical cylindrical geometry representation was used, i.e. with the axial direction of the cylinder along the center line (major axis) of the torus. This geometrical model allows representation of a blanket/shield region on the outer part of the torus and a shield-only region on the inner part of the torus. The vacuum vessel/first wall used in the calculations had an effective thickness of 1 cm and was composed of 50% (volume) steel and 50% water, based on the design discussed in subsection 3.2.5. The nuclear calculations were done using an S_8 treatment of the angular quadrature and a P_3 approximation to the anisotropic scattering.

A 26 neutron group cross section set was prepared from the ORNL-RSIC distributed VITAMIN-C processed data library⁽⁴⁻¹³⁾ for use in the calculations. This library provides for the capability to perform resonance self-shielding, which will have an important effect on the calculations, especially in water moderated blankets. Cross section sets for Fe and ^{238}U were self-shielded using the automated process provided for in VITAMIN-C. However, the data set for ^{232}Th was not available in the required format to perform self-shielding in this manner. Thus, a hand calculation was performed using the method of Bondarenko⁽⁴⁻¹⁴⁾ to calculate a self-shielded data set for ^{232}Th . This hand calculation required a great deal of rough interpolation, and thus is not thought to be as accurate as the automated method. However, it is felt that the hand self-shielded ^{232}Th data set is a good approximation. The data set for ^{232}Th in the required format

for self-shielding has recently (March, 1978) become available from RSIC, and hence future calculations will use this improved data set. The self-shielding to date has included energy effects only, i.e. no spatial self-shielding effects have been accounted for. Spatial self-shielding will be taken into account in future calculations, now that the ^{232}Th data set in the required format has become available.

4.1.5.1 Parameter Definition and Normalization

The neutronics results presented in the following sub-sections will be expressed in terms of two sets of parameters - the per fusion neutron blanket parameter values and the converted DTHR macroscopic performance parameters. The nomenclature used for these variables is defined below:

$F \equiv$ number of fissile atoms (net) produced per incident fusion neutron (from plasma)

$f \equiv$ number of blanket fission reactions produced per incident fusion neutron

$M \equiv$ blanket D-T fusion neutron energy multiplication factor

Assuming 200 MeV of energy released per fission, M is calculated from

$$M = \frac{14.1 + 200 \times f}{14.1} = 1.0 + 14.2 \times f$$

$\ell \equiv$ number of neutrons leaking out the back of the blanket per incident fusion neutron

$h =$ ratio of energy released in the blanket and shield from non-fission reactions to the fusion reaction energy (17.6 MeV)

The product $17.6 \times h$ is the usual value of ~ 20 -21 MeV released from exothermic nuclear reactions in the blanket and shield regions. This does not, however, include the energy release from blanket fission reactions, which is included separately in the f and M values.

With regard to the plasma fusion driver, the following definitions are used:

$PA \equiv$ plant availability factor

$DC =$ plasma duty cycle

P_F = peak plasma fusion power (MW)

P_n = peak fusion neutron power (MW) = $0.8 \times P_F$

An important consideration is the fraction of the plasma fusion neutrons which directly strike areas covered by blanket modules. Two variables have been used to describe the blanket coverage, with definitions:

b_p $\equiv$ blanket poloidal wall coverage factor = fraction of the fusion neutrons which directly intercept blanket modules in the poloidal direction

b_t $\equiv$ blanket toroidal wall coverage factor = fraction of the total toroidal circumference which is covered by blanket modules

With these variable definitions, the following formulas can be used to calculate the annual fissile production and the total plant thermal power:

G $\equiv$ annual fissile production rate (kg/year)

$$G = F \frac{\text{fissile atoms}}{\text{fusion neutron}} \times P_n \text{ MW} \times 6.28 \times 10^{18} \frac{\text{MeV}}{\text{MW-sec}} \\ \times \frac{1 \text{ neutron}}{14.1 \text{ MeV}} \times \frac{A_o \text{ gm}}{6.02 \times 10^{23} \text{ atoms}} \times \frac{1 \text{ kg}}{10^3 \text{ gm}} \times 3.154 \times 10^7 \frac{\text{sec}}{\text{yr}} \\ \times PA \times DC \times b_p \times b_t$$

or

$$G = 2.333 \times 10^{-2} \times F \times P_n \times A_o \times PA \times DC \times b_p \times b_t \frac{\text{kg fissile}}{\text{year}}$$

where P_n is in MW and A_o is the atomic weight (amu) of the fissile fuel. Also, the plant power production is:

P_T $\equiv$ peak thermal power of the plant (MWt), including both plasma fusion reactions and blanket fission reactions.

$$P_T = P_F \times h + \left[200 \times f \frac{\text{MeV}}{\text{neutron}} \times P_n \text{ MW} \times 6.28 \times 10^{18} \frac{\text{MeV}}{\text{MW-sec}} \right. \\ \left. \times \frac{1 \text{ neutron}}{14.1 \text{ MeV}} \times \frac{1 \text{ MW-sec}}{6.28 \times 10^{18} \text{ MeV}} \times b_t \times b_p \right]$$

or

$$P_T = P_F \times h + \left[200 \times f \times P_n \times \frac{1}{14.1} \times b_t \times b_p \right]$$

But, $P_n = 0.8 \times P_F$ or $P_F = 1.25 \times P_n$; and from the definition of M,

$$200 \times f = 14.1 (M-1)$$

Thus the equation for the plant power becomes:

$$P_T = P_n \times \left[1.25 \times h + (M - 1) \times b_t \times b_p \right] \text{ MWt}$$

4.1.5.2 DTHR PERFORMANCE ASSUMPTIONS

In order to convert the blanket neutronics parameters such as F and M to power and fissile fuel production quantities P_T and G, specific values must be assumed for the fusion driver parameters and blanket coverage factors defined in the previous sub-section. For the DTHR device the following values have been used in the neutronic analysis:

$P_F = 950$ MW of fusion power

$P_n = 760$ MW of D-T fusion neutron power

PA = 0.40, plant availability factor

DC = 0.50, plasma duty cycle

$b_p = 0.43$, poloidal wall coverage factor

$b_t = 0.56$, toroidal wall coverage factor

$h = 1.14$, non-fission energy enhancement factor due to exothermic nuclear reactions in shield

The value of $P_F = 950$ MW is equivalent to assuming a fusion neutron wall loading of 2 MW m^{-2} in the DTHR, as shown in Table 3-2 of subsection 3.1.1.

The value for the poloidal wall coverage factor b_p is estimated from a calculation of the poloidal wall loading distribution about the first wall of the DTHR. The computational method used in this analysis is described in reference 4-15.

The method is based on an outwardly shifted plasma source distribution which models the magnetic flux surface pattern of the plasma as described in subsection 3.1.4. For the DTHR, this analysis indicates that in the poloidal direction about 43% of the fusion neutrons will directly intercept the blanket modules of height 3.7 m. This is further discussed in subsection 4.1.1 and is also shown in Figure 4-5.

The value for the toroidal wall coverage of 56% assumes blanket modules are located in between 9 of the 16 TF coils, as described in subsection 4.1.1.

For the energy release from non-fission reactions in the blanket and shield regions, a value of 20 MeV has been assumed. Hence, $h = 20 \div 17.6 = 1.14$.

Thus with these values for the DTHR plasma performance and blanket coverage, the formulas for fuel and power production become:

$$P_T = 1083 \times \left[1.0 + (M-1) \times 0.169 \right] \text{ MWt}$$

$$G = 199 \times F \text{ kg } ^{233}\text{U/year}$$

and $G = 204 \times F \text{ kg } ^{239}\text{Pu/year}.$

4.1.5.3 BLANKET DESCRIPTION AND PERFORMANCE

The basic DTHR blanket concept consists of ThO_2 pellets in zircaloy clad rods with water cooling. The total blanket thickness of 25 cm is divided into two zones with thicknesses of 10 cm and 15 cm, as discussed in subsections 4.1.3 and 4.1.4. The material volume fractions used in the neutronics calculations and the pin diameters and pitches assumed in the two zones are shown in Table 4-11.

In order to maximize the neutron multiplication from fission, $(n, 2n)$, and $(n, 3n)$ reactions in the Th, it is desirable to have as high a fuel volume fraction as possible in the blanket regions closest to the plasma D-T neutron source and as low a fraction as possible of materials which undergo competing reactions. However, as will be shown in subsection 4.1.5.4, after ~10 cm of blanket thickness the neutrons are to a large extent moderated below the energies needed to

TABLE 4-11

BLANKET ZONE MATERIAL DESCRIPTIONS USED IN THE NEUTRONICS CALCULATIONS

	<u>ZONE 1</u>	<u>ZONE 2</u>
THICKNESS	10 cm	15 cm
ThO ₂ VOLUME FRACTION	0.569	0.386
H ₂ O VOLUME FRACTION	0.241	0.474
ZIRCALOY VOLUME FRACTION*	0.176	0.130
VOID VOLUME FRACTION	0.014	0.010
$\frac{\text{WATER}}{\text{FUEL}}$ VOLUME RATIO	0.423	1.228
PITCH (TRIANGULAR)	1.60 cm	1.95 cm
$\frac{\text{PITCH}}{\text{DIAMETER}}$ RATIO	1.10	1.35

*INCLUDES BOTH CLAD AND STRUCTURE.

ROD o.d.	1.45 cm (0.571 in)
CLAD THICKNESS	.065 cm (0.026 in)
GAP THICKNESS	.008 cm (0.003 in)
PELLET DIAMETER	1.304 cm (0.513 in)

produce these threshold type neutron multiplying reactions in Th. Thus beyond this thickness the water-to-fuel volume ratio can be increased with only a very small penalty in the ^{233}U production. Decreasing the ThO_2 and increasing the water volume fractions has at least three beneficial effects:

- The ThO_2 blanket inventory is decreased,
- The concentration of ^{233}U in Th is increased,
- Neutron leakage from the back of the blanket is decreased due to the larger fraction of water which provides more neutron attenuation.

These effects are further discussed in subsection 4.1.5.4, where results are presented for two other values of the water-to-fuel volume ratio in the last 15 cm of the blanket.

In Table 4-12 the performance of the water cooled, zircaloy clad ThO_2 DTHR blanket concept is shown as a function of exposure time. The DTHR is designed with a wall loading of 2 MWm^{-2} and a product of the plant availability x plasma duty cycle of 0.20, so that in 1 year the DTHR will achieve an integral neutron wall loading of 0.4 MW-yr-m^{-2} .

As can be seen, the net fuel production rate F or G decreases with operating time to a value after 3 years of exposure in the DTHR that is about 23% smaller than at BOL. The reason for this is that some of the ^{233}U which starts to build up under irradiation then begins to fission in the thermal neutron spectrum. The capture reactions in ^{232}Th are increased due to the higher neutron population in the blanket; however, this is not enough to counter-balance the higher neutron absorption rate in ^{233}U nuclei. Hence the net fissile production rate decreases, although it is still greater than zero. Here the absorption rate includes both capture and fission reactions in ^{233}U . In these blankets the α (capture-to-fission ratio) for the ^{233}U is ~ 0.15 .

TABLE 4-12
PERFORMANCE OF THE REFERENCE WATER COOLED, ZIRCALOY CLAD ThO_2 BLANKET CONCEPT
IN THE DTHR AS A FUNCTION OF EXPOSURE TIME

	AT BOL	AFTER 1 YEAR	AFTER 2 YEARS	AFTER 3 YEARS
INTEGRATED WALL LOAD ($\text{MW}\cdot\text{yr}\cdot\text{m}^{-2}$)	0.0	0.4	0.8	1.2
F , $\frac{\text{NET NO. OF } ^{233}\text{U ATOMS PRODUCED}}{\text{INCIDENT FUSION NEUTRON}}$	1.01	0.94	0.87	0.78
F in 1 st ZONE (10 cm)	0.62	0.53	0.46	0.39
F in 2 nd ZONE (15 cm)	0.39	0.41	0.41	0.39
f^{Th} , $\frac{\text{NO. Th FISSION REACTIONS}}{\text{INCIDENT FUSION NEUTRON}}$	0.056	0.060	0.064	0.069
f^{U} , $\frac{\text{NO. } ^{233}\text{U FISSION REACTIONS}}{\text{INCIDENT FUSION NEUTRON}}$	0.0	0.174	0.367	0.582
M , ENERGY MULTIPLICATION FACTOR	1.798	4.316	7.115	10.24
ℓ , $\frac{\text{BACK BLANKET NEUTRON LEAKAGE}}{\text{INCIDENT FUSION NEUTRON}}$	0.12	0.16	0.22	0.28
ϵ_1 , AVERAGE ^{233}U CONCENTRATION IN 1 st ZONE	0.0%	0.30%	0.56%	0.78%
ϵ_2 , AVERAGE ^{233}U CONCENTRATION IN 2 nd ZONE	0.0%	0.19%	0.38%	0.57%
k_{eff} , BLANKET EIGENVALUE	NC*	NC*	NC*	0.49
P_T , TOTAL THERMAL POWER (MWt)	1229	1690	2202	2774
G , INSTANTANEOUS ^{233}U PRODUCTION RATE (kg/yr)	201	187	173	155
CUMULATIVE ^{233}U PRODUCTION (kg)	0	194	374	538

*NOT CALCULATED

As shown in Table 4-12, the back 15 cm blanket zone contributes about 39% of the total ^{233}U production at BOL but this increases with exposure to 50% after 3 years. Note that the net F in the second zone stays about constant with exposure, and most of the decrease in F comes only from the first zone. This is also reflected in ϵ , the ^{233}U concentration in ^{232}Th , which increases linearly with time in the second zone to 0.57% after 3 years, while the increase in ϵ in the first zone is less than linear to 0.78% after 3 years.

The reason for this variation between the two zones is currently under investigation. One area of interest which may provide some insight is the neutron absorption in ^{232}Th and ^{233}U by energy range in the two blanket zones. This breakdown is shown in Table 4-13 for the reference blanket concept after 2 years (0.8 MW-yr-m^{-2}) of exposure in the DTHR. As shown in this table, about 50% of the ^{232}Th capture reactions in the first blanket zone occur from neutrons with energies less than 0.4 eV, while about 43% result from captures in the energy range from 10 eV to 1 MeV. However, in the second zone about 75% of the ^{232}Th captures occur for neutrons below 0.4 eV while about 21% are from the range 10eV - 1 MeV. This is because the second blanket zone has a softer neutron spectrum than the first zone due to the higher water-to-fuel volume ratio, as shown in Table 4-11. This spectrum difference between the two blanket zones is also apparent from the ^{233}U absorption reactions - about 60% occur from neutrons below 0.4 eV in the first zone while 80% are from this range in the second zone.

This breakdown by energy range of the blanket absorption reactions is useful ... attempting to assess the effect of incorporating some Li bearing material in the blanket zones. The use of Li might be helpful in decreasing the ^{233}U burn out and thus increasing the relative fissile production rate, as well as preventing blanket power increases and also breeding some tritium. However, as Table 4-13 shows, a large fraction (0.50 - 0.75) of the ^{232}Th capture reactions occur from "thermal" neutrons. Hence, the use of some Li would most likely also reduce the fissile production. An ANISN run with some Li was made to test this possibility, and the ^{233}U production was found to be smaller. It may be possible to avoid this situation by tailoring the spatial distribution of the Li bearing material in the blanket; however, this would be an area for future analysis using optimization techniques.

