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**Advanced Tokamak Reactors
Based on the Spherical Torus (ATR/ST)
Preliminary Design Considerations**

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ADVANCED TOKAMAK REACTORS BASED ON THE SPHERICAL TORUS (ATR/ST) Preliminary Design Considerations

by

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ABSTRACT

Preliminary design results relating to an advanced magnetic fusion reactor concept based on the high-beta, low-aspect-ratio, spherical-torus tokamak are summarized. The concept includes resistive (demountable) toroidal-field coils, magnetic-divertor impurity control, oscillating-field current drive, and a flowing liquid-metal breeding blanket. Results of parametric tradeoff studies, plasma engineering modeling, fusion-power-core mechanical design, neutronics analyses, and blanket thermalhydraulics studies are described. The approach, models, and interim results described here provide a basis for a more detailed design. Key issues quantified for the spherical-torus reactor center on the need for an efficient drive for this high-current (~ 40 MA) device as well as the economic desirability to increase the net electrical power from the nominal 500-MWe(net) value adopted for the baseline system. Although a direct extension of present tokamak scaling, the stability and transport of this high-beta (~ 0.3) plasma is a key unknown that is resolvable only by experiment. The spherical torus generally provides a route to improved tokamak reactors as measured by considerably simplified coil technology in a configuration that allows a realistic magnetic divertor design, both leading to increased mass power density and reduced cost.

1. EXECUTIVE SUMMARY

1.1. Introduction

In tracing the evolution of conceptual magnetic fusion reactor designs, the reduction of the fusion power core (FPC, i.e., plasma chamber, first wall, blanket, shield, coils, and related structure) and associated reactor plant equipment emerges as a major theme.^{1,2} As summarized in Ref. 3 and the following Sec. 1.2., a range of tokamak improvements and innovations was subjected to preliminary study as a means to achieve more attractive reactor systems. The four major contributors to the FY 1984-85 Tokamak Power Systems Studies (TPSS)³ and the respective approaches to improved tokamak reactors were Argonne National

Laboratory [second stability region (SSR) tokamak, STARFIRE-II], MIT (conventional Resistive-Coil Tokamak Reactor, RCTR), GA Technologies, Inc. (highly elongated tokamak, ET), and Los Alamos (spherical-torus tokamak, ST⁴⁻⁶); groups from TRW, Rensselaer Polytechnic Institute, and UCLA also contributed to the FY-1985 TPSS activity through the SSR, ST, and ECRH-heated tokamak approaches, respectively.

The degree of detail and focus varied among the TPSS groups, with the Los Alamos study of an Advanced Tokamak Reactor (ATR) based on the ST striving for a preliminary but self-consistent design with which to assess technological and economic promise. After a brief background statement on both the TPSS activity³ and the ST concept⁴⁻⁶ in Sec. 1.2., the scope of work is given in Sec. 1.3., the overall approach and methodology are described in Sec. 1.4., and key results are then given in Sec. 1.5. Details of the physics basis used in this study are given in Sec. 2., which describes equilibrium, stability, plasma simulations, current drive, and magnetic-divertor impurity control. An ATR/ST systems model was assembled and used to perform parametric and sensitivity studies; this model represents the cornerstone of this study and is described in Sec. 3. Adjustments to the systems model described in Sec. 3. were made in the course of preliminary engineering design and integration studies of key FPC subsystems, the model for and results from which are given in Sec. 4. Study conclusions are presented in Sec. 5. and summarized in Sec. 1.6. A detailed design table for two ATR/STs [500 and 1000 MWe(net)] can be found in Appendix A. Appendix B describes the cost database as well as a detailed cost breakdown, and Appendix C gives a general rationale for emphasizing fusion systems with increased FPC mass power density [MPD(kWe/tonne), ratio of net-electric power to FPC mass]. The oscillating-field current drive (OFCD) model is discussed in Appendix D.

1.2. Background

1.2.1. Tokamak Power Systems Studies (TPSS). The several groups contributing to the TPSS have examined a range of potential innovations that may improve the tokamak approach to magnetic fusion. The goal of the TPSS was to explore avenues for improvement made possible in the period since the last major tokamak design study.⁷ This exploration phase established a framework for a more detailed conceptual design of a tokamak reactor that incorporates a self-consistent set of these innovations and responds to the changing perceptions of that which constitutes an attractive, competitive magnetic fusion reactor.

A broad range of tokamak critical issues was considered as areas of performance improvement by the TPSS participants, including:

- ♦ Higher β limits: first versus second stability regimes, non-circular plasma cross sections; tailoring of current profiles.
- ♦ Steady state versus pulsed operation: inductive and non-inductive heating and current drives.
- ♦ Reactor size reduction: use of multiplexing to offset the economy of scale.
- ♦ Resistive versus superconducting coils: impact on reactor size and cost (coil cost versus recirculating power cost and reduced technology).
- ♦ Impurity control: self-pumping versus pumped limiter; cost-effective magnetic divertors.
- ♦ Safety and environment: inherent safety for loss of coolant accidents; limited wall loading; long-term radwaste storage (near-surface versus deep-geological burial).
- ♦ Reliability/availability/maintainability: long life versus periodic replacement; "patch" versus "batch" maintenance; technological complexity and repairability.
- ♦ First-wall/blanket/shield: self-cooled liquid metals, helium-cooled liquid metals, FLIBE, single- versus dual-media coolants (heat-load issue).

In addition to investigating these critical issues, several reactor design concepts were developed to achieve higher values of β , which was deemed an improvement in physics by all TPSS participants as crucial to achieving improved tokamak reactors. These concepts are summarized as follows:

- ♦ Spherical Torus (ST): high β in the first stability regime achieved with a large current flowing in a moderately elongated, low-aspect-ratio plasma.
- ♦ Elongated Tokamak (ET): high β in the first stability regime achieved by a highly elongated plasma.
- ♦ Resistive Coil Tokamak Reactor (RCTR): high β in the second stability regime, pulsed operation.
- ♦ Superconducting Tokamak: high β in the second stability achieved by fast-wave current drive in a high-aspect-ratio, low-current plasma.
- ♦ Electron Cyclotron Heated (ECH) Tokamak: high β achieved by tailoring the plasma-current profile.

Available parameters emerging from the several TPSS point designs are summarized in Table 1.2.-I.³