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TABLE 4-13
 VARIATION OF ^{232}Th CAPTURE AND ^{233}U ABSORPTION REACTIONS
 BY NEUTRON ENERGY RANGE AND ZONE IN THE DTHR REFERENCE BLANKET CONCEPT
 AFTER 2 YEARS (0.8 MW yr m^{-2}) OF EXPOSURE

NEUTRON ENERGY RANGE	$^{232}\text{Th}(n,\gamma)$ REACTIONS PER INCIDENT FUSION NEUTRON		^{233}U ABSORPTION REACTIONS PER INCIDENT FUSION NEUTRON	
	1 st ZONE	2 nd ZONE	1 st ZONE	2 nd ZONE
1.0 MeV - 14.1 MeV	0.011	0.003	0.003	0.001
5.5 keV - 1.0 MeV	0.106	0.034	0.005	0.001
10.7 eV - 5.5 keV	0.206	0.087	0.029	0.008
0.4 eV - 10.7 eV	0.033	0.017	0.066	0.023
<0.4 eV	0.362	0.431	0.158	0.128
TOTAL	0.718	0.572	0.261	0.161

The increase in the ^{233}U fissioning in the blanket causes a rise in the multiplication M and total plant thermal power P_T during exposure, as shown in Table 4-12. In calculating P_T for the DTHR, there is a constant contribution of 1083 MWt from the plasma power plus non-fission nuclear reactions in the blanket and shield ($h = 1.14$). Hence the power from fission reactions in the blanket increases from $1229 - 1083 = 146$ MWt at BOL to $2774 - 1083 = 1691$ MWt after 3 years, for greater than an order of magnitude increase. Note, however, that the blanket is still far from critical ($k_{\text{eff}} = 0.49$) even after 3 years when the average ^{233}U concentration is 0.78% in the first zone and 0.57% in the second zone. The blanket eigenvalue is very low due to a number of factors, including the thinness of the blanket, the low ^{233}U concentration, the shield region directly behind the blanket, and the fact that the blanket is only located on the outer part of the torus.

In Figure 4-18 the spatial variation of the blanket fission power density is plotted for BOL and after exposures of one, two, and three years in the DTHR with a wall loading of 2 MW m^{-2} . The fall-off with distance is nearly exponential at BOL because the fissions are all coming from fast neutron interactions with the ^{232}Th . However, as the power density increases due to thermal fissions in ^{233}U the spatial distributions become flatter due to the peaking of the thermal flux near the middle of the blanket. Also, the peak-to-average power density ratio in the first and second zones decreases from 1.80 - 1.98 at BOL to 1.17 - 1.48 after 3 years. Both the power leveling and lower peak-to-average ratio are desirable from the standpoint of blanket cooling, as discussed in subsection 4.1.4.

The blanket leakage λ out the back of the blanket zone more than doubles from BOL to 3 years exposure, as shown in Table 4-12. Comparing λ to the net fissile production per neutron F , one can see that increases of 10 - 20% in the fissile production would be possible if all the leakage could be prevented, e.g. by the use of a reflector or a thicker blanket module. A thicker blanket would have the disadvantage of increasing the ThO_2 inventory and decreasing the ^{233}U concentration. However, the use of a reflector could be advantageous, and will be investigated in future work on the DTHR blanket nuclear and mechanical design.

Figure 4-18

Radial Distribution of the Blanket Power Density in the DTHR For Different Exposure Times.

4.1.5.4 VARIATION OF SECOND BLANKET ZONE COMPOSITION

In this subsection the effect of varying the water-to-fuel volume ratio in the second zone of the reference DTHR blanket concept is presented and discussed. Two other second zone compositions were investigated for the reference blanket discussed in the previous subsection and shown in Table 4-11. The first had a lattice pitch of 1.60 cm and a water-to-fuel volume ratio of 0.423, i.e., the blanket composition was the same throughout both zones at the largest ThO_2 volume fraction possible under the assumed conditions on fuel rod size. The second composition analyzed had a pitch of 2.25 cm and a water-to-fuel volume ratio of 2.045, i.e. a much looser and wetter lattice was used in the second zone. For these three variations of the second zone composition, the first zone with 10 cm thickness was held fixed at the highest ThO_2 loading (lowest water-to-fuel volume ratio) in order to maximize the neutron multiplying reactions in the Th. The second zone thickness was kept the same at 15 cm.

The results of the neutronics calculations at BOL for the different second zone compositions are presented in Table 4-14. It can be seen that the total ^{233}U production, measured by F , varies by less than 6% among the cases. This is because most of the neutron multiplication occurs in the first 10 cm of the blanket, which has the same composition for all the cases. After ~ 10 cm of blanket thickness the neutrons are nearly all moderated below the energies needed to produce $(n, 2n)$, $(n, 3n)$, and fission reactions in Th. Hence the second zone mostly serves to further attenuate the neutrons leaking from the first zone and to cause them to be captured in that zone. Note that for the three cases the second zone provides 37% to 39% of the total fissile ^{233}U production. Also, there is not much difference in the Th fissions and blanket multiplication M since most of the fissions occur in the first zone.

The neutron leakage λ out the back of the blanket decreases with increasing water-to-fuel volume ratio due to the enhanced moderation with the increased water content. However, the parasitic absorption losses in the water are also increased in this situation, and this off-sets the decrease in blanket leakage.

TABLE 4-14
NEUTRONIC PERFORMANCE CHARACTERISTICS AT BOL FOR
DIFFERENT COMPOSITIONS IN THE SECOND BLANKET ZONE

	SECOND ZONE LATTICE PITCH p^* .		
	$p = 1.60 \text{ cm}$	$p = 1.95 \text{ cm}$	$p = 2.25 \text{ cm}$
ThO_2 VOLUME FRACTION (2^{nd} ZONE)	0.569	0.386	0.290
WATER-TO-FUEL VOLUME RATIO (2^{nd} ZONE)	0.423	1.228	2.045
PITCH-TO-DIAMETER RATIO (2^{nd} ZONE)	1.10	1.35	1.55
F , $\frac{\text{NO. OF } ^{233}\text{U ATOMS PRODUCED}}{\text{INCIDENT FUSION NEUTRON}}$ (TOTAL)	1.033	1.011	0.974
F FROM FIRST ZONE ONLY	0.627	0.616	0.617
F FROM SECOND ZONE ONLY	0.406	0.395	0.357
f , $\frac{\text{NO. Th FISSION REACTIONS}}{\text{INCIDENT FUSION NEUTRONS}}$	0.061	0.056	0.054
M , ENERGY MULTIPLICATION FACTOR	1.865	1.798	1.762
λ , $\frac{\text{BACK BLANKET NEUTRON LEAKAGE}}{\text{INCIDENT FUSION NEUTRON}}$	0.158	0.125	0.117
RELATIVE ThO_2 INVENTORY (TOTAL)	1.24	1.00	0.87
ϵ_2 , AVERAGE ^{233}U CONCENTRATION IN Th IN SECOND ZONE AFTER 1 YEAR (0.4 MW-yr-m^{-2}) EXPOSURE	0.13%	0.19%	0.22%

*SECOND ZONE THICKNESS HELD FIXED AT 15 cm. FIRST ZONE (10 cm) COMPOSITION KEPT AT $p = 1.60 \text{ cm}$, 0.569 VOLUME FRACTION ThO_2 .

The data in Table 4-14 also shows the penalty paid in ThO_2 fuel inventory and in the ^{233}U concentration in Th when the lattice pitch is decreased in the second zone. For example, the blanket ThO_2 heavy metal inventory increases by 24% while the ^{233}U production increases by only 2% in going from $p = 1.95$ cm to $p = 1.60$ cm. Hence there can be significant savings in using different lattice pitch sizes in different zones of the blanket.

4.1.5.5 NEUTRONIC PERFORMANCE OF METALLIC Th FUELS

The previous calculations have been for ThO_2 fueled blankets. As discussed in subsection 4.1.3, another fuel under consideration for use in the DTHR is metallic Th with 5% - 10% zirconium alloy in co-extruded zircaloy rods. In this subsection the neutronic performance of this fuel is compared with the ThO_2 fuel.

The neutronic calculations have assumed a density for ThO_2 of 9.65 gm/cm^3 , which is $\sim 96\%$ of theoretical density. This then translates into an atomic Th density of:

$$n_{\text{Th}} = 9.65 \frac{\text{gms}}{\text{cm}^3} \times \frac{6.02 \times 10^{23} \text{ atoms}}{264 \text{ gms}} = 2.20 \times 10^{22} \frac{\text{atoms Th}}{\text{cm}^3}$$

The oxygen atomic density is then twice this value, or $4.4 \times 10^{22} \text{ atoms } ^{16}\text{O/cm}^3$.

For the Th metallic fuel with 5 weight percent zirconium alloy, with a density of 11.6 gms/cm^3 , the Th atomic density is:

$$n_{\text{Th}} = 11.6 \frac{\text{gms}}{\text{cm}^3} \times 0.95 \times \frac{6.02 \times 10^{23} \text{ atoms}}{232 \text{ gms}} = 2.86 \times 10^{22} \frac{\text{atoms Th}}{\text{cm}^3}$$

The Zr density is then

$$n_{\text{Zr}} = 11.6 \frac{\text{gms}}{\text{cm}^3} \times 0.05 \times \frac{6.02 \times 10^{23} \text{ atoms}}{91.2 \text{ gms}} = 3.83 \times 10^{21} \frac{\text{atoms Zr}}{\text{cm}^3}$$

These densities may be summarized as such:

	<u>ThO₂</u>	<u>Th(5% Zr)</u>	<u>RATIO, $\frac{\text{metal}}{\text{oxide}}$</u>
Bulk density ($\frac{\text{gms}}{\text{cm}^3}$)	9.65	11.6	1.20
Th atomic density (10^{22} atoms Th/cm ³)	2.20	2.86	1.30

Another neutronic advantage of the metallic fuel, besides its higher Th atom density, is the smaller concentration of a low Z isotope. The Zr density from the alloy is over 10 times smaller than the oxygen density from the ThO₂.

A comparison of the neutronic performance at BOL for these two fuels is shown in Table 4-15. The calculations were done for the same blanket thickness and zone compositions shown in Table 4-11. It can be seen that the ²³³U production, measured by F, is about 20% higher with the metallic alloy fuel. However, the concentration of ²³³U in Th is about the same or even slightly smaller (in the first zone) with the metal fuel due to the higher Th atom density.

The Th fission rate (f) is about 32% higher with the metal, due mostly to the higher Th atom density but also partly because of the smaller concentration of low Z isotopes. The parasitic absorptions in the metallic fuel blanket are also smaller because the high concentration of oxygen from the ThO₂ has been eliminated. The number of neutrons produced per fusion neutron by (n, 2n), (n, 3n), and fission reactions in Th is about 33% higher with the metal fuel. However, when these neutrons are added to the one neutron produced in the plasma per D-T reaction, the total number of neutrons which are available to be absorbed in the blanket is only about 10% larger with Th metal alloy. The ²³³U production is then about 20% higher with the metal because of the higher Th density and lower parasitic absorption rate.

4.1.5.6. DEPLETED UO₂ WATER COOLED BLANKET

For comparison with the Th-²³³U fuel cycle performance in the DTHR, a calculation was made of the performance of a UO₂, water cooled, zircaloy clad blanket. The uranium was assumed to be depleted, with tails concentration of 0.3% ²³⁵U.

TABLE 4-15

COMPARISON OF THE NEUTRONIC PERFORMANCE AT BOL FOR Th(5% Zr)
METALLIC ALLOY AND ThO₂ BLANKET FUELS. BOTH USE WATER COOLING,
ZIRCALOY CLAD RODS WITH THE SAME BLANKET THICKNESS AND ZONE VOLUME FRACTIONS

	Th(5% Zr)	ThO ₂
F, <u>NO. OF ²³³U ATOMS PRODUCED</u> <u>INCIDENT FUSION NEUTRON</u>	<u>1.21</u>	<u>1.01</u>
f, <u>NO. OF Th FISSION REACTIONS</u> <u>INCIDENT FUSION NEUTRON</u>	0.074	0.056
M, ENERGY MULTIPLICATION FACTOR	2.056	1.798
ℓ, <u>BACK BLANKET NEUTRON LEAKAGE</u> <u>INCIDENT FUSION NEUTRON</u>	0.14	0.12
ε, AVERAGE ²³³ U CONCENTRATION IN Th AFTER 1 YEAR (0.4 MW-yr-m ⁻²) EXPOSURE		
FIRST ZONE	0.27%	0.30%
SECOND ZONE	0.19%	0.19%
RELATIVE HEAVY METAL BLANKET INVENTORY	1.20	1.00

The same zone compositions as shown in Table 4-11 were used in the calculation, with UO_2 substituted for ThO_2 . The blanket thickness was increased to a total of 35 cm by making the second zone 25 cm thick. This was done to keep the neutron leakage out the back of the blanket about the same for both the UO_2 and ThO_2 , and was necessary due to the better neutron multiplying properties of uranium relative to thorium.

The comparison of the UO_2 and ThO_2 blanket performance at BOL in the DTHR is shown in Table 4-16. The fissile fuel production rate is about 67% higher and the power production in the DTHR is about 2.5 times higher for the UO_2 blanket. These results for a water cooled UO_2 blanket are consistent with those of Greenspan (4-3), who used a different blanket composition and thickness and mainly considered natural (0.7% ^{235}U) uranium.

The reason for the increased fuel and power production with the UO_2 blanket is the enhanced neutron multiplication with the uranium. For example, the fission reaction rate in ^{238}U is nearly 5 times larger than the fission rate in the ^{232}Th . Both ^{238}U and ^{232}Th only fission for high neutron energies, but the fission cross section for ^{238}U is larger. Another large source of energy and neutrons comes from fission reactions in the ^{235}U atoms due to the large thermal neutron flux in the blanket. There is no fissile material present in the ThO_2 blanket at BOL, so its energy multiplication factor is much lower. As Table 4-16 shows, there is a fairly large destruction rate of ^{235}U . If this is considered to be part of the blanket fissile material composition change, then it must be subtracted from the ^{239}Pu production. In this case, the net fissile production rate for the UO_2 blanket would then be only 211 kg fissile/year, not very different from the ThO_2 blanket production rate. However, the UO_2 blanket is essentially burning otherwise unusable uranium tailings to make fissile ^{239}Pu , so one can also argue that the ^{235}U burn-out should not be subtracted from the ^{239}Pu production when calculating the blanket fissile yield.

4.1.5.7 EFFECT OF A UO_2 MULTIPLIER ZONE

The previous subsection has shown the superior fuel production and power generation capabilities of water cooled UO_2 blankets relative to ThO_2 blankets. However, if the main goal of the blanket is to produce large quantities of ^{233}U ,

TABLE 4-16

COMPARISON OF ThO_2 AND DEPLETED UO_2 BLANKET PERFORMANCE
 AT BOL IN THE DTHR. BOTH ARE WATER COOLED WITH ZIRCALOY CLAD.
 URANIUM IS 0.3% ^{235}U .

	<u>UO_2</u>	<u>ThO_2</u>
TOTAL BLANKET THICKNESS (cm)	35	25
F , <u>NO. OF FISSILE ATOMS PRODUCED</u> INCIDENT FUSION NEUTRON	1.65 (^{239}Pu)	1.01 (^{233}U)
f_{Th} , <u>NO. OF Th FISSION REACTIONS</u> INCIDENT FUSION NEUTRON	—	0.056
f^{238} , <u>NO. OF ^{238}U FISSION REACTIONS</u> INCIDENT FUSION NEUTRON	0.269	—
f^{235} , <u>NO. OF ^{235}U FISSION REACTIONS</u> INCIDENT FUSION NEUTRON	0.517	—
M , ENERGY MULTIPLICATION FACTOR	12.1	1.80
ℓ , <u>BACK BLANKET NEUTRON LEAKAGE</u> INCIDENT FUSION ENUTRON	0.12	0.12
P_T , TOTAL THERMAL POWER (Mwt)	3120	1229
G , FISSILE FUEL PRODUCTION RATE	336 $\frac{\text{kg } ^{239}\text{Pu}}{\text{year}}$	201 $\frac{\text{kg } ^{233}\text{U}}{\text{year}}$
U^{235} DESTRUCTION RATE	125 $\frac{\text{kg } ^{235}\text{U}}{\text{year}}$	—

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it might still be possible to take advantage of the uranium neutron multiplying reactions by using a multiplier zone of UO_2 in front of a zone containing ThO_2 . In this way it might be possible to produce more ^{233}U than with a ThO_2 blanket only by achieving a high neutron leakage rate into the ThO_2 zone from the UO_2 zone. Previous calculations⁽⁴⁻¹⁶⁾ have indicated only very slight gains in ^{233}U production are possible using this technique in He cooled blankets, although there is an increase in the blanket power production and significant quantities of ^{239}Pu are produced.