TABLE 1.2.-I

SUMMARY OF KEY PARAMETERS SUGGESTED FOR IMPROVED TOKAMAK REACTORS BY THE FY-1985 TPSS ACTIVITY

DEVICE	STARFIRE-II	RCTR	ET	ATR/ST	ST/IBC	TRV
Date	1985	1985	1985	1985	1985	1985
Plasma minor dimensions, a/b(m)	0.875/1.75	1.34/2.68	0.27/2.54	1.50/4.50	1.40/2.80	0.83/1.49
Plasma major toroidal radius, R_T (m)	5.25	6.72	2.73	2.70	2.10	3.7
Aspect ratio, $A = R_T/a$	6.0	5.0	10.0	1.8	1.5	4.46
Plasma volume, V_p (m ³)	150.	476.	46.6	358.	160.	90.
Average plasma density, $\langle n \rangle$ (10 ²⁰ /m ³)	1.38	1.5	4.0	1.21	1.11	2.0
Average plasma temperature, $\langle T \rangle$ (keV)	23.	17.	20.	15.	20.	17.
Plasma energy (GJ)	0.23	0.19	0.18	0.32	0.17	0.19
Field energy (GJ)	TBD	9.2	0.91	12.	1.1	TBD
Net electric power, P_E (MWe)	540.	1,100.	513.	500.	508.	600.
Total thermal power, P_{TH} (MWt)	1,559.	3,007.	1,768.	2,047.	1,670.	1,610.
Recirculating power fraction, $1/\eta_E$	0.23	0.19	0.38	0.32	0.22	0.09
Thermal conversion efficiency, η_{TH}	0.45	0.45	0.45	0.36	0.423	0.41
Net plant efficiency, η_p	0.35	0.37	0.29	0.24	0.33	0.37
Plant availability, p_f	TBD	0.75	0.70	0.76	TBD	TBD
Neutron first-wall loading, I_{wv} (MW/m ²)	3.4	3.2	7.0	3.3	5.5	5.3
Plasma power density, P_F/V_p (MW/m ³)	8.5	5.26	38.0	4.7	7.7	16.0
Average beta, $\langle \beta \rangle$	0.25	0.25	0.315	0.291	0.16	0.20
Field at plasma, B_p (T)	3.83	2.51	4.12	2.51	1.89	4.6
Field at coil, B_c (T)	6.0	3.57	6.82	6.9	7.9	TBD
Plasma thermal diffusivity, χ_E (m ² /s)	TBD	TBD	NeoAlcator	0.53	TBD	TBD
Plasma current, I_p (MA)	3.99	6.1	10.1	39.8	22.8	4.4
FPC volume, V_{FPC} (m ³)	TBD	2,332.	908.	2,102.	236.	TBD
FPC mass, M_{FPC} (tonne)	TBD	12,340.	4,461.	6,344.	641.	TBD
FPC power density, P_{TH}/V_{FPC} (MWt/m ³)	TBD	1.29	2.0	1.0	7.2	TBD
FPC mass utilization, M_{FPC}/P_{TH} (tonne/MWt)	TBD	4.10	2.5	3.1	0.43	TBD
Mass power density, $1000 P_E/M_{FPC}$ (kWe/tonne)	>150	90.	115.	79.	792.	TBD
FPC unit direct cost (\$/kg)	TBD	41.	205.	45.	TBD	TBD
(FPC Cost)/TDC	TBD	0.26	0.21	0.24	TBD	TBD
Unit direct cost (\$/kWe)	TBD	1,760. ^(a)	1,784. ^(a)	2,441. ^(b)	TBD	TBD
Construction time (years)	TBD	6	6	6	TBD	TBD
"Then-current" (date)	TBD	1991	1991	1986	TBD	TBD
Constant/then-current dollar COE (mills/kWeh)	TBD	44.6	44.5 ^(c)	61/116	TBD	TBD

^(a)1985 dollars^(b)1980 dollars^(c)8.7% capital recovery factor (constant dollars)

1.2.2. The Spherical Torus Concept.⁴⁻⁶ Central to the improvement of the commercial prospects of the tokamak is the need to increase the plasma beta, which in turn increases the plasma power density for a given magnetic field and/or reduces the coil magnetic field and current density. In some conditions, resistive coils, thin blanket/shield, and reduced FPC mass and volume become a possibility, as shown by early studies of the resistive-coil Compact Reversed-Field Pinch Reactor (CRFPR).⁸ Comparisons⁹ of MHD stability analyses with present experiments have correlated the maximum (critical) beta according to

$$\beta \approx (0.03-0.04)I_{\phi}/aB_{\phi V}(0), \quad (1.2.-1)$$

where I_{ϕ} (MA) is the toroidal plasma current, a (m) is the outboard minor radius of the plasma at the equatorial plane, and $B_{\phi V}$ (T) is the vacuum toroidal magnetic field at the plasma magnetic axis. Plasma equilibrium considerations⁴ for a given value of tokamak safety factor, q , predict

$$qI_{\phi}/aB \approx \frac{5(1.22 - 0.68\varepsilon)\varepsilon(1 + \kappa^2)/2}{(1 - \varepsilon^2)^2}, \quad (1.2.-2)$$