In this subsection the effect of using a UO_2 multiplier zone is investigated. Two multiplier thicknesses, 5 cm and 10 cm, were compared with the reference ThO_2 blanket without a multiplier zone. The composition of the UO_2 zone was 56.9% (volume) UO_2 , 24.1% water, and 17.6% zircaloy clad plus structure. The uranium was depleted (0.3%) ^{235}U . The ThO_2 zones behind the multiplier were kept the same with a total thickness of 25 cm and with zone compositions as shown in Table 4-11.

The results of a 5 cm and 10 cm thick multiplier zone at BOL in the DTHR are compared to the case without a multiplier zone in Table 3-30. It can be seen that as the multiplier zone thickness increases, the ^{233}U production rate decreases, e.g. at 10 cm thickness the rate is $\sim 30\%$ smaller than without the multiplier. However, large amounts of ^{239}Pu are being produced from neutron captures in the multiplier zone, so that at 10 cm thickness the total fissile (^{239}Pu plus ^{233}U) production is $\sim 40\%$ larger than the ^{233}U production with no multiplier. Also, the multiplier zone provides increased power production due to the ^{235}U and ^{238}U fission reactions.

The reason the ^{233}U production decreases with increasing multiplier zone thickness is that the neutron leakage out of the UO_2 zone into the ThO_2 zone is decreasing. This is because of the excellent moderating properties of the water, which attenuates the neutrons and allows a large fraction of them to be captured in the UO_2 zone before they can leak into the ThO_2 zone. This effect could change during exposure of the blanket, when the ^{239}Pu begins to build-up and fission and cause more neutron leakage into the ThO_2 zone. The burn-up of such blankets has not been investigated, however.

TABLE 4-17

EFFECT OF A DEPLETED UO_2 MULTIPLIER ZONE ON THE BLANKET PERFORMANCE AT BOL IN A DTHR. THE ThO_2 ZONE IS KEPT AT THE SAME THICKNESS AND MATERIAL COMPOSITION

	UO_2 MULTIPLIER ZONE THICKNESS		
	0 cm	5 cm	10 cm
F^{Th} , $\frac{\text{NO. OF } ^{233}\text{U ATOMS PRODUCED}}{\text{INCIDENT FUSION NEUTRON}}$	1.01	0.89	0.70
F^{U} , $\frac{\text{NO. OF } ^{239}\text{Pu ATOMS PRODUCED}}{\text{INCIDENT FUSION NEUTRON}}$	—	0.33	0.72
f^{Th} , $\frac{\text{NO. OF Th FISSION REACTIONS}}{\text{INCIDENT FUSION NEUTRON}}$	0.056	0.028	0.016
f^{238} , $\frac{\text{NO. OF } ^{238}\text{U FISSION REACTIONS}}{\text{INCIDENT FUSION NEUTRON}}$	—	0.129	0.199
f^{235} , $\frac{\text{NO. OF } ^{235}\text{U FISSION REACTIONS}}{\text{INCIDENT FUSION NEUTRON}}$	—	0.074	0.164
M, ENERGY MULTIPLICATION FACTOR	1.80	4.28	6.38
ℓ , $\frac{\text{BACK BLANKET LEAKAGE}}{\text{INCIDENT FUSION NEUTRON}}$	0.12	0.08	0.05
P_T , TOTAL THERMAL POWER (MWt)	1229	1683	2067
G^{U} , ^{233}U PRODUCTION RATE $\left(\frac{\text{kg } ^{233}\text{U}}{\text{year}}\right)$	201	177	139
G^{Pu} , ^{239}Pu PRODUCTION RATE $\left(\frac{\text{kg } ^{239}\text{Pu}}{\text{year}}\right)$	—	67	146
^{235}U DESTRUCTION RATE $\left(\frac{\text{kg } ^{235}\text{U}}{\text{year}}\right)$	—	18	40

Another possible way to use UO_2 in a blanket to increase the ^{233}U production would be to mix UO_2 and ThO_2 in one zone instead of having a separate UO_2 only multiplier zone. This type of blanket arrangement has not been investigated at the present, but will be considered in future studies.

4.1.5.8 NEUTRONIC COMPARISON OF HELIUM AND WATER COOLANTS

In this subsection a comparison is made of the effects of water cooling vs. helium cooling on the ThO_2 blanket neutronic performance. The blanket composition used for this analysis was that shown in Table 4-11 with thickness of 25 cm, except that the water volume fraction was replaced with He. It is recognized that He coolant may not be compatible with zircaloy clad rods, and that a different blanket mechanical design would be required with He. However, the purpose of this analysis was to investigate the neutronics effects of He vs. water coolant, and thus a one-for-one replacement was most useful. In the water cooled blanket concept the first wall and the shield regions were also water cooled. For the He cooled blanket, the first wall and shield regions were assumed to be He cooled, so that the water was eliminated from all zones.

The comparison of the water and He cooled blanket performance at BOL is shown in Table 4-18. It can be seen that the ^{233}U production rate, as measured by F , is $\sim 50\%$ larger in the water cooled blanket. However, the leakage out of the back of the blanket zone is over five times larger for the He cooling. This is because the same blanket thickness (25 cm) has been used in both cases. The neutrons are greatly attenuated and the neutron spectrum is much softer with the water coolant, and hence the leakage is much less. If the blanket thickness for the He cooled case was increased so that the blanket leakage was decreased to the same value as for the water cooled case ($\lambda = 0.12$), then the approximate value of F would be about $0.66 + (0.66 - 0.12) = 1.20$. This is about 19% larger than the F value for the water cooled case, although to achieve it the blanket fuel inventory would have to be increased with a corresponding decrease in the ^{233}U concentration.

The number of fission reactions in the Th is about 37% larger with the He cooled blanket, as shown in Table 4-18. Also, the number of neutrons produced by $(n, 2n)$, $(n, 3n)$, and fission reactions in the Th nuclei is $\sim 37\%$ larger with

TABLE 4-18

COMPARISON OF WATER VS. He COOLED ThO_2 BLANKET CONCEPTS AT BOL.
 BOTH BLANKETS HAVE THE SAME FUEL AND STRUCTURE VOLUME FRACTIONS
 AND ZONE THICKNESSES.

	<u>WATER COOLED</u>	<u>He COOLED</u>
F, $\frac{\text{NO OF } ^{233}\text{U ATOMS PRODUCED}}{\text{INCIDENT FUSION NEUTRON}}$	1.01	0.66
f, $\frac{\text{NO. OF Th FISSION REACTIONS}}{\text{INCIDENT FUSION NEUTRON}}$	0.056	0.077
M, ENERGY MULTIPLICATION FACTOR	1.80	2.09
λ , $\frac{\text{BACK BLANKET NEUTRON LEAKAGE}}{\text{INCIDENT FUSION NEUTRON}}$	0.12	0.66

the He because of the harder neutron spectrum. As discussed in subsection 4.1.5.3, the net fissile production in the water cooled ThO_2 blanket decreases with exposure time since the ^{233}U begins to fission in the high thermal flux. This problem of the fissile burn-out would probably be greatly alleviated with the He cooling, since then the thermal neutron flux would be much smaller.

In summary, the most important neutronic trade-offs in He vs. water cooled ThO_2 blankets are:

- He offers slightly higher fuel production (up to $\sim 20\%$) at BOL, although with a thicker blanket;
- The thicker blanket required with He coolant results in a larger blanket fuel inventory and lower fissile ^{233}U concentration in Th;
- Water cooling allows thinner, more compact blankets which have a lower fuel inventory and higher fissile concentration;
- He coolant may alleviate the fissile burn-out and blanket power increase found for the water cooled blanket after extended exposure times.

Other possible blanket coolants such as heavy water, steam, or boiling water may provide an intermediate type neutron spectrum between the soft spectrum of the light water and hard spectrum of the He, and hence offer a compromise on the trade-offs discussed above. These coolants, and others, will be considered in future studies.

4.1.6 NUCLEAR PERFORMANCE POTENTIAL

The blanket performance of the DTHR is extrapolated to obtain an estimate of the fuel and power production in a commercial* tokamak hybrid reactor (CTHR). The basic blanket concept used in this extrapolation is the ThO_2 pellet, Zircaloy clad, water-cooled design for which the nuclear performance has been discussed in subsection 4.1.5 and is shown in Table 4-12.

An important aspect of the blanket performance in a CTHR will be the ability to breed tritium as well as fissile fuel. The blanket concepts discussed in subsection 4.1.5 for the DTHR were not concerned with tritium breeding. In the nuclear calculational model, shield regions containing borated water and stainless steel were used on the inner part of the torus and on the outer part of the torus behind the blanket zones. Since the ^{10}B isotope is similar to the ^6Li isotope in its nuclear characteristics (i.e., $\frac{1}{v}$ absorber with high thermal neutron absorption cross section), an estimate can be made of the tritium breeding potential in the DTHR blanket concepts by calculating the number of neutrons absorbed in the ^{10}B nuclei. An additional factor can then be used to estimate the tritium production from ^7Li nuclei, which is a threshold reaction that only occurs for neutron energies above ~ 3 MeV.

The nuclear calculations performed with the ANISN neutron transport code modeled a blanket/shield region on the outer part of the torus and a shield-only region on the inner part of the torus. Due to the toroidal geometry effect illustrated in Figure 4-4, this model resulted in 72% of the plasma fusion neutrons striking the outer part of the vacuum vessel while the other 28% struck the inner part of the vacuum vessel. This is equivalent to assuming a blanket poloidal wall coverage factor b_p (defined in subsection 4.1.5.1) of 0.72. In the ANISN calculations, even though only 0.28 fusion neutrons strike the inner shield region, the number of neutrons absorbed in the inner shield region per plasma source neutron can be larger than 0.28. This is because there can be

*The term "commercial" is used here to connote a deployable system. The manner of deployment has not been studied in depth. However, the hybrid fuel production facility may ultimately be owned and operated by the government as is the case with isotopic enrichment plants.

significant back-scatter and leakage from the blanket region on the outer part of the torus as a result of neutron multiplying reactions in the Th and neutron moderation by the water.

To extrapolate to a CTHR, the following values will be used as compared to the DTHR:

	<u>DTHR</u>	<u>CTHR</u>	<u>Ratio</u>
PA, Plant Availability Factor	0.40	0.70	1.75
DC, Plasma Duty Cycle	0.50	0.85	1.70
b_p , Poloidal Wall Coverage Factor	0.43	0.67	1.56
b_t , Toroidal Wall Coverage Factor	0.56	0.87	1.55

These produce a total upgrade factor from the DTHR to the CTHR of ~ 7.19 . The same plasma fusion power of 950 MW and wall loading of 2 MW m^{-2} is assumed for both the DTHR and the CTHR.

The assumption on b_p of 0.67 represents blanket coverage on the outer one-half of the vacuum vessel poloidal surface. The toroidal coverage b_t of 0.87 represents placing blanket modules in all areas except those necessary for the six neutral beam injectors and the bundle divertor chamber.

Substituting into the formulas developed in subsection 4.1.5.1 with the assumed CTHR parameters, the following expressions are obtained for the CTHR fuel and power production:

$$G = 1433 \times F \frac{\text{kg } ^{233}\text{U}}{\text{year}}$$

$$P_T = 1083 \times [1 + (M-1) \times 0.409] \text{ MWt}$$

where F is the net number of ^{233}U atoms produced per incident fusion neutron and M is the D-T energy multiplication factor.

For the ThO_2 blanket performance shown in Table 4-11, the ANISN calculations show that the number of neutrons absorbed in the ^{10}B nuclei in the inner and

outer shield regions increases from 0.43 at BOL to 0.64 after 1.2 MW-yr-m^{-2} exposure. Assuming a one-for-one trade of ^{10}B for ^6Li , these values give an approximate idea of the possible contribution to the tritium breeding ratio from ^6Li .

Benchmark calculations⁽⁴⁻¹⁷⁾ of tritium breeding blankets have indicated that per D-T fusion neutron incident on a Li bearing medium, about 0.4 tritium producing reactions in ^7Li can be achieved. For a CTHR, about $1 - b_p = 1 - 0.67 = 0.33$ of the D-T fusion neutrons strike the Li bearing inner shield region. Hence, an estimate of tritium production from ^7Li in the DTHR inner shield would be about $0.4 \times 0.33 = 0.13$. No credit is taken for tritium production from ^7Li in Li bearing regions behind the blanket on the outer part of the torus, since the neutron spectrum leaking into this zone is mostly below the threshold for the ^7Li reaction.

Hence, in a CTHR with Li bearing regions in the inner part of the torus and also in back of a 25 cm-thick water-cooled ThO_2 blanket, the approximate tritium breeding ratio (TBR) versus exposure is:

	<u>^6Li</u>	<u>^7Li</u>	<u>Total T.B.R.</u>
BOL	0.43	0.13	0.56
0.4 MW-yrs- m^{-2}	0.49	0.13	0.62
0.8 MW-yrs- m^{-2}	0.56	0.13	0.69
1.2 MW-yrs- m^{-2}	0.64	0.13	0.77

Hence, assuming a necessary tritium breeding ratio of ~ 1.1 , the ThO_2 blanket discussed in subsection 4.1.5.3 can produce from $\sim 50\%$ to $\sim 70\%$ of the tritium necessary without a blanket modification.

Using the F and M values given in Table 4-12, an estimate of the fuel and power production rates in a CTHR are:

	<u>Instantaneous ^{233}U Production Rate</u>	<u>Cumulative ^{233}U, kg</u>	<u>Thermal Power</u>
BOL	1447 kg $^{233}\text{U}/\text{yr}$	0	1436 MWt
0.4 MW-yrs-m ⁻²	1347	466	2552
0.8 MW-yrs-m ⁻²	1247	898	3792
1.2 MW-yrs-m ⁻²	1118	1292	5176

Note that the CTHR would achieve about 1.2 MW-yrs-m⁻² exposure during one year's operating time. Hence, after about one year in the CTHR, the net fissile production would be ~1300 kg of ^{233}U . The average concentration of the ^{233}U in ^{232}Th would be ~0.65%.

Depending upon the PWR fuel cycle assumed (see subsection 4.2), this fuel production in the CTHR translates into:

	<u>Chemically Separable Fuel Cycle</u>	<u>Denatured ^{233}U Fuel Cycle</u>
PWR ^{233}U make-up required* ($\frac{\text{kg } ^{233}\text{U}}{\text{GWe-yr}}$)	255	340
CTHR fuel production ($\frac{\text{kg } ^{233}\text{U}}{\text{yr}}$)	1300	1300
No. of 1 GWe PWR's supplied per year	5.1	3.8

*See subsection 4.2.

There exists the possibility of significant improvement in the CTHR fissile fuel production. A number of other possible blanket concepts have been discussed in subsection 4.1.2. In addition, neutronics calculations discussed in subsection 4.1.5 show improvements of ~ 20% are possible through the use of either Th metallic fuel or He blanket cooling.

If both the metallic fuel and He cooling were used, the CTHR fuel production would increase to $\sim 1.2 \times 1.2 \times 1300 \sim 1900$ kg ^{233}U per year. This would be sufficient to supply ~ 6-8 present day PWR's (1 GWe) per year, depending on the assumed fuel cycle. Tritium production would also increase due to the increased neutron multiplication in these blankets, so that tritium self-sufficiency could probably also be closely approached.

4.2 SYMBIOTIC HYBRID/FISSION REACTOR FUEL CYCLES

There are a large number of fuel cycle scenarios that could be used in a symbiotic hybrid reactor and fission reactor relationship. This section discusses the leading fuel cycle alternatives, the initial concepts used for a DTHR/PWR scenario, and the results of this fuel cycle on the PWR performance.

4.2.1 HYBRID FUEL CYCLE ALTERNATIVES

The two leading fuel cycle alternatives under consideration for hybrid reactors are the ^{238}U - ^{239}Pu cycle and the ^{232}Th - ^{233}U cycle. These two cycles can be combined in one blanket which produces both ^{239}Pu and ^{233}U . Another option would be to use fissile enrichment in the blanket as opposed to using only the fertile materials ^{238}U and Th in the initial load. In the latter case, the fissile material content may grow more rapidly as the blanket is irradiated.