where $\varepsilon \equiv a/R_T = 1/A$ is the inverse plasma aspect ratio and $\kappa \equiv b/a$ is the plasma vertical elongation. Hence, conventional tokamak scaling would predict high-beta plasma if the aspect ratio is low ($A \leq 2$) and the elongation is high ($\kappa > 2-3$). This regime defines the ST, which strives for high beta in a high-current, low-aspect-ratio torus with moderate natural elongation. The ST is also characterized by strong toroidal-field paramagnetism [$B_{\phi}(0)/B_{\phi V}(0) > 1.5$] and $\beta_{\theta} \approx \beta \approx 0.25$. Figure 1.2.-1 compares a present-day tokamak plasma ($A \leq 4$, $\kappa = 1-1.5$) with the ST ($A \leq 2$, $\kappa \geq 3$) to illustrate the spherical nature of the latter.⁶ The advantages of high beta extrapolated from the existing tokamak database are countered by a serious disadvantage; high plasma currents must be generated and stably maintained in a torus where ε is sufficiently large to make difficult the use of the standard inductive-current startup while requiring large currents to be sustained in steady state. A preliminary but quantitative understanding of the tradeoffs between these and other advantages and disadvantages is the primary goal of this ATR/ST reactor study.

Fig. 1.2.-1. Comparison of a typical present-day ($A \leq 4$, $\kappa \approx 1.5$) and a proposed ST ($A \leq 2$, $\kappa \geq 2$) tokamak plasma.⁶

1.3. Scope of Work

As discussed in Sec. 1.2.2., an approach to the ATR based on the ST⁴⁻⁶ concept was considered in order to extend tokamak operation to a high-beta regime using an extrapolation of the existing experimental database by the use of low aspect ratio ($A < 2$) and significant vertical elongation ($\kappa = 2-3$); a first-stability-regime beta limit approaching 0.35 [relative to the central vacuum toroidal field, $B_{\phi V}(0)$] is predicted based on Troyon-Gruber scaling⁹ results. The perspective of this study, motivated by designs of the Compact Reversed-Field-Pinch Reactor (CRFPR),^{2,8} is the application of resistive-coil, higher mass-power-density (MPD) features to the ATR/ST reactor with a view to identifying cost-performance, availability, and development advantages^{1,8} (Appendix C) associated with state-of-the-art resistive-coil technology. Although this study is preliminary, many design tools have been adapted from

previous work on the CRFPR so that a common methodology could facilitate comparison of results.

Key areas of analysis have been organized to emphasize plasma and nuclear-engineering issues and interfaces, as is shown on Fig. 1.3.-1. Study activities included parametric systems analysis of key design variables (e.g., beta, aspect ratio, plasma elongation, unit costs, net electric power, neutron first-wall loading, degree of FPC multiplexing) leading to the determination of design points with minimum cost of electricity (COE) or MPDs in excess of a ~100-kWe/tonne threshold target suggested for commercially competitive fusion.¹⁵⁻¹⁷ Equilibrium, beta values, profile enhancement factors, divertor

Fig. 1.3.-1. ATR/ST study approach organized by key plasma and nuclear engineering activities and interfaces. NEQ,^{10,11} NEWLIT,¹² MCNP,¹³ and ZEPHYR¹⁴ are computer codes.

magnetics, and other key plasma parameters were calculated using a modified version of the ORNL equilibrium code NEQ^{10,11} as constrained by stability limits on beta.^{9,18}

A relatively short resistive decay time for the plasma and highly restricted room for an ohmic-heating-coil (OHC) set in the low-aspect ratio ATR/ST geometry make pulsed operation and inductive start-up difficult. Studies of RF-assisted, ohmically heated start-up were performed, leading to steady-state operation maintained by oscillating-field current drive (OFCD).^{2,19} Current drive based on lower-hybrid (LH) or Alfvén waves was also examined (Sec. 2.4.2.) to determine the degree to which the OFCD-based design would have to be reoptimized.

Preliminary FPC integration (e.g., FPC thermalhydraulics, mechanical layout, and assembly-and-maintenance approach) and supporting engineering studies (e.g., neutronics, divertor-chamber design and blanket thermalhydraulics) were performed for a medium-power 500-MWe(net) baseline case. The ATR/ST point design was used to address key physics and technology issues required to characterize this approach and, therefore, serves as a framework for future design-point selection and subsystem elaboration.