For the single fuel cycle blankets with unenriched loads, the blanket would be made up of either natural uranium (or depleted uranium) or thorium. Previous neutronic studies have indicated the superior performance of ^{238}U relative to Th because of the high energy cross section effects. These studies indicate that for single region, fast spectrum blankets, the ^{238}U blanket will produce nearly three times more power and over 50% more fissile fuel than the Th blanket. However, the conversion ratio of fission reactors utilizing ^{233}U is generally higher than for those with ^{239}Pu , and hence an equivalent number of reactors may be able to be fueled by the hybrid for either fuel cycle. The choice of fuel material type is also important, since metal fuels outperform (neutronically) such ceramic fuels as oxides and carbides. However, the lower temperature and burnup operating levels of the metal fuels may eliminate them from consideration, at least for near-term applications.

A potentially attractive blanket composition for breeding ^{233}U involves using a ^{238}U bearing zone followed by a Th and moderator zone. This may provide better ^{233}U production rates than the Th-only blanket previously mentioned because the ^{238}U zone produces additional neutron multiplication. However, such a blanket will necessarily produce some ^{239}Pu from neutron capture in the ^{238}U and hence, is more complicated in that both fuel cycle technologies are involved. One variation of this blanket involves providing some initial enrichment in ^{239}Pu in the ^{238}U zone so as to nearly break even in the ^{239}Pu production and consumption rates.

Although such a blanket has the potential for high power and ^{233}U production rates, it introduces the complications of both fuel cycles plus ^{239}Pu enrichment.

There clearly are a number of possible blanket designs that can produce ^{239}Pu , ^{233}U , or both. The advantages and disadvantages of each blanket must be considered from a number of standpoints, such as:

- Proliferation Resistance
- Neutronic Performance
- Fission Reactor Market to be Supplied
- Safety Implications
- Environmental Considerations
- Economics

The initial phase of the hybrid reactor study has concentrated on the more proliferation resistant Th- ^{233}U fuel cycle.

Experience with fission reactor fuels is based predominantly on work with uranium oxide fuels, since both Light Water Reactors (LWR's) and Heavy Water Reactors (HWR's) use them. However, the ^{239}Pu produced in the initial load can be recycled in subsequent loads with uranium fuel. Since the conversion ratio for these reactors is low, the initial market for the hybrid reactor may be that in which the hybrid supplies ^{239}Pu make-up fuel for use in LWR's and HWR's.

In addition to this present-day fuel cycle strategy with Pu recycle, there are several alternative, more advanced, fuel cycle candidates which will be considered. An example is the "denatured" fuel cycle of $^{238}\text{U}/^{235}\text{U}/^{233}\text{U}$, which has the potential of being proliferation resistant. Here the hybrid reactor would supply the necessary amounts of ^{233}U from breeding with Th. Another fission reactor fuel cycle could be based on Th/ ^{233}U , with the hybrid supplying the make-up ^{233}U which is combined with recycled ^{233}U produced in the fission

reactor. The Th/Pu cycle can also be considered where the LWR, in effect, burns Pu and produces ^{233}U for use in other reactors with the hybrid supplying the Pu needed.

There are a myriad of other alternative advanced fuel cycles which can be considered for fission reactors. For the initial hybrid study, this number was limited to utilizing thorium fuel enriched with hybrid bred ^{233}U in a standard PWR using oxide fuel form. Other fuel cycles can be examined in conjunction with possible hybrid blanket designs to arrive at the most desirable scenario, once the results of the fuel cycle assessment from the NASAP program are available.

4.2.2 INITIAL DTHR FUEL CYCLE SCENARIO

The reference fuel cycle that has been investigated is shown schematically in Figure 4-19. The blanket about the fusion device consists of fertile material. Fresh thorium, ^{232}Th , would be used so that the fabrication operations could be performed essentially under "hands-on" conditions. The fissile fuel ^{233}U would be formed by the radiative capture of fusion neutrons in the ^{232}Th blanket (see Appendix D for the possible transmutations and decay chains). The uranium isotope mix (mostly ^{233}U) generated by the hybrid would be reprocessed and diluted with diffusion plant tailings (99.75% of ^{238}U) such that the ratio of fissile U/total U would be no more than 10% (for the studies reported here, 9.72%). The hybrid reactor and reprocessing plant would be located in a controlled area, and all shipped fuel would be useless for weapons without isotopic separation. This denatured fissile fuel would be used with the filler isotope ^{232}Th in the fabrication of fuel elements for PWRs. The fuel would be burned in PWRs for the purpose of generating electricity. The uranium and thorium would be recycled, and the Pu produced as a result of the presence of ^{238}U would be chemically separated and stored. Since uranium constitutes only $\sim 1/3$ of the heavy metal in the core, the amount of Pu produced is much less than for cores currently in operation. Because of the recycling of the irradiated fuel, the fabrication of the PWR fuel assemblies would be carried

out remotely. However, if as an alternate fuel cycle the thorium were not recycled, there would be a period of time following reprocessing where the radioactivity is greatly reduced. In this case, there is a possibility that total remote fabrication would not be required for the fuel assemblies. This possibility is discussed in Appendix D.

Another possible option would be the use in the LWRs of the fissile uranium as produced in the hybrid without denaturing. In this case, the fissile fuel could be chemically separated so that the weapons proliferation concerns must be satisfied solely by the difficulty of handling the fuel because of its radioactivity. However, as described in Appendix D, there is a time interval following chemical separation where the radioactivity is greatly reduced.

Calculations have been performed to estimate the equilibrium feed requirements in kg fissile ($^{233}\text{U} + ^{235}\text{U}$) per year necessary to sustain one standard 3817 MWt or 1276 MWe PWR in both the "denatured"* and "chemically separable"** scenarios for various batch burnups. The results have been normalized to a 1000 MWe PWR for some of the presentations. (It is specified where such extrapolations were carried out). These results are presented in subsection 4.2.3. A safety evaluation of the use of $\text{UO}_2 - \text{ThO}_2$ fuel in Westinghouse PWR's is presented in subsection 4.2.4.

* Diluted ^{233}U using ^{238}U
**No ^{238}U

4.2.3 PWR UTILIZATION OF ^{233}U GENERATED IN A HYBRID REACTOR

REFERENCE PWR FUEL

The PWR chosen for simulation in this study of ^{233}U utilization is the Westinghouse 414 3817 MWt plant. The reactor core has 193 fuel assemblies with 17 x 17 fuel pin arrays. The fuel pin pitch is on 0.496 inch squares and the assembly pitch 8.466 inch squares. The pins are 14 feet long and have an O.D. of 0.374 inch. A fuel pellet diameter of 0.3250 inch was used in this study. The Zircaloy clad has a thickness of 0.0225 inch. Other salient parameters necessary to describe the core physics are:

Full power average moderator temperature: 597° F

Grids: 100% Zircaloy, 0.5 cell moderator volume

Interassembly plus RCC* Thimble Volume: Water - 90.52%
Zircaloy - 8.83%
SS-304 - 0.65%

Operating pressure: 2250 psia

Ceramic as-fabricated fraction of theoretical density: 95%

Non-lattice (RCC gap) volume fraction: 9.38%

Flux peaking factor in non-lattice regions: 1.10

Geometric buckling: $2.07 \times 10^{-4} \text{ cm}^{-2}$

Average linear power: 5.32 kW/ft (17.44 kW/m)

Effective reactor flow: $4.00 \times 10^5 \text{ gpm}$

In the reference PWR equilibrium fuel cycle, uranium enriched to 3.2 a/o ^{235}U in total uranium is fed into the core in batches and burned over three years (at 80% load factor) to 33,000 MWd/T (megawatt days per tonne) average discharge burnup. The fuel resides near the core periphery for one year and is shuffled inward at the end of the first and second years so as to minimize power peaking and feed enrichment.

*Rod Cluster Control

Two important quantities that are often referred to in discussions of fuel cycles and core designs are the Conversion Ratio (CR) and the Fissile Inventory Ratio (FIR). The conversion ratio is defined as the ratio of the rate at which fissile nuclei are produced to the rate at which they are destroyed. It is calculated within the computer code by the algorithm:

$$\frac{(\text{Captures in } ^{232}\text{Th} - ^{233}\text{Pa} + ^{234}\text{U} + ^{238}\text{U} + ^{240}\text{Pu})}{(\text{absorptions in } ^{233}\text{U} + ^{235}\text{U} + ^{239}\text{Pu} + ^{241}\text{Pu})} \quad (4.2-1)$$

An average value of the conversion ratio, $\overline{\text{CR}}$, can be expressed in terms of integral quantities over the burnup as:

$$\overline{\text{CR}} \equiv \text{Fissile Nuclei Produced/Fissile Nuclei Destroyed} \quad (4.2-2)$$

The Fissile Inventory Ratio is defined as the ratio of fissile mass at the end-of-life to the fissile mass at the beginning-of-life; i.e.,

$$\text{FIR} \equiv \text{EOL Fissile Mass/BOL Fissile Mass} \quad (4.2-3)$$

As an example, the conversion ratio in the reference PWR equilibrium cycle for the U/ ^{235}U fuel cycle varies with burnup as shown in Table 4-19. The average conversion ratio is $\overline{\text{CR}} \approx 0.60$, and the fissile inventory ratio is $\text{FIR} = 0.47$.

TABLE 4-19
CONVERSION RATIO AS A FUNCTION OF BURNUP

<u>Burnup (Mwd/T)</u>	<u>Conversion Ratio</u>	<u>^{238}U Captures/ Fissile Absorption</u>
BOL	0.46	0.46
5,000	0.49	0.47
10,000	0.55	0.50
15,000	0.60	0.51
20,000	0.64	0.53
25,000	0.67	0.54
33,000	0.74	0.58

DESCRIPTION OF CALCULATIONS

All calculations were made with standard Westinghouse core analysis computer codes. Each depletion was performed such that the batch was considered to be two-thirds burned (end of second year in core) when the boron-free eigenvalue reached 1.005. At equilibrium, this method of burning a batch is nearly equivalent to performing repeated detailed core shufflings for a three-zone core. Criticality over the batch lifetime was maintained by adjusting the leakage. The boron concentration was held constant for the various problems as follows:

<u>Denatured Fuel</u>		<u>Chemically Separable Fuel</u>	
<u>(10³ Burnup MWd/tonne)</u>	<u>Boron Concentration (B - 10 ppm)</u>	<u>(10³ Burnup MWd/tonne)</u>	<u>Boron Concentration (B - 10 ppm)</u>
13.7	400	17.8	350
20.8	450	25.0	400
27.2	500	31.5	450
33.4	550	37.7	500
39.4	600	43.6	550
45.1	650	49.1	600

(These are only rough estimates of cycle-average boron concentration.)

For a typical case (of the twelve specified above), the hybrid uranium was assumed to have the following isotopic distribution (ignoring the ²³²U):

U-233 = 99.91 a/o
U-234 = 0.091 a/o
U-235 = 0.00026 a/o
U-236 = (negligible)

(Recent burnup calculations indicate that for the reference water-cooled blanket, the hybrid produced uranium would consist of ≈94% of ²³³U. Studies are required to determine the effects of varying the hybrid produced uranium isotopic composition.)

Sufficient diffusion plant tailings (99.75 a/o ^{238}U , 0.25 a/o ^{235}U) were added (in the "denatured" problems) to cut the fraction of fissile U in total U to 0.0972 a/o. The bulk of the fuel pellet ceramic was taken to be ^{232}Th . The initial enrichments were chosen to provide a spectrum of burnups over the range 15,000 - 50,000 Mwd/T. When the fuel was recycled (as batches 2, 3 and 4), fissile makeup was added to achieve the same burnup reached in batch #1.

The ceramic volume loss due to chipping was assumed to be 1% and the reprocessing efficiency times the fabrication efficiency $0.99 \times 0.99 = 0.98$.

Three recyclings were performed in each of the twelve cases (six denatured cases at various average discharge burnups, six chemically separable cases at various burnups). Iteration upon the burnup was carried on until all batches were within ± 100 Mwd/T of the first batch burnup.

RESULTS AND CONCLUSIONS

The analyses have been directed at obtaining the isotopic changes, enrichment, fissile inventory (without recycling any plutonium in the denatured case), mass flow, and yearly production of uranium required of the hybrid reactor to support a standard PWR.

Examples of the isotopic changes are given in Tables 4-20 and 4-21 for cores fueled with thorium enriched with chemically separable ^{233}U and denatured ^{233}U , respectively. These two examples most closely represent the reference equilibrium PWR fuel cycle. Results for the first batch of fuel are only accurate to within about 10% due to the approximate method of representing fuel shuffling (which represents only an equilibrium cycle closely). When batch #4 is burned (by which time the precise isotopic distribution of the initial fuel loading has also tended to lose its influence), equilibrium in fissile makeup requirements is being approached. Note in Table 4-21 that Pu isotopes are generated as a result of the ^{238}U used in denaturing the ^{233}U . It can be seen that although the production of plutonium is calculated for each batch, it is not recycled; i.e., there is no plutonium at the beginning-of-life for any batch. This imposes a substantial penalty on the denatured cycle in terms of the makeup fuel requirements.

TABLE 4-20
CHANGE IN ISOTOPIC COMPOSITIONS FOR THE FIRST FOUR BATCHES OF FUEL
(Th + CHEMICALLY SEPARABLE ^{233}U)
FOR BURNUP OF 31.5 Gwd/T; $k = 1.005$ AT 2/3 LIFE OR 21.0 Gwd/T

ISOTOPE	BATCH 1		BATCH 2		BATCH 3		BATCH 4	
	T-BOL [*]	$\Delta\text{kg/T}^{\dagger}$	T-BOL	$\Delta\text{kg/T}$	T-BOL	$\Delta\text{kg/T}$	T-BOL	$\Delta\text{kg/T}$
Th-232	92.59		92.18		91.89		91.67	-31.5
Pa-233	-	1.44		1.39		1.36		1.34
U-233	2.78	-11.86	2.77	-11.53	2.75	-11.19	2.74	-10.98
U-234	2.55×10^{-3}	3.74	0.35	2.50	0.58	1.78	0.74	1.32
U-235	7.5×10^{-6}	0.60	0.06	0.69	0.12	0.52	0.17	0.38
U-236	-	0.07	0.01	0.23	0.03	0.32	0.06	0.35
TOTAL	95.37		95.37		95.37		95.37	

^{*}Metric Tonnes of isotope at beginning-of-life

[†]Change in mass of isotope per unit heavy metal mass in the core between the beginning-of-life and end-of-life of a batch

TABLE 4-21
CHANGE IN ISOTOPIC COMPOSITIONS FOR THE FIRST FOUR BATCHES OF FUEL
(Th + DENATURED ²³³U)

FOR BURNUP OF 33.3 GWd/T; k = 1.005 at 2/3 LIFE OR 22.2 GWd/T.

ISOTOPE	BATCH 1		BATCH 2		BATCH 3		BATCH 4	
	T-BOL*	Δkg/T [†]	T-BOL	Δkg/T	T-BOL	Δkg/T	T-BOL	Δkg/T
Th-232	65.24		64.66		64.04		63.53	
Pa-233	-	0.84	3.11	0.82		0.79		0.78
U-233	3.06	-13.93		-14.08	3.13	-13.99	3.14	-13.90
U-234	2.80×10^{-3}	3.15	0.31	2.24	0.52	1.68	0.67	1.30
U-235	7.36×10^{-2}	0.01	0.08	0.48	0.13	0.45	0.17	0.36
U-236	-	0.14	0.01	0.18	0.03	0.24	0.05	0.28
U-238	29.70	-11.92	29.96	-11.89	30.33	-11.92	30.66	-11.95
Pu-239	-	3.11		3.20		3.29		3.35
Pu-240	-	1.01		1.01		1.02		1.02
Pu-241	-	0.76		0.77		0.77		0.78
Pu-242	-	0.24		0.23		0.22		0.22
TOTAL	98.08		98.13		98.18		98.22	

* Metric Tonnes of isotope at beginning-of-life

[†] Change in mass of isotope per unit heavy metal mass in the core between the beginning-of-life and end-of-life of a batch

The enrichment increases from batch to batch as ^{234}U and ^{236}U build up. The initial feed from the hybrid has been assumed to be almost all ^{233}U based on some early studies of helium-cooled carbide fuel blankets. However, in the PWR, the presence of hydrogen causes neutrons born in fission to downscatter past the ^{234}U resonances. Consequently, ^{234}U (and hence, ^{236}U) build up in the fuel from batch to batch. Since ^{236}U is a dead end (and a parasitic absorber), it will accumulate monotonically in the reprocessed uranium. Therefore, true equilibrium will only be approached, but never actually reached.