1.4. Approach/Methodology

The approach and methodology of this design study are described in Fig. 1.3.-1. A range of minimum-cost (COE) design points is determined by a comprehensive systems code (Sec. 3.1.), which, as shown in Fig. 1.3.-1, is continually updated with input generated from a range of neutronics, equilibrium, magnetics (divertor), thermalhydraulic, current-drive, and plasma-simulation models. Through these support calculations and the evolving FPC engineering design, the minimum-COE design point is continually adjusted and updated as the overall design evolved into a better defined engineering concept. The parametric systems analysis remains at the centrum of the overall design optimization, with cost impacts as well as key engineering and physics constraints also being inventoried and assessed.

The scope of the ATR/ST study did not allow a multitude of first-wall, impurity control, blanket/shield, or other options to be examined and compared. Quick choices in some areas had to be made in order to assure a reasonably self-consistent design would emerge by the end of the brief study period. These key choices and related rationale are summarized below.

- ♦ Resistive copper toroidal-field coils (TFCs): minimize FPC size and mass while invoking high-beta to minimize power recirculated to TFCs.
- ♦ Superconducting equilibrium-field coils (EFCs): an otherwise large power consumer, but located outside bulk of already reduced FPC.
- ♦ Double-null poloidal-field magnetic divertors: compatible with ST (naturally elongated) geometry and use of closely coupled, thinly shielded, radiation-hardened, resistive coils.
- ♦ Absence of ohmic-heating coils (OHCs): no room inboard for minimum aspect ratio and maximum beta.
- ♦ Single-piece maintenance: separate and independent and single-piece maintenance of the a) TFC central column, b) top/bottom divertor and associated TFC horizontal conductors, and c) outboard blanket/shield.
- ♦ Outboard breeding blanket only: adequate tritium breeding for self-cooled PbLi surrounding a moderately elongated ST plasma.
- ♦ Self-cooled liquid-metal breeding blanket: maintain open higher-power-density options, separately cooled first wall may be required for higher power densities.
- ♦ Thin (~ 0.2 -m) water-cooled coil shields: space restrictions, need to recover fusion power at temperatures of interest to power conversion.
- ♦ Integral vacuum vessel: open geometry enhances maintenance.
- ♦ Medium power output [500 MWe(net)]: more appropriate to early commercial (pilot) plant, more costly, scale to 1000-1200 MWe without significant FPC redesign by increasing neutron wall loading while maintaining integrated first-wall/blanket cooling.
- ♦ Oscillating field current drive (OFCD): consistent with highly paramagnetic plasma, but no obvious field-line reconnection mechanism, consider lower-hybrid (LH) or Alfvén-wave drive as option, use latter options for the startup transient.

1.5. Summary Results

Figure 1.5.-1 illustrates representative ATR/ST parametric results for a near-optimum aspect ratio, $A = 1.8$ (Sec. 3.2.1.). The COE (constant 1980 dollars, 6-year construction time) is shown as a function of plasma minor radius, a , that was varied in the range 0.5-3.0 m. For a given net electric power, P_E , and plasma minor radius, a , each point on Fig. 1.3.-1 is a minimum-COE design. The net reactor output power is varied from 250-1200 MWe and exhibits the nuclear economy of scale built into the cost database (Appendix B). The COE minima shown in Fig. 1.5.-1 are explained as follows. For low values of

Fig. 1.5.-1. Representative ATR/ST parametric results illustrating minimum-COE design points, economies of scale with increasing net power comparison with the STARFIRE design,⁷ and a 500-MWe design point adopted for further elaboration by this study.