The conversion ratios (see Equations 4.2-1 and 4.2-2) are shown in Figure 4-20 as a function of burnup for a standard PWR fueled with uranium enriched with ^{235}U (current fuel), thorium enriched with chemically separable ^{233}U , and thorium enriched with denatured ^{233}U . The conversion ratio increases with burnup because the relative decrease in fissile material is much larger than the relative decrease in fertile material. Therefore, for a constant power, the fissile isotope destruction rate remains fairly constant, but the fissile production rate increases. Because of this same effect, it would be expected that the magnitude of the rate of decrease of the Fissile-Inventory-Ratio with burnup should decrease. This effect can be seen in Figures 4-21 and 4-22. Returning to Figure 4-20, the $\overline{\text{CR}}$ for a core loaded with $^{238}\text{U}/^{235}\text{U}$ fuel is smaller in general than the others because the ratio, α , of thermal capture cross section to thermal fission cross section is approximately twice as large for the fissile fuel ^{235}U as it is for ^{233}U . The thermal fission cross sections are also comparable. Therefore, for a given power level or fission rate and a thermal neutron spectra, the fissile destruction rate is larger. The same effect occurs in the resonance range only this time due to ^{233}U having a larger fission resonance integral; the capture resonance integrals for ^{233}U and ^{235}U are comparable. Furthermore, the thermal radiative capture cross section of the fertile ^{232}Th is 2.7 times larger than that of ^{238}U . There is a compensating effect; the infinitely dilute resonance integral for ^{238}U is 3.2 times larger than that for ^{232}Th . However, because of the large self-shielding effects of the ^{238}U , the ratio of the effective resonance integrals is expected to be less than 3.2.

Figure 4-20 Conversion Ratio as a Function of Burnup in a PWR

Figure 4-21 Fissile Inventory Ratio As A Function Of Burnup In A PWR
Fueled With Th/²³³U (Chemically Separable)

Figure 4-22. Fissile Inventory Ratio As A Function of Burnup, In A PWR Fueled With Th/²³³U (Denatured).

The conversion ratio for a core fueled with thorium enriched with denatured ^{233}U is larger than that for thorium enriched with chemically separable ^{233}U for burnups less than ~ 33 GWd/T. Consider first the times near zero burnup. In this case, both cores are fueled predominantly with ^{233}U . The slightly larger quantity of ^{235}U in the denatured fuel (in either case there is only a small amount of ^{235}U ; several percent of total fissile) would tend to make the $\text{Th}/^{235}\text{U}$ curve fall below the $\text{Th}/^{233}\text{U}$ curve. Furthermore, the average thermal radiative capture cross section for the fertile isotopes is smaller for the denatured fuel because of the percent of ^{238}U ($\sim 30\%$ of fertile material). On the other hand, the capture resonance integral for ^{238}U is larger than that for thorium. Another positive effect is the larger fast fission cross section of ^{238}U relative to ^{232}Th . Because of the higher occurrence of fissions of the fertile material, fewer fissions of the fissile fuel are required for a given power level and, hence, the conversion ratio is higher. However, with increased burnup, the amount of fissile plutonium in the denatured core increases and because of the large ratio of thermal radiative capture cross section to thermal fission cross section ($\alpha = 0.36$) for plutonium, the conversion ratio does not increase as rapidly with burnup as for the core fueled with chemically separable ^{233}U . At 33 GWd/T, the conversion ratios for these two cores are equal.

The fissile inventory ratios (FIR) shown in Figures 4-21 and 4-22 decrease with burnup because the conversion ratios are less than one. For the denatured case, the FIR is shown when the plutonium fissile isotopes are included and are not included. The FIR with the plutonium isotopes included is of interest because a portion of the plutonium is burned during the lifetime of the fuel batch and it is of interest to know the amount of plutonium that must be stored after fuel reprocessing. The FIR not including plutonium is of interest because the plutonium was not considered to be recycled for the reference fuel cycle. The FIR for the denatured-fuel core when including the fissile plutonium is larger than the FIR for the chemically separable fuel core at the same burnup because the conversion ratio is higher as discussed above.

A summary of important parameters which characterize the PWR core performance are given in Table 4-22. The creation and burnup of fissile mass, which are

TABLE 4-22
KEY PARAMETERS CHARACTERIZING THE PWR CORE PERFORMANCE
FOR A BURNUP OF 33 Gwd/T

Th/CHEMICALLY SEPARABLE ^{233}U FUEL CYCLE

$$\overline{\text{CR}} = 0.74$$

$$\text{FIR} = 0.68$$

Th/DENATURED ^{233}U FUEL CYCLE

$$\overline{\text{CR}} = 0.47 \text{ (0.78 INCLUDING Pu)}$$

$$\text{FIR} = 0.62 \text{ (0.745 INCLUDING Pu)}$$

U/ ^{235}U CURRENT FUEL CYCLE

$$\overline{\text{CR}} = 0.60$$

$$\text{FIR} = 0.26 \text{ (0.47 INCLUDING Pu)}$$

shown in Tables 4-23, 4-24 and 4-25 for the $\text{Th}/^{233}\text{U}$ (chemically separable), $\text{Th}/^{233}\text{U}$ (denatured) and $\text{U}/^{235}\text{U}$ fuel cycles respectively, were obtained from these parameters by assuming that to a good approximation, the average conversion ratio equals the ratio of the integral fissile mass produced to that destroyed. The initial fissile concentration for the $\text{Th}/^{233}\text{U}$ (chemically separable) fuel is 5% less than the $\text{U}/^{235}\text{U}$ fuel, and for the $\text{Th}/^{233}\text{U}$ (denatured) fuel is 5% greater than the $\text{U}/^{235}\text{U}$ fuel. At the end-of-life, following a burnup of 33 Gwd/T, the fissile concentration is higher for the $\text{Th}/^{233}\text{U}$ fuels because the conversion ratios are larger.

The required amounts of makeup fuel required for the three fuel cycles under study are given in Table 4-26. In each case, the required makeup fuel has been normalized for a 1 GWe PWR. It has been assumed that plutonium will not be recycled, so the makeup fuel required for the $\text{Th}/^{233}\text{U}$ (denatured) fuel cycle is larger than that required for the $\text{Th}/^{233}\text{U}$ (chemically separable) fuel. However, if it were assumed that plutonium would be recycled and if a fissile plutonium nucleus were assumed to be equivalent to a fissile uranium nucleus, the required makeup fissile fuel would be approximately the same for these two fuel cycles. As can also be seen in Table 4-26, the makeup fuel required for the $\text{U}/^{235}\text{U}$ fuel cycle is appreciably higher than that required for the $\text{Th}/^{233}\text{U}$ fuel cycles. Figures 4-23 and 4-24 show the fissile fuel flows for the $\text{Th}/^{233}\text{U}$ cores schematically.

The feed requirement of 255 kg/year of ^{233}U for the $\text{Th}/^{233}\text{U}$ (chemically separable) fuel can be compared to the value given by Karl Cohen⁽⁴⁻¹⁸⁾. "A 1000 MWe PWR using ^{233}U and thorium in its standard fuel geometry, which is obviously not optimized for the thorium cycle, calls for about 210 kg/year of fissile makeup." The particulars of the lattice and the burnup which Cohen had in mind are not given, but they are no doubt similar to the ones assumed in this study.

Another quantity of interest is the "apparent fissile consumption," AFC, defined as the ratio of the net fissile material consumed during fuel exposure to the total thermal energy generated during fuel exposure. As an example, the AFC for the $\text{Th}/^{233}\text{U}$ (chemically separable) fuel is:

TABLE 4-23
STANDARD PWR OPERATION WITH Th AND CHEMICALLY SEPARABLE ^{233}U FUEL CYCLE

BURNUP = 33 Gwd/T

TONNES OF FISSIONABLE MATERIAL:

BOL	2.8690	$\left(\frac{\text{FISSIONABLE MASS}}{\text{HM MASS}} = 3.01\% \right)$
CREATED	<u>+2.6133</u>	
TOTAL	5.4823	
DESTROYED	<u>-3.5314</u>	$\left(\text{MASS FIS}^{\dagger} = 3.207; \frac{\text{MASS CAP}^{\Delta}}{\text{MASS FIS}} = 0.10 \right)$
EOL	1.9509	$\left(\frac{\text{FISSIONABLE MASS}}{\text{BOL HM MASS}} = 2.05\% \right)$

† Mass of Fissionable Material Fissioned

$^{\Delta}$ Mass of Fissionable Material Destroyed by Neutron Capture

TABLE 4-24
STANDARD PWR OPERATION WITH Th AND DENATURED ^{233}U FUEL CYCLE

BURNUP = 33 GWd/T, NO Pu RECYCLE

TONNES OF FISSILE MATERIAL:

BOL	3.2551	$\left(\frac{\text{FISSILE MASS}}{\text{HM MASS}} = 3.32\% \right)$
CREATED	+ 2.9429	
	(U) <u>+ (1.7733)*</u>	
TOTAL	6.1980	
	(U) (5.0284)	
DESTROYED	- 3.7730	$\left(\text{MASS FIS}^{\dagger} = 3.315; \frac{\text{MASS CAP}^{\Delta}}{\text{MASS FIS}} = 0.14 \right)$
	(U) -(3.0102)	
EOL	2.4250	$\left(\frac{\text{FIS MASS}}{\text{BOL HM MASS}} = 2.47\% \right)$
	(U) (2.0182)	$\left(\frac{\text{FIS U-MASS}}{\text{BOL HM MASS}} = 2.06\% \right)$

* Uranium Fissile Mass Created Only

† Mass of Fissile Material Fissioned

$^{\Delta}$ Mass of Fissile Material Destroyed by Neutron Capture

TABLE 4-25

STANDARD PWR OPERATION WITH CURRENT U/²³⁵U FUEL CYCLE

BURNUP = 33 Gwd/T, NO Pu OR U RECYCLE

TONNES OF FISSIONABLE MATERIAL:

BOL	(U)	3.2700	$\left(\frac{\text{FISSILE MASS}}{\text{HM MASS}} = 3.17\% \right)$
CREATED	(Pu) +	<u>2.5997</u>	
TOTAL		5.3697	
	(U)	(3.2700)	
DESTROYED	-	4.3328	$\left(\text{MASS FIS}^{\dagger} = 3.513; \frac{\text{MASS CAP}^{\Delta}}{\text{MASS FIS}} = 0.23 \right)$
	(U)	-(2.4198)	
EOL		1.5369	$\left(\frac{\text{FIS MASS}}{\text{BOL HM MASS}} = 1.49\% \right)$
	(U)	(0.8502)	$\left(\frac{\text{FIS U-MASS}}{\text{BOL HM MASS}} = 0.82\% \right)$

[†]Mass of Fissile Material Fissioned^ΔMass of Fissile Material Destroyed by Neutron Capture

TABLE 4-26
MAKEUP FISSILE FUEL FOR STANDARD PWR NORMALIZED TO 1 GWe

EQUILIBRIUM CYCLE; BURNUP = 33 GWd/T
REPROCESSING/FABRICATION EFFICIENCY = 0.98

OPERATION WITH Th FUEL ENRICHED WITH ^{233}U MANUFACTURED IN A FUSION HYBRID REACTOR

URANIUM RECYCLE; PLUTONIUM STORAGE:

- CHEMICALLY SEPARABLE ^{233}U : 255 kg/YEAR
- DENATURED ^{233}U : 340 kg/YEAR
- FISSILE Pu STORED PER
YEAR FOR DENATURED CYCLE : 106 kg/YEAR

OPERATION WITH U ENRICHED WITH ^{235}U

- NO FUEL RECYCLE (CURRENT : 872 kg/YEAR (NO CREDIT FOR ^{235}U IN NEW URANIUM)
"THROW-AWAY" CYCLE) : 688 kg/YEAR (0.7% ^{235}U IN FRESH URANIUM)
- URANIUM RECYCLE : 649 kg/YEAR
- FISSILE Pu STORED PER YEAR : 179 kg/YEAR

Figure 4-23 Fissile Fuel Flow for PWR's Fueled With Th/U-233 (Chemically Separable). PWR Power = 1 GWe
Burnup Per Fuel Batch = 33 GWd/T.

Figure 4-24 Fissile Fuel Flow For PWR's Fueled With Th/U-233 (Denatured). PWR Power = 1 GWe; Burnup Per Fuel Batch = 33 GWd/T.

$$AFC = \frac{BOL \text{ FISSILE MASS} - EOL \text{ FISSILE MASS}}{BURNUP}$$

$$AFC = \frac{2.869 T - 1.9509 T}{33000 \frac{MWd}{T} \times 95.37 T} \left(\frac{10^6 \text{ gm}}{T} \right) = 0.292 \frac{\text{gm}}{MWd}$$

where the quantities used were obtained from Tables 4-23 and 4-20. The apparent fissile consumption and other quantities are shown in Table 4-27 and compared with values obtained in a study carried out⁽⁴⁻¹⁹⁾ by Combustion Engineering for EPRI. It can be seen that there is good agreement.

TRITIUM PRODUCTION IN PWR's

Separate calculations have been performed to obtain preliminary estimates of the potential for tritium production in PWR's. This tritium could partially fulfill the fuel requirements for the hybrid reactor. Estimates were made of the tritium production rate to be expected from a 3817 MWt, or 1276 MWe PWR if the tritium were produced in doubly-clad Zircaloy pins filled with natural LiF and employed as shim control. The reaction of interest is:

At neutron energies characteristic of those in a PWR, Li^7 produces essentially no tritium. The self-shielding factor applicable in the LiF pins is roughly 0.7 (with respect to average thermal flux), which is about the same as that in a reference PWR pyrex burnable poison pin. [This is true because with a small adjustment of the as-fabricated density Σ_a for LIF, it can be made to equal Σ_a in a pyrex pin.]

The 3800 MWt core must have 57 of its 193 assemblies reserved for control rod drive mechanisms and two more set aside for sources. Also, there are 16 peripheral assemblies of very low worth, which would probably not produce enough tritium to be worth the expense of fitting them for LiF pins. There should be, in analogy to pyrex burnable poison, 16 LiF pins per assembly. The OD of the pin should be 0.482 inch (1.22 cm). The ID of the inner jacket should be about 0.300 inch (0.76 cm), which allows for relatively thick Zircaloy cladding. Then the maximum available LiF volume, for a 14 foot (4.27 m) core, would be $3.67 \times 10^5 \text{ cm}^3$, and the amount of tritium which would be generated in one year is 2.430 kg tritium (maximum).

TABLE 4-27
PERFORMANCE CHARACTERISTICS OF THORIUM AND URANIUM FUELS

	FUEL	BURNUP (GWd/T)	CONVERSION RATIO ($\overline{CR}$)	APPARENT FISSION CONSUMPTION AFC (gm/MWd)	RATIO OF Pu FISSIONS TO U FISSIONS
(4-12) COMBUSTION ENGINEERING	UO ₂ (3% U-235)	30.4	0.599	0.518	0.68
PRESENT CALCULATION	UO ₂ (3.17% U-235)	33	0.60	0.510	0.70*
(4-12) COMBUSTION ENGINEERING	ThO ₂ (2.99% U-233)	33.4	0.745	0.289	-
PRESENT CALCULATION (CHEMICALLY SEPARABLE)	ThO ₂ (2.88% U-233) (0.13% U-235)	33	0.74	0.292	-
PRESENT CALCULATION (DENATURED)	ThO ₂ (3.19% U-233) +UO ₂ (0.13% U-235)	33	0.78	0.255	0.20 [†]

* Assuming $\alpha = 0.18$ for U (primarily U-235) and 0.366 for Pu

[†] Assuming $\alpha = 0.09$ for U (primarily U-233) and 0.366 for Pu

Typically, the B^{10} concentration at the beginning of the cycle is about 1200 ppm (by weight, relative to water). If the ${}_3\text{Li}^6$ reactivity is to be the same at the beginning of the cycle, then

$$(\Sigma_a \phi V)_{B^{10}} = (\Sigma_a \phi V)_{Li^6}$$

That is, the absorption of neutrons in ${}_3\text{Li}^6$ pins must equal that in ${}_5\text{B}^{10}$ in the moderator.