a, space is restricted for the TFC centerpost, resulting in higher coil current density, increased Joule losses, higher recirculating power and, therefore higher COE. For large values of a, the physical size of the FPC increases resulting in lower values of 14.1-MeV-neutron first-wall loading, I_W (MW/m²) lower FPC power density, and higher FPC direct costs for fixed net-electric power output. Minimum-COE design points are indicated for each net-electric power, with 1200-MWe-class ATR/STs yielding COE values comparable to or below that for the STARFIRE design⁷; the minimum-COE ATR/ST design has a higher recirculating power fraction (0.25 compared to 0.17), but significantly smaller less-costly FPCs result. A 500-MWe(net) design point shown on Fig. 1.3.-1 was selected for further elaboration to characterize tokamak operation at moderate

unit sizes, with a $I_w = 5.7 \text{ MW/m}^2$ (peak) or 3.3 MW/m^2 (average) sufficiently low to be consistent with an outboard blanket using a flowing PbLi eutectic breeder/coolant material that also cools the outboard first wall. Figure 1.5.-2 shows the minimum-COE designs given on Fig. 1.5.-1 expressed as a function of the mass power density [MPD(kWe/tonne), Appendix C], defined^{16,17} as the ratio of net electric power to the FPC mass. As indicated by generic costing studies of fusion,^{15,22} for $\text{MPD} \geq 100\text{--}200 \text{ kWe/tonne}$, the FPC plays a reduced role in the overall cost equation. As addressed in Appendix C, however, operational and developmental incentives not directly incorporated into present costing methods (Appendix B) may exist for operating at even higher values of MPD.

Fig. 1.5.-2. Dependence of COE on mass power density and net power for the ATR/ST. A comparison with the CRFPR⁸ is shown. Lines of constant neutron wall loading are also shown. Design points for STARFIRE (1200 MWe),⁷ MARS (1200 MWe),²⁰ and MINIMARS (600 MWe)²¹ are also shown, all reduced to the 1980 constant-dollar basis.

Parameters of the 500-MWe baseline design are summarized in Table 1.5.-I together with a 1000-MWe design having similar size and geometry (i.e., higher I_w). Figure 1.5.-3 presents an isometric view of the ATR/ST fusion power core. The plasma is shaped by superconducting EFCs designed for a current density (15 MA/m² that includes insulation, dewar, and casing) and the resistive (copper-alloy) divertor-field coils (DFCs, 7.5 MA/m²) into a double-null divertor configuration. The resistive TFCs (N = 12, 0.2% on-axis field ripple, and 1.2% ripple at outboard edge of plasma, 70% copper conductor) are subdivided into segments using demountable joints to facilitate vertical assembly/maintenance. Resistive powers dissipated in the plasma, DFCs, outboard TFC legs, and TFC centerpost are, respectively, 14.9(2.0% of gross electric power), 36.8(5.0%), 36.0(4.9%), and 96.3(13.1%) MW, with an additional 7% of the gross electrical output allocated to auxiliary functions; a total recirculating power fraction $1/Q_E = 0.322$ results for this 500-MWe(net) design. It is emphasized that higher Q_E values for a given net-electric power are possible only at the expense of increased COE (i.e., operating to the right of the minima shown on Fig. 1.5.-1). Using the Troyon-Gruber beta scaling,³ the $I_\phi = 40$ -MA plasma current provides a toroidal beta of 0.29 relative to the square of the central vacuum toroidal field [$B_{\phi V}(0) = 2.30$ T] or a beta of 0.14 relative to the volume-average square of the paramagnetic toroidal field, [$\langle B_\phi^2 \rangle^{1/2} = 3.39$ T, $B_\phi(0)/B_{\phi V}(0) = 1.78$].

Plasma current sustainment is postulated to be provided by OFCD^{2,19} (Sec. 2.4.2.1.), noting the nearness of the ATR/ST plasma equilibrium to a minimum-energy Taylor state²³ as measured by the central value of $\mu(r) \equiv \mu_0 \int j \cdot B/B^2 \approx \text{constant}$. The particular mechanism for nonlinear internal flux conversion identified for the RFP, however, is thought²⁴ not to apply to the tokamak because of fundamental differences in q-profiles, and an as yet undiscovered relaxation mechanism that is non-disruptive remains to be identified for the tokamak. Should OFCD prove inapplicable to the ATR/ST, steady-state current drive based on RF waves [e.g., compressional Alfvén or lower-hybrid (LH) waves] would necessitate modest adjustment of the design point to somewhat higher temperature and lower density to facilitate efficient wave coupling (Sec. 2.4.2.2.); these adjustments result in a 10-15% decrease in (the already low) Q_E and an increase in COE that depends sensitively on the cost of RF power.

TABLE 1.5-I
ATR/ST PARAMETER SUMMARY

	<u>ATR/ST</u>	<u>ATR/ST</u>
	500.	1000.
Net electric power, P_E (MWe)		
Plasma minor dimensions, a/b (m)	1.50/4.50	1.50/4.50
Plasma major toroidal radius, R_T (m)	2.70	2.70
Plasma aspect ratio, $A = R_T/a$	1.8	1.8
Plasma volume, V_p (m ³)	358.	358.
Average plasma density, $n(10^{20}/m^3)$	1.21	1.63
Plasma temperature, T (keV)	15.	15.
Plasma energy, W_p (GJ)	0.32	0.43
Field energy, W_B (GJ)	12.	16.
Total thermal power, P_{TH} (MWt)	2,047.	3,710.
Recirculating power fraction, $1/Q_E$	0.32	0.25
Thermal conversion efficiency, η_{TH}	0.36	0.36
Net plant efficiency, $\eta_p = \eta_{TH}(1 - 1/Q_E)$	0.24	0.27
Plant availability, p_f	0.76	0.76
Neutron first-wall loading, I_w (MW/m ²)	3.24	5.87
Plasma power density, P_F/V_p (MW/m ³)	4.7	8.5
Average toroidal beta, β	0.291	0.291
Field at plasma, B_0 (T) ^(a)	2.30(4.10)	2.67(4.77)
Field at coil, B_c (T)	6.9	8.0
Plasma thermal diffusivity, χ_E (m ² /s) ^(b)	0.53	0.72
Plasma current, I_ϕ (MA)	39.8	46.2
FPC volume, V_{FPC} (m ³)	2,102.	2,120.
FPC mass, M_{FPC} (tonne)	6,344.	6,492.
FPC power density, P_{TH}/V_{FPC} (MWt/m ³)	1.0	1.75
FPC mass utilization, M_{FPC}/P_{TH} (tonne/MWt)	3.1	1.75
Mass power density, $1000P_E/M_{FPC}$ (kWe/tonne)	79.	154.
FPC unit cost (\$/kg)	45.	45.
(FPC Cost)/TDC	0.24	0.20
Unit direct cost, UDC(\$/kWe) ^(c)	2,441. ^(b)	1485.
Cost of electricity, COE(mills/kWeh) ^(c,d)	61.4/116.0	37.8/71.1

(a)Values in parentheses are on-axis paramagnetic fields.

(b)Defined as $\chi_E = (3/16)a^2/\tau_E$.

(c)1980 dollars.

(d)Constant/then-current dollars based on a 6-year construction time and a 76% plant availability.

Fig. 1.5.-3. Isometric view of the ATR/ST fusion power core showing the resistive, demountable window-frame toroidal-field coils (TFC), the superconducting equilibrium-field coils (EFC), and the resistive divertor-coil/chamber assemblies with maintenance access from above and below.

Startup of the ATR/ST benefits from significant ohmic heating, particularly if neoclassical, trapped-particle enhancement of the plasma resistance can be included. For the equilibrium current-density profiles computed for the ST plasma, this enhancement amounts to a factor of ~ 8.0 , excluding profile effects, which itself adds a factor ~ 2.8 and makes ohmic heating to ignition a possibility. Anticipating that a self-consistent calculation would suppress the current density in the cooler edge plasma, resulting in less enhancement of ohmic heating, calculations of RF-assisted start-up were performed (Sec. 2.4.1.), suggesting input powers below ~ 100 MW applied for up to 400 s could bring the plasma to steady-state operating conditions, assuming neo-Alcator transport scaling²⁵ as well as a soft beta limit on energy confinement imposed through χ_E to inhibit thermal runaway.