It has been shown that if the maximum possible volume is filled with LiF, the neutron absorption is about equal to that in ${}_5\text{B}^{10}$ in a reference PWR.

If it were desired to leave more of the core free of LiF pins, then perhaps only a portion of the maximum shim reactivity (1200 ppm ${}_5\text{B}^{10}$ equivalent) could be covered by the stationary pins. For instance, if half of the 1200 ppm ${}_5\text{B}^{10}$ were still to be supplied by boron, the tritium production would be:

$$M_T(1 \text{ yr}) = 1.237 \text{ kg tritium}$$

If the maximum amount of tritium that can be produced in a 1 Gwe plant is assumed to be 1.9 kg/year, and if an upgraded DTHR (CTHR) operating at a plasma fusion power of 1 GW can produce sufficient fissile material to fuel 7 PWR's (see subsection 4.1.6), these PWR's can produce 13 kg of tritium per year. The CTHR would burn about 34 kg of tritium per year. This does not include losses and decay of the tritium. Therefore, the seven PWR's could potentially provide up to ~ 38% of the tritium burned by the CTHR.

It should be emphasized that these calculations are preliminary in nature. No depletion of the ${}_3\text{Li}^6$ was performed to ascertain its rate of burnup for comparison with the reactivity swing currently being supplied by altering ${}_5\text{B}^{10}$ concentration in the coolant. Also, although Zircaloy is the logical choice for cladding the ${}_3\text{Li}^6$ (in order to getter the tritium), LiF is only one of a number of relatively stable ceramics and alloys which might carry the parent isotope. Safety and economic issues remain to be examined.

It should also be reemphasized that the calculations for fissile fuel makeup requirements and the tritium production were based on present day PWR's. With improved designs of the future optimized for Th/²³³U fuel, the fissile production in a hybrid reactor could support a larger number of PWR's and the potential tritium production from all of these reactors could provide a larger fraction of the fuel required by the hybrid.

4.2.4 SAFETY EVALUATION OF THE USE OF $\text{UO}_2\text{-ThO}_2$ FUEL IN WESTINGHOUSE PWR'S

The safety aspects of the use of ^{233}U in PWR's has been investigated in a manner similar to the standard safety evaluation used for current Westinghouse reactors. This method is to compare the physics characteristics of the core (i.e., reactivity coefficients, control rod worths, power distributions, etc.) to limits of these parameters at which the safety of the core has been verified. For the present analysis, calculations were performed to determine typical physics characteristics of the ^{233}U fueled core which were then compared to typical values for a standard ^{233}U fueled PWR. From this comparison it is determined if limiting values will be met. For physics characteristics which do not fall within the limits, an evaluation was made to estimate the effect on the safety of the core.

Only one physics characteristic was determined to be a potential safety problem. This was the delayed fraction which was smaller than for current plants and could be a problem in the rod ejection accident. However, there are compensating factors such as a more negative Doppler coefficient and lower control rod worths. A detailed analysis of this accident will have to be performed to determine whether or not the lower delayed neutron fraction is a real problem.

In this study, other accidents, like steambreak, were determined to have increased safety margins. The net conclusion of this study is, therefore, that while there are areas that need further study, it is judged that there is not likely to be any safety problem associated with the use of ^{233}U as the fuel in a PWR.

METHODS

The same codes were used as used for the generation of physics characteristics for current Westinghouse PWR's. Usually, in connection with safety analysis, diffusion theory instead of nodal theory is used for dimensional calculations. However, for the purposes of this study there was no need to use diffusion theory because its principle advantage is more detailed power distributions which were unnecessary for the present analysis. Further, comparison to results by other investigators in the field of thorium use in LWR's showed good agreement. Therefore, it was concluded that the methods used were adequate for the purposes of this study, which was to identify safety problems associated with the use of thorium fuel

CORE MODEL

The model of the core used in this study is the standard Westinghouse 4-loop plant in all respects except that $\text{UO}_2\text{-ThO}_2$ is used as the fuel. The standard 4-loop plant has 17 x 17 fuel assemblies, a core average coolant temperature of about 590° F and power densities of about 105 kW/ft. Trojan Unit I and Comanche Peak Unit I are examples of the standard plant and the physics characteristics of these plants are referred to when comparison of the thorium fuel cycle results is made to standard plant results.

The $\text{UO}_2\text{-ThO}_2$ fuel that has been analyzed contains about 3.3 w/o ^{233}U at beginning of life. The oxide pellet contains about 1/3 UO_2 and 2/3 ThO_2 . The composition of the uranium is about 1 part ^{233}U to 9 parts ^{238}U . The fuel will be in the reactor for three, yearly cycles and achieve a burnup of 33000 MWD/MTM*.

An equilibrium cycle-throw away cycle has been analyzed. This means that an equilibrium reload cycle has been reached where the same number of enrichment and assemblies are loaded each cycle, but where the bred ^{233}U in the discharged fuel is not recycled. It is realized that the equilibrium cycle with recycle of the bred ^{233}U at the end of each cycle is of more interest. However, the simpler cycle was necessary because of time constraints and it will give pertinent results because the isotopes ^{234}U and ^{235}U do not build up in sufficient quantities to alter the results in any significant manner. Actually, the simpler cycle tends to accentuate the difference between the thorium and standard fuel cycles and is conservative for this reason.

The standard uranium fuel cycle for comparison is a three region core with a cycle length of 11000 MWD/MTU and a region burnup of 33000 MWD/MTU after three cycles. The standard fuel cycle is also an equilibrium cycle and a throw away cycle with an initial enrichment of 3.2 w/o ^{235}U . Thus, the thorium fuel cycle is virtually identical to the standard fuel cycle except for the difference in the fuel.

*MTM is metric tons of heavy metal

PHYSICS CHARACTERISTICS

In this section, the physics characteristics of the thorium fuel cycle are compared to those of standard Westinghouse PWR's. Typical values are compared and from this comparison it is determined whether or not specific limits on a particular parameter are met. The effect of not meeting limits on specific accident analysis is addressed in the subsequent paragraphs.

Moderator Temperature Coefficient

The moderator temperature coefficient is less negative than in a standard plant by 5 to 10 pcm/°F throughout the cycle. The end of cycle value is about -20 pcm/°F compared to -30 pcm/°F for a standard reload core. At BOL the thorium cycle has a value of +2 pcm/°F compared to -8 pcm/°F for a standard plant. Therefore, the thorium fuel cycle will meet the most negative limit on the moderator temperature coefficient, but will not meet the least negative limit. However, fixed burnable poisons can be added to the core to enable it to meet the least negative limit. Burnable poisons are typically added to 18 month cycle standard plants for this purpose, so the more positive nature of the moderator temperature coefficient will not result in a safety problem.

Doppler Coefficient

The Doppler temperature coefficient is about 20 to 30% more negative for thorium than for uranium because the resonance integral of thorium gets larger as temperature increases, faster than ^{238}U . However, because fuel temperatures are expected to be less in thorium fuel and only two-thirds of the fuel is thorium in the denatured cycle, the Doppler power coefficient will only be about 10% more negative than standard. A more negative Doppler Coefficient is generally good in safety analysis.

Delayed Neutron Fraction

This quantity is significantly smaller than for a standard core. This is caused by the low value associated with ^{233}U . The delayed neutron fraction for the thorium system is 0.003 compared to the value for the standard plant of 0.005. This low value has adverse consequences on safety analysis principally in the rod

$$^* \text{pcm} = 10^{-5} \Delta k$$

ejection accident which will be addressed in more detail later. While this study has used the throw away cycle, recycle with its greater concentration of ^{235}U will not greatly improve the delayed neutron fraction of the thorium system because the amount of ^{235}U does not increase significantly.

Prompt Neutron Lifetime

This is 20% smaller than for the standard plant with the low value being 15 microseconds and the high value being 18 microseconds. The prompt neutron lifetime is smaller because of the somewhat faster spectrum in the thorium system. The difference compared to the standard plant is not important in safety analysis.

Control Rod Worth

This is about 10% smaller in a thorium system compared to the standard plant. The rod worth is reduced because of the somewhat faster spectrum and also because of the slightly larger thermal absorption cross section of the fuel caused by the larger thorium cross section. Since minimum values of the rod worth will still be met, the 10% reduction will not affect the safety of the plant.

Boron Worth

The boron worth is in the range from -6 to -7 pcm/ppm as compared to -10 to -11 pcm/ppm for a standard plant. This difference is caused by the faster spectrum and the greater cross section of ^{232}Th in the thermal energy group. This will result in only slightly higher soluble boron concentrations in the core because the thorium fuel is somewhat less reactive. The lower boron worth will not be a safety problem because excess boration capability is present in the standard plant.

Burnable Poison (BP) Worth

This also will be smaller for the same reasons given for the boron worth. However, since BP's are used to decrease the boron concentration, they will do this as well as in a standard system because the dissolved boron worth is also smaller and the two effects will offset each other.

Peaking Factors

The peaking factors (i.e., maximum relative power) of the thorium fueled core are not changed compared to a standard plant for both rodged and unrodged power distributions. If anything, the rodged peaking factors may be somewhat smaller because of the lower rod worth. The reason the peaking factors will not change is that the cross sections for the thorium system are not much different than for the uranium system and, therefore, the power distributions will not be much different. This means that the thorium core will be able to meet all the peaking factor limits that are met by the standard core.

Trip Reactivity

The trip reactivity or the rate at which negative reactivity is inserted into the core following a control rod trip or scram will be the same as for a standard core. While the rod worth is somewhat less in a thorium system, the 4% lower limit on rod worth used as the basis for safety analysis can still easily be met and, since the power distributions are unchanged, the shape of the negative reactivity insertion curve as a function of time will also be the same.

Decay Heat

Decay heat for a thorium system is not larger than a uranium system and it may be smaller. Comparing the Shippingport decay heat curve to that of the standard plant shows that they are almost the same. The decay heat from ^{233}Pa is comparable to the decay heat associated with the fission products of ^{232}Th . However, because the percent of fissions in ^{232}Th is small, the decay heat of ^{232}Pa is judged not to be a problem. The decay heat due to delayed neutrons will be less in a thorium system because there are less delayed neutrons (smaller delayed neutron fraction). Therefore, decay heat will not be a safety problem for the thorium system because it is the same or smaller than for the standard plant.

Fuel Temperatures

The fuel temperatures should be smaller in a thorium system because the thermal conductivity of ThO_2 is higher. This fact combined with the fact that the heat

capacity is about the same as UO_2 means that the stored energy in the thorium fuel will not be greater than for standard fuel and will, therefore, not be an additional safety problem.

^{233}U Buildup

One unique characteristic of the thorium fuel cycle is the increase in reactivity after shutdown due to the buildup of ^{233}U from the decay of ^{233}Pa . It is not a safety problem because the total increase in reactivity is only a few percent Δk and, more importantly, the rate of increase is very slow because of the 27 day half-life of ^{233}Pa . Thus, the boron system can easily handle both the magnitude and rate of change in the core reactivity.

SPECIFIC ACCIDENT CONSIDERATIONS

The following is a list of some important accidents which are analyzed in the design of a reactor. The effect on these accidents of the changes in the physics characteristics due to the use of the thorium fuel cycle are presented.

Boron Dilution

This accident will have reduced consequences compared to the standard plant because the dissolved boron worth is less and, therefore, not as much reactivity is being inserted during dilution.

Control Rod Withdrawal

The consequences of this accident are also reduced because the rods are worth less and therefore not as much reactivity is being inserted into the core.

Dropped Rod, Bank, or Misalignment

These accidents are not worse than for the standard plant because the power distributions for these accidents are not worse than standard. Again, the fact that the rod worth is smaller is a benefit.

Control Rod Ejection

This accident is likely to be worse compared to the standard plant because the delayed neutron fraction is smaller for a thorium system. Some mitigating factors for this accident are the larger Doppler coefficient, lower rod worth, and possibly shallower rod insertion limits in a thorium system. This accident will have to be investigated more closely to see if design changes will be necessary for the thorium system in order to meet safety criteria during the accident.

Steambreak

This is not likely to be a problem because the moderator temperature coefficient is much less negative than in a standard plant and, therefore, the increase in reactivity due to primary coolant cool-down is much less. In addition, the return to power coefficient will be more negative because the Doppler coefficient is more negative. The rod worth is less, but this is not a large penalty for two reasons. First, less rod worth is required because the increase in the Doppler "defect" is more than offset by the decrease in the moderator "defect". Second, the N-1 rod worth used for steambreak will not be reduced as much as the total rod worth because the worth of the worse stuck rod is also less. In any case, the less negative moderator temperature coefficient will compensate for any loss of rod worth and, therefore, steambreak safety criteria will be met. The reduction in boron worth during the safety injection phase is not a problem.

LOCA

LOCA will not be a problem because peaking factors, fuel temperatures, and decay are not worse than the standard plant.

CONCLUSIONS

Certain physics characteristics, like the delayed neutron fraction, were identified as requiring further analysis to determine their exact effect on safety analysis. No physics characteristics were found that would definitely prevent a PWR using the thorium fuel cycle from meeting the same safety criteria as a standard plant. Generally, the physics characteristics of the thorium core are comparable to the standard core with the result that the operational characteristics of the standard core are not significantly affected and the safety margins are not compromised.

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APPENDIX A

THE COAST CODE

The computer code COAST⁽¹⁾ (Costing And Sizing of Tokamaks) has been modified to include the DTHR to provide self-consistent sizing data, plant and capital equipment (PACE) costs, operating costs, and parametric data for trade-off and sensitivity studies. The modified version, COAST 4, containing features of the tokamak hybrid was developed during this study period and is based on the results of engineering design studies on the DTHR. The following is a brief description of the various code models as applied to a D-T hybrid reactor. The reader should consult references (1) through (6) for documentation of previous work in the sizing, costing, and trade-off studies of TNS devices performed with the COAST code. During those studies, the code was extended and upgraded several times; the third version was employed in the final TNS sizing analysis.

In general, the code was written to evaluate, in a quantifiable manner, the engineering considerations of D-T burning tokamak reactors and associated facilities; over forty subsystems or components are considered. While system models included in the code calculations are refined, the input to them is generalized so that a wide range of tokamak systems can be studied. Normal copper or superconducting (NbTi or Nb_3Sn) coils are modeled for both the poloidal and toroidal field coil systems. Neutral beam heating for either an ignition tokamak or a beam-driven device is included as well as recent divertor concepts as reported in the main text. The entire facility (reactor cell, fueling [tritium] systems, heat dissipation systems, shield and blanket systems, and electrical power production and conversion systems) is modeled. In addition, several of the plasma engineering aspects of reactor performance are modeled and the resulting parameters are used in the sizing factor of device components.

In the area of plasma engineering, COAST 4 evaluates the characteristics of the plasma configuration during the steady-state burn. Both energy balance and particle balance considerations are incorporated, where

$$\frac{(E_e + E_i) \bar{n}_e}{[\bar{n}_e \tau_E]} = W_\alpha + W_{NB} - W_L \quad (1)$$

$$[\bar{n}_e \tau_E] = C \bar{n}_e^u (aS)^v q^w B_t^x A^y \hat{T}_e^z \quad (2)$$

In Eq. (1), the kinetic energy losses in the plasma associated with the electrons and ions are balanced by the α -particle power density (from both plasma-plasma and beam-plasma interactions), the kinetic power density due to the neutral beam particles ($W_{NB} = 0$, if ignited), and radiation losses. In Eq. (2), the scaling of $[\bar{n}_e \tau_E]$ is coded in a generalized expression involving density, plasma radius times shape factor, stability factor, magnetic field on axis, aspect ratio, and temperature. For empirical scaling, it is assumed that $C = 3.2 \times 10^{-21}$ meter second, $u = v = 2$, $w = 1/2$, $x = y = z = 0$, $\bar{n}_e$ in m^{-3} , and aS in m. Particle confinement time is assumed to be a constant multiplied by the energy confinement time. The calculations take plasma particle populations into account for seven particle types (electrons, tritons, deuterons, impurity ions, "warm" alphas, "hot" alphas, and "hot" beam particles). The parameters calculated include neutron wall loading, particle and heat fluxes, toroidal and poloidal betas, plasma current, fusion power, Q-values, etc. These, in turn, are used in the sizing of the tokamak mechanical and electrical components.