Operation with a magnetic-field divertor in present experiments generally gives cleaner discharges, better energy confinement, and improved first-wall protection.²⁶ Assuming $\sim 30\%$ of the power delivered directly to the plasma (~ 400 MW alpha-particle plus ~ 15 MW ohmic-heating power) is radiated to the plasma chamber walls and assuming an edge-plasma recycle coefficient of 0.9, the inboard and outboard divertor chambers (55 and 133 m³, respectively) can be designed to modest average surface heat loads of ≤ 1.5 MW/m² for the 500-MWe case. This divertor configuration results in $\sim 14\%$ loss of outboard blanket coverage and $\sim 13\%$ reduction in first-wall area. The choice of a double-null poloidal-field divertor was made in order to provide a) maximum heat-transfer surface area and b) better plasma vertical stability control. Subsequent estimates indicate a single-null poloidal divertor positioned at the top of the torus can also meet these needs, with the added benefit of eliminating the difficult bottom-side maintenance of the divertor channel/coil assembly. The present ATR/ST design represents the first recent attempt²⁷ to incorporate a divertor into a tokamak reactor configuration; the result at the level presented herein appears promising for the ST magnetics geometry.

The ATR/ST outboard blanket (thickness $\Delta b = 1.09$ -m, PbLi-cooled, 5 v/o HT-9 ferritic-steel structure) is composed of three poloidal channels containing flowing PbLi eutectic breeder/coolant (90% enriched ⁶Li). The tritium breeding ratio and neutron energy multiplication factor were calculated to be $TBR = 1.117 \pm 0.007$ and $M_N = 1.21$, respectively, by a three-dimensional neutronics code, MCNP.¹³ The peak power density in the blanket is estimated to be ~ 52 MW/m³. Approximately 5% and 22% of the fusion neutron power

($P_F = 1,338$ MW) is deposited in the TFC and associated inboard shield (thickness $\Delta s = 0.2$ m), respectively. Average neutron displacement rates in the TFC central conductor, shield, first wall, and DFC are 0.92, 18.9, 57.0, and 0.47 dpa/y, respectively. For a maximum structural temperature of 450°C and PbLi coolant velocity of ~ 2 m/s the MHD pressure drop in the blanket is estimated by means of a one-dimensional model to be ~ 0.3 MPa (42 psi), and thermal conversion efficiency is $\eta_{TH} \approx 0.355$, with $\sim 39\%$ of the thermal power emerging in the water-cooled inboard shield and divertor-chamber coolant circuits.

The FPC mass [i.e., coils (3,994 tonnes), drained blanket (167 tonnes), and shield (1,111 tonnes)] results in an estimated MPD of 95 kWe/tonne (79 kWe/tonne if $\sim 1,090$ tonne of support-structure mass is included) for the $P_E = 500\text{-MWe}(\text{net})$ base case. The largest FPC component masses are 739 tonnes for the TFC inboard-shield/centerpost assembly and 793 tonnes for the outboard-shield/blanket assembly, drained of coolant (1,716 tonnes). Few-piece maintenance of critical subsystems may be possible (i.e., TFC central column; PFC/divertor chamber/horizontal TFC conductor; outboard blanket/shield). A higher net-electric ATR/ST (i.e., 1,000-1,200 MWe) at nominally the same physical size and mass would improve the MPD, lower the recirculating power fraction, and reduce the COE; for $P_E = 1,000$ MWe, the average neutron wall loading would be increased to $I_w = 5.9$ MW/m² and the MPD would be increased to 154 kWe/tonne.

1.6. Conclusions and Recommendations

Although preliminary, the ATR/ST study has generated sufficient detail to make a number of interim conclusions. Generally, the ATR/ST design has shown that a resistive-coil tokamak can be competitive and possibly less expensive than a superconducting design like STARFIRE.⁷ This promising result points towards significant advantages of low-technology, demountable coils applied to a system that utilizes magnetic-divertor impurity control and single- or few-piece maintenance of the FPC. The following cautionary indications emerging from this study are listed to provide guidance for future work.

- ♦ Significant power is delivered to the water coolant ($\sim 39\%$), leading to decreased thermal efficiency