In addition to steady-state plasma performance data, the code also estimates the plasma startup current and voltage as a function of time. These time profiles are used in computing the current and voltage requirements for operating the ohmic-heating coil besides the shaping and equilibrium field coils.

The neutral beam injector system model accounts for the various aspects of operation of a positive-ion based system with or without direct energy conversion. For a given source geometry selected through input data, the sizing and performance of the sources, neutralizer ducts, ion collectors, and vacuum pumping are evaluated. The electrical power requirements are computed taking into account transmission, power supply, and neutralization efficiencies for the neutralizer geometry selected.

The various blanket and/or shield models have been developed from detailed design calculations. These models have been generalized, where possible, and require design data obtained from prior design analysis. For example, blanket performance data (energy multiplication factors, fissile and/or tritium atom production ratios) are required for a specified blanket design of known blanket and shield region thicknesses. Based on the sizing of the plasma core and on the wall coverage fraction, the blanket size (material requirements) and performance parameters (e.g., thermal power production) are obtained for each device being sized. An option for the conversion of thermal power to electrical power is also included in the modeling. If the thermal power is not converted (as in the present DTHR design) the code sizes a heat dissipation system for dumping the power. In the event that the once through coolant system is not adopted for DTHR, the costs of the cooling tower, heat exchanger, etc. would be included.

To model the long-pulse, beam-driven performance of a tokamak reactor, a fueling system model and a plasma exhaust system model based on an advanced-concept bundle divertor system are incorporated. The fueling system includes subsystems for deuterium storage, tritium storage, fuel fabrication and injection, exhaust plasma processing, and cryogenic distillation. The bundle divertor system model allows for the sizing of the divertor magnet and associated nuclear shielding, burial chamber, and heat dissipation systems.

The sizing of the various poloidal field coils and the calculation of the time profile of the coil voltages and currents reflect the effects of changes in the geometry of the tokamak in the sizing process and of the behavior of the plasma, as described above. One coil system consists of an ohmic-heating coil set which has an air core and occupies the central region within the bore created by the toroidal field coil assembly. The remaining systems make up the shaping and equilibrium field coils which can be located within the bore of the TF coils or external to the TF coils, by setting a program flag. These coils are divided in three groups; i.e., inside coils (at radii $< R_0$), divertor-like coils (above and below the midplane), and outside coils (at radii $> R_0$). The currents in the shaping and equilibrium field coils are scaled as a function of coil radius and vertical distance from the device midplane from current magnitudes supplied as input for a reference system (and assumed to satisfy plasma

equilibrium conditions). The OH coil currents and voltages in each coil set are calculated using a coil-coil and coil-plasma inductance matrix which is evaluated from the coil geometry (i.e., coil radii and vertical positions, OH coil dimensions) and the plasma position (i.e., plasma radius). From the voltages and currents power supply requirements are determined.

The toroidal field coil geometry and sizing includes a number of coil shapes as well as coil technologies and are calculated taking into account the various space requirements imposed by other device components. The coil shapes include the constant-tension D, circular, and the compact constant-tension shape used for DTHR. The coil technologies include: 1) copper (both room temperature and liquid nitrogen cooled); 2) superconducting NbTi; 3) superconducting Nb₃Sn; and 4) a Cu/NbTi compound coil system. The space requirements for sizing the TF coil horizontal and vertical bores consist of meeting the space requirements imposed by the components located within the bore region, by access requirements for neutral beam ducting, by the bundle divertor coils, by TF ripple at the plasma edge, and by access requirements imposed by assembly and maintenance of components. In sizing the TF coils, the centering forces are handled via wedging and/or a central bucking cylinder. The coil cooling systems and power conversion systems are also sized.

Following completion of the sizing and the rating of power supplies and the heat dissipation loads, COAST calculates (system-by-system) the costs associated with the device. The costing is for a complete facility and is grouped according to the major systems; i.e., tokamak, electrical power and control, tokamak support, structures and site, and neutral beam injection. The cost relationships are patterned for maximum flexibility and include economy of scale factors where appropriate. Direct costs as well as total plant and capital equipment costs and operating costs are obtained. The costing requires approximately 100 unit cost coefficients to be supplied as input data. The output lists costs and percentage of total cost per subsystem.

The input/output ratio associated with the code is extensive so as to allow considerable flexibility and to keep component models as general as possible. All input data is listed as part of the input. Extensive data is also printed on the sizing of the plasma, TF coils, PF coils, power supply ratings, neutral beam parameters, etc. A listing of the geometry along the device midplane as well as along the vertical direction above the plasma center is generated and can be used in making engineering layouts.

The modeling and logic incorporated in the COAST 4 code is the work of the entire WFPS staff. The experience and expertise of professionals in the areas of plasma physics, fusion technology, thermal engineering, mechanical engineering, electrical engineering, chemical engineering, and nuclear engineering have been utilized. The resulting code has been a powerful tool in the study and design of fusion tokamak reactor systems and should also prove invaluable in the design and analyses of hybrid reactor systems.

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APPENDIX B

ALTERNATE FUSION DRIVER

The reference geometry and plasma parameters used for DTHR in order to perform preliminary engineering studies have been described in subsection 3.1.1. A smaller alternate design has also been discussed in the October-December 1977 Quarterly Progress Report⁽¹⁾. The smaller design was based on the elimination of the bucking cylinder for the TF coils and allowing a minimum amount of space for the OH coil. The possibility of minimizing the TF coil bore was also discussed. Further engineering studies are required to determine if the alternate design is feasible. Both of these designs are flexible designs that would allow near ignition conditions to be reached while maintaining β as low as 5-6% based on an empirical scaling law. Although ignition is not essential for meeting the DTHR objectives, the reference and alternate designs assure plasma conditions that could be attained, where plasma-plasma fusion reactions as well as beam-plasma fusion reactions would contribute to the neutron production and high Q values would result.

In this Appendix, a second alternate tokamak design is discussed that results in a small device that requires less demanding technological advances and is less costly at the expense of having less flexibility. The tokamak would operate at low Q, essentially in the intensely beam-driven (TCT) mode of operation. Ignition could be achieved only at very high β ($>10\%$). However, the goals of the DTHR (viz, the production of >100 kg of fissile material per year) could be met while retaining the same neutral beam power, viz, 150 MW.

The tokamak design discussed in this section has the following features:

1. The peak field at the TF coils is limited to 10 T. This would relax the design constraints such as forces and amount of structure, current density and coil cross section, and thermal margin for the superconductors. On the other hand, the toroidal magnetic field at the plasma would be decreased which would tend to increase β . However, for TCT-type operation, high plasma densities are not required and stable low- β plasma operation is tolerable if beam-plasma reactions are the main source of fusion neutrons.

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2. Neutral beam injection systems developed for TFTR and having an injection energy of 150 keV would be utilized. This technology should be available even though the pulse length would be much longer than for the TFTR application. The lower energy injection can be used because of the lower plasma density at which this tokamak will operate.
 3. A circular plasma, which is typical of most present day devices, would be generated. This should greatly relieve the shaping field requirements and improve the probability of attaining an acceptable design based on SF coils located outside the TF coils. Since high Q operation is not essential for DTHR, the advantages of plasma elongation can be sacrificed.
 4. Since achieving ignition grade plasmas at low β is not necessarily a requirement of DTHR, a smaller plasma major radius of $R = 4.4$ m has been selected. This will result in a toroidal field of ≈ 4 T. Because of the relaxed specifications of the TF and OH coils, there is sufficient space inside the plasma loop for these components. Because of the smaller major radius and compact TF coil design, there is less space for the bundle divertor. However, since the toroidal field has been reduced, the specifications for the divertor are relaxed. At the smaller major radius the number of TF coils required can be reduced; 12 have been specified.

The features of the smaller DTHR are given in Table B-1. This design also has been studied using the COAST code. The plasma current for this design is 1.9 MA, which is smaller than the guideline value of 3 MA given in Table 3.1.1-1. However, estimates have been made of the widths of the "banana orbits" for α -particles in this device and they have been found to be less than the minor radius of the plasma for $I_p \sim 1.5$ MA. Therefore, the plasma current of 1.9 MA should be acceptable. Furthermore, confinement of the α -particles may not be required to achieve the plasma operating conditions postulated. With α -particle confinement, energy balance requires an energy confinement time of 0.13 s; without α -particle confinement an energy confinement time of 0.18 s is required.

TABLE B-1

ALTERNATE (SMALL) DTHR PLASMA PARAMETERS

MAJOR RADIUS	4.4 m
MINOR RADIUS	1.0 m
ELONGATION	1 (CIRCULAR)
TOROIDAL FIELD ON AXIS	4.1 T
PEAK FIELD AT THE TF COIL	10 T
PLASMA CURRENT	1.9 MA
NEUTRAL BEAM ENERGY	150 keV

Table B-2 shows the variation in wall loading as a function of the plasma composition. The plasma operating conditions are given below the table. In order to maintain $\beta \sim 5\%$, the electron density must be low, $n_e = 5 \times 10^{19} \text{ m}^{-3}$. At low densities the neutron production results primarily from deuterons in the beam undergoing fusion reactions with tritons in the plasma and the wall loading is decreased as the tritium fraction in the plasma is decreased. For example, with a 50% tritium/50% deuterium plasma the wall loading is 50% smaller than with a pure tritium plasma. However, as shown below it is impossible to maintain a pure tritium plasma. If a pure tritium plasma could be maintained, the fusion power generated in this idealized case would be $2.1 \times 150 \text{ MW} = 315 \text{ MW}$, which is $1/3$ the fusion power generated in the reference design. Therefore, for equivalent blanket coverage $\sim 1/3$ as much fissile material would be produced per year. However, for the same blanket thickness the fertile material inventory would be reduced by $\sim 1/2$ because of the smaller device. To achieve the design goal of producing 100 kg of fissile fuel per year, additional blanket coverage of the torus would be possible, viz, the entire outer half of the circumference of the torus with the exception of the penetrations for neutral beams and divertor. An average wall loading is obtained of 1.3 MW/m^2 , which compares favorably with the 2.0 MW/m^2 of the reference design, because of the smaller surface area of the wall for the smaller DTHR (200 m^2 as compared to 380 m^2 for the reference).

Table B-3 shows the variation in wall loading as a function of the plasma composition for the reference DTHR. Note that in contrast to Table B-2 the wall loading increases as the tritium/deuterium ratio increases in the plasma to a value of 2 MW/m^2 at a 50/50 mixture. This results because, at the high densities that can be achieved in the reference tokamak, a significant fraction of the fusion reactions occur between the plasma particles. If the reference tokamak were operated at low densities, the wall loading would decrease as the tritium concentration in the plasma decreased as reflected in Table B-2 for the small DTHR.

A serious problem arises when operating in a strongly beam driven mode at low plasma densities, viz, the deuterium added to the plasma via the neutral beams tends to make the relative deuterium concentration in the plasma high and

TABLE B-2
VARIATION OF WALL LOADING WITH PLASMA COMPOSITION
FOR THE SMALL DTHR

T-FRACTION α_T	D-FRACTION α_D	Q	WALL LOAD (MW/m ²)
1.0	0.0	2.10	1.30 (IDEALIZED) CASE
0.58	0.42	1.22	0.76 (BEST ATTAINABLE) CASE
0.5	0.5	1.08	0.67
0.4	0.6	0.90	0.56
0.2	0.8	0.48	0.30

PLASMA OPERATING CONDITIONS:

150 MW, 150 keV NB, 13 keV plasma, $n_e = 5 \times 10^{19} \text{ m}^{-3}$

$$Q = Q_{\text{beam}} + Q_{\text{plasma}} \cong 2.1 \alpha_T + 0.83 \alpha_T (\alpha_D - 0.42)$$

TABLE B-3
VARIATION OF WALL LOADING WITH PLASMA COMPOSITION FOR REFERENCE DTHR

T-FRACTION α_T	D-FRACTION α_D	Q	WALL LOAD (MW/m ²)
1.0	0.0	2.3	0.72
0.8	0.2	5.2	1.6
0.6	0.4	6.4	2.0
0.5	0.5	6.4	2.0
0.4	0.6	6.0	1.9
0.2	0.8	3.8	1.2
0.1	0.9	2.1	0.66

PLASMA OPERATING CONDITIONS:

150 MW, 200 keV NB, 13 keV plasma, $n_e = 1.5 \times 10^{20} \text{ m}^{-3}$

$$Q = Q_{\text{beam}} + Q_{\text{plasma}} \cong 2.3 \alpha_T + 21 \alpha_T \alpha_D$$

therefore, the wall loading low. In fact, under certain conditions, an increase in the neutral beam current can result in a lower wall loading.

For the smaller DTHR, assume that 150 MW of 150 keV-D⁰ are added to the plasma. This means the equivalent of 1 kA or $6.2 \times 10^{21} \text{ D}^+ \text{ s}^{-1}$ are added to the plasma. The plasma has a volume of 87 m^3 so the deuteron plasma density would tend initially to increase at a rate of $R_{D^+} = 7.1 \times 10^{19} \text{ D}^+ \text{ m}^{-3} \text{ s}^{-1}$. The plasma electron density is assumed to be $5 \times 10^{19} \text{ m}^{-3}$ and the plasma temperature 13 keV. Beta due to the electrons alone is $\beta_e = 1.5\%$. Under these plasma conditions the time required for the 150 keV deuterons to slow down to 13 keV is $\tau_s \simeq 0.3 \text{ s}$. Thus, the density due to these hot ions is $n_h \simeq R_{D^+} \tau_s$ or $n_h = 2.1 \times 10^{19} \text{ D}^+ \text{ m}^{-3}$. This represents 42% of the total ions that possibly exist in the plasma based on charge neutrality. If the average energy of these deuterons is $\sim 75 \text{ keV}$, beta due to these particles is $\beta_h \sim 2.5\%$.

Consider now the density of deuterons in the bulk plasma at the plasma temperature of 13 keV. Since only a small fraction ($\sim 2\%$) of the deuterons are lost due to fusion reactions while slowing down and a small fraction due to charge exchange losses, the approximate rate at which deuterons "enter" the bulk plasma equals the rate at which energetic deuterons are injected into the plasma. Once thermalized, the deuterons should have a confinement time approximately equal to the tritons in the plasma. The energy confinement time based on empirical scaling is τ_E (empirical) = 0.25 s. If the particle confinement time is twice the energy confinement time $\tau_p = 0.50 \text{ s}$. Therefore, the deuteron bulk plasma density is $n_{D^+} \simeq R_{D^+} \tau_p = 7.1 \times 10^{19} \text{ D}^+ \text{ m}^{-3} \text{ s}^{-1} \times 0.50 \text{ s}$ or $n_{D^+} \simeq 3.5 \times 10^{19} \text{ D}^+ \text{ m}^{-3}$. The contribution to the total β due to these ions is $\beta_{D^+} = 1.1\%$. Comparing the estimate of the total deuteron density, $n_h + n_{D^+} = (2.1 \times 10^{19} + 3.5 \times 10^{19}) \text{ D}^+ \text{ m}^{-3} \simeq 5.6 \times 10^{19} \text{ D}^+ \text{ m}^{-3}$ with the electron density $n_e = 5 \times 10^{19} \text{ m}^{-3}$, it can be seen that the plasma could not be generated with an appreciable tritium concentration when injecting 150 MW of 150 keV-D⁰. Although these are crude estimates, they do indicate the possibility of a serious problem.

The results are sensitive to the particle confinement time. If the particle confinement time were assumed to be equal to the energy confinement time, i.e., $\tau_p = \tau_E$ (empirical) = 0.25 s, a tritium density of $n_{T^+} \simeq 1.2 \times 10^{19} \text{ T}^+ \text{ m}^{-3}$ could be maintained. This corresponds to a tritium concentration in the plasma of

$\alpha_{T+} = (1.2 \times 10^{19} \text{ T}^+ \text{ m}^{-3}) / 5 \times 10^{19} \text{ m}^{-3} = 0.24$. The deuteron concentration would be $\alpha_{D+} = (3.8 \times 10^{19} \text{ D}^+ \text{ m}^{-3}) / 5 \times 10^{19} \text{ m}^{-3} = 0.76$. As indicated in Table B-2, the fusion power production would be significantly reduced under these conditions. If the plasma particles have an extremely short containment time, as would be predicted by the trapped ion mode scaling $\tau_{TIM} = 0.036 \text{ s}$ (however, profile effects are expected to increase this value by an order of magnitude⁽²⁾), the tritium density could approach $n_{T+} = 2.9 \times 10^{19} \text{ m}^{-3}$ and $\alpha_{T+} = 0.58$. Of course, under this condition the plasma throughput is high and this places an extra burden on the divertor and vacuum pumping systems.

The composition, $\alpha_{T+} = \alpha_{D+} = 0.5$ could be maintained if equal currents of energetic deuterium and tritium were injected into a plasma.

The α -particles that are produced by the fusion reactions do not contribute significantly to the ion density, but since their energies are high, they can contribute to the β . Equations have been programmed in a subroutine of COAST to calculate the densities of all species in the plasma and to take into account profile effects.

The problem that has been discussed above is aggravated by the fact that, in addition to the high energy deuterons, deuterons of 1/2 and 1/3 energy are injected into the plasma because of the acceleration of diatomic and triatomic molecules. For example, if 80% of the ions that are extracted from the ion source and subsequently accelerated to 150 keV are monatomic and the remainder molecular ions, the total equivalent current of D^+ entering the plasma is $\approx 2.3 \text{ kA}$ rather than 1.0 kA. This would result in still higher deuterium concentrations in the plasma.

Considering the buildup of deuterium in the plasma of the reference design due to the neutral beam injection, the plasma can operate at a higher density than the smaller DTHR while maintaining the same β and plasma temperature because of the larger toroidal magnetic field strength. The density is three times larger; therefore, the slowing-down time of the energetically injected ions is about 1/3 as long. The slowing-down time is not reduced quite by a factor of 1/3 since the neutral beam energy is $\approx 200 \text{ keV}$ as compared to 150 keV for the smaller DTHR.

However, the equivalent current of neutral beam is smaller for the same power input (150 MW) because of the higher energy of the particles. Therefore, the relative concentration due to the energetic deuterons (ratio of number of energetic ions to total number of ions) in the plasma is reduced by $\sim 1/3$ due to the more rapid slowing-down, by $\sim 1/3$ due to the higher plasma volume, and $\sim 1/3$ due to the higher plasma density. Recall that $n_h/n_e = (I_b \tau_s)/n_e V_p$, where I_b is the particle current in the neutral beams and V_p is the plasma volume. The energetic deuterons in the plasma of the reference tokamak should represent only a few percent of the total number of ions.

However, the relative concentration of deuterons that have degraded in energy to the plasma temperature should be comparable for the reference and smaller DTHR's. This concentration is given by

$$\alpha_D^+ = (N_D^+/n_e) \sim (I_b \tau_p)/(n_e V_p) \sim (I_b a^2 (1+\delta^2))/(R a^2 \delta)$$

if the particle confinement time, τ_p , is assumed to scale as the empirical energy confinement time, i.e., $\tau_p \sim n_e a^2 (1+\delta^2)$. For a given neutral beam power $I_b \sim E_b^{-1}$,

$$\alpha_D^+ \sim (1+\delta^2)/(E_b R \delta),$$

and

$$\frac{\alpha_D^{+-reference}}{\alpha_D^{+-small}} = \frac{150 \text{ keV}}{200 \text{ keV}} \frac{4.4 \text{ m}}{5.2 \text{ m}} \frac{1}{1.6} \frac{(1 + 1.6^2)}{2}$$

$$\alpha_D^{+-reference}/\alpha_D^{+-small} = 0.71$$

Therefore, if the particle confinement time is twice the energy confinement time obtained from empirical scaling, $\alpha_D^{+-small} = 0.7$ (as shown earlier) and $\alpha_D^{+-reference} = 0.5$, which is acceptable for the reference design. Furthermore, if the energy confinement time is predictable by empirical scaling, the required plasma conditions can be maintained at a reduced neutral beam power (as discussed in Section 3.1.1) which will alleviate the problem under investigation.

The smaller DTHR operating at low densities has many appealing features, but it would appear that there would be difficulties in achieving the high fusion power necessary for a viable device. However, a tokamak smaller than the reference and operating under less demanding requirements may be feasible, but additional studies are required. During the next quarter the COAST code will be used to study the effects discussed in this section and to optimize the size and operating conditions of a reference design taking into account the performance versus costs.

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APPENDIX C

CRYOSTABILITY ANALYSIS OF OH COIL SUPERCONDUCTOR

The locations of the OH coils for the DTHR are shown in Figure 3-8 (subsection 3.2.1). The majority of the coils are located at the center column of the reactor. The others are located on the out-board side of the tokamak together with the SF coils. The coils are superconducting using $\text{Nb}_3\text{Sn-Cu}$ composites as in the TF coils. The cable design is similar to the TF coil cable, except that the conductor area is 3.61 cm^2 instead of 5 cm^2 . All composite strands in the OH coil cable are designed to carry current. The OH coil conductors are, however, subjected to the superconductor hysteresis and coupling losses, and the structural material eddy current losses due to the pulsed field. The cryostability of the superconductors is, therefore, the primary thermal design objective of the OH coils. The preliminary design parameters and specifications for the OH and SF coils are shown in Table 3-10 and 3-11, respectively of subsection 3.2.3.

The superconductor ac losses were calculated based on the method of solution given in reference (1). The rates of field change at the peak field region of the OH coils used in the analysis are shown in Figure C-1. The profile shown in the figure was obtained from the COAST code analysis. The cycle time is taken to be 85 seconds. The peak field in the in-board OH coils is about +9 Tesla at the beginning of the cycle. The field decreases with a rate of about 100 T/s for the first .005 second, then with a rate of 11 T/s to 1 second and 3 T/s to 2 seconds. The rate is very small from 2 to 68 seconds. From 68 to 70 seconds the magnetic field is assumed to reverse from -9 Tesla to +9 Tesla with the same rapid change rate at about 70 seconds. Using this pulsed profile, the heat generation rate in the superconductor at the peak field region due to the ac losses at the beginning of the cycle was calculated to be 45 W/cm^3 from the first .005 second and 0.40 W/cm^3 from 0.005 to 2 seconds. During the field reversal at the end of the cycle the same two

rates of heat depositions were also assumed to occur. The conductor and helium temperature responses at the peak field region of 9 Tesla at the beginning of the cycle were calculated and shown in Figure C-2. It is seen from the figure that with a helium flow rate of 2 g/sec the conductor temperature is increased to about 14.7K at 0.005 seconds. Since the superconductor critical temperature for this design is 10.5K, the superconductor was driven to the normal state. After the $\frac{dB}{dt}$ is reduced to about 1/10 of its previous value, the superconductor started to recover with the helium flow rate considered for this case. The full recovery is at about 52 m-sec. It should be noted that the analysis assumed that the heat generation due to ac losses continued to take place after the superconductor was driven above its critical temperature. The results are, therefore, conservative. The LH_e refrigeration loads for pumping power, dewar heat leakage (coil current leads cooling is not included here) and ac losses were estimated as shown in Tables 3-10 and 3-11. The loads for the in-board OH coils are very conservative, since not all the in-board OH coils are at the peak field of 9 Tesla. The portion of the refrigeration load due to the ac losses would not be needed, however, when the reactor is not operating.

It is noted that in addition to the heat deposition due to the ac losses, heat deposition due to mechanical instabilities, such as strand friction, strain release, etc., would also occur. These mechanical instabilities would mostly occur during the rapid change of the magnetic field of the coil. The most conservative case is considered to be that in which the heat release due to the mechanical instability drives the superconductor to a normal state instantly at the very beginning of a cycle to, for example, 18 K. Because of the additional ac losses during this time the conductor temperature would be further raised to a higher level. The conductor and coolant temperature responses for this case with a helium flow rate of 2 g/s are shown in Figure C-3. The calculation showed that the conductor temperature reaches 20 K, but starts to recover when the field change rate is reduced. The superconductor recovers at about 80 msec. It is, therefore, seen that with this OH coil design and the pulsed field profile considered, the superconductor is cryostable.

The cryogenic recovery capability of the OH coil at various helium flow rate per conductor is shown in Figure C-4. The design point flow rate can be selected to be 2 g/s per conductor. Below 2 g/s the recovery capability is very small. At this flow rate the maximum recoverable initial normal temperature is 18.5K, equivalent to a total heat removal capability of 550 mJ/cm^3 of conductor volume.

Since the helium temperature is also increased by about 1 to 1.5 degrees at the peak field region (Figures C-2 and C-3) during the pulse, the time required for the helium temperature to recover to its initial value of 4.2K is another design consideration. This will be evaluated in the conceptual design of the OH coils.

REFERENCES - APPENDIX C

1. Gray, W. H. and Ballow, J. K., "A Computer Program To Calculate Composite Conductor Losses In Pulsed Poloidal Coil Systems," IEEE Trans. on Magnetism, Vol. MAG-13, No. 1, 1977, p. 213.

Figure C-1. Peak Field Change Profile of the In-Board OH Coil During a Pulse

Figure C-2. Recovery of OH Coil From Pulsed AC Losses at the Beginning of a Pulse

Figure C-3. Recovery From Initially Imposed Conductor Normal Zone Temperature of 18 K at the Beginning of a Pulse

Figure C-4. Cryogenic Recovery Capability of the Superconductors for OH Coils

APPENDIX D

RADIATION HAZARD ASSOCIATED WITH REPROCESSING FUEL BASED ON THE Th/ ^{233}U CYCLE

Primarily, this appendix addresses the radioactivity buildup for the Th/ ^{233}U / ^{232}U system as a function of time following the separation of uranium from thorium in irradiated fuel. As mentioned in the text, this information is important relative to the fuel reprocessing and also for nonproliferation considerations. During the mid 1960's two pilot plants were operated for fabricating Th/ ^{233}U fuels. The experience gained in operating these plants has been documented in the Proceedings of the Second International Thorium Fuel Cycle Symposium held at Gatlinburg, Tennessee in May 1966. Because of their interest to the present work the abstracts of these two papers are presented. Also, some information from these papers is presented in figures shown in this appendix.

ABSTRACT FROM REFERENCE 1

"An Unshielded Pilot Plant For Recycling ^{233}U "
BABCOCK & WILCOX R&D

A pilot plant to convert recycle fuel and fabricate prototype-fuel elements of the power-reactor type is described. Initial operation with thoria-urania containing ^{233}U has been successfully demonstrated. Recycled ^{233}U is usually contaminated with a small amount of ^{232}U which, through some of its decay daughters, contributes significantly to the gamma radioactivity of recycled ^{233}U fuels, particularly in the penetrating high-energy region. The resultant fabrication problem is usually solved by using remote or semiremote techniques. However, to avoid the inherently high capital and operating costs of such methods and to simplify maintenance problems, a method has been developed whereby the ^{233}U fuel can be deactivated and then refabricated directly. The fuel is deactivated by removing the first daughter of ^{232}U (^{228}Th) and is then refabricated rapidly before the increase in the ^{232}U daughters and the associated gamma activity creates a hazard.

Customary glove-box techniques, which are relatively inexpensive, are used in refabrication.

The pilot plant was successfully tested with about 390 kg of natural material before being operated with 82 kg of oxide containing approximately 2.3 kg of ^{233}U contaminated with about 42 ppm ^{232}U . A prototype element typical of those used in pressurized-water reactors has been fabricated. Radiation exposures to the operators were well within permissible limits. The information gained during the hot runs indicates that 1000 ppm ^{232}U - ^{233}U (or approximately 300 mg ^{232}U per batch) is representative of the level of impurity that can be tolerated in the existing pilot plant operating for five days at a capacity of 50 kg of ThO_2 -3 wt.% UO_2 per week.

The experimental program and major results are summarized and evidence of the feasibility of the direct fabrication technique is presented.

ABSTRACT FROM REFERENCE 2

"The Impact of Kilorod Facility Operational Experience on the Design of Fabrication Plants For ^{233}U -Th Fuels"

ORNL

To advance the technology of the thorium fuel cycle, the Kilorod Facility was designed, constructed, and operated. Specific objectives of the 8-month-long demonstration program were (1) to prepare approximately 1000 Zircaloy-clad rods containing 3% $^{233}\text{UO}_2$ -97% ThO_2 , (2) to determine the radiation levels and personnel exposures encountered in the fabrication of ^{233}U -bearing fuels, and (3) to provide base-line engineering information for future fabrication plants. The results of the pilot-plant demonstration were highly encouraging. In sustained operation, data were collected on the effect of the many variables that affect radiation dose rates and thus the allowable ^{232}U content of fuel in similar fabrication lines. A model was

developed to explain the Kilorod data and to provide a basis for extrapolating dose rates to other lines having a nominal capacity of 10 kg of oxide per day. Also, design parameters for conceptual ^{233}U -Th fuel-fabricating plants having capacities from 60 to 3700 kg of heavy metal per day were estimated.

Figure 1 shows some nuclei formed by the neutron irradiation of thorium. In particular, the chain leading to ^{232}U is shown. Figure 2 shows the decay chain for ^{232}U . Note that ^{232}U has a 74 year half-life so the amount present will increase proportional to the neutron irradiation. The first daughter, ^{228}Th has a 1.91 year half-life which is much longer than any of the other daughters, so the buildup of the decay radiation in this chain will be characterized by the ^{228}Th half-life. The ^{208}Tl daughter emits a 2.6 MeV gamma ray and contributes a large fraction of the gamma dose rate; it rapidly attains equilibrium. The ^{212}Bi nucleus emits a 2.2 MeV gamma ray. Figure 3⁽¹⁾ shows the calculated build up of activity as a function of time after separating out all the daughters from ^{232}U . After several years, the quantity of ^{228}Th approaches equilibrium, i.e., the rate of disintegrations of ^{228}Th approaches the decay rate of ^{232}U which produces ^{228}Th . Figure 4⁽¹⁾ shows some experimental results of the same data. Notice that 70 days after purification the activity is $\approx 7\%$ of the equilibrium activity. The initial decrease in activity is attributed to the decay of the 10.6 hr ^{212}Pb . (In the Kilorod Program the solvent-extraction process removed the ^{228}Th , ^{224}Ra , and ^{212}Pb daughters by factors of 2500, 5000, and 100 respectively.) Figure 5⁽²⁾ shows similar results.

Figure 3. Decay of ^{232}U and Buildup of Daughters With Time

Figure 1. Some Nuclei Formed by the Irradiation of Thorium; Reactions Leading to ^{232}U .

Figure 2. The ${}^{232}\text{U}$ Decay Chain

Figure 4. Calculated and Observed Growth of Gamma Activity of ^{233}U Material Containing About 42 ppm of ^{232}U .

Figure 5. The ^{208}Tl Activity from ^{232}U Decay as a Function of Postpurification Time, Method of Purification, and Aging Time Before Purification.

REFERENCES - APPENDIX D

1. Schileo, G., "An Unshielded Pilot Plant for Recycling ^{233}U ," Proceedings of the Second International Thorium Fuel Cycle Symposium, Gatlinburg, TN, May 3-6, 1966; AEC 12 Symposium Series "Thorium Fuel Cycle" Division of Technical Information (February 1968).
2. R. E. Brooksbank, J. P. Nichols, and A. L. Lotts, "The Impact of Kilorod Facility Operational Experience on the Design of Fabrication Plants for ^{233}U -Th Fuels," Proceedings of the Second International Thorium Fuel Cycle Symposium, Gatlinburg, TN, May 3-6, 1966; AEC 12 Symposium Series "Thorium Fuel Cycles" Division of Technical Information (February 1968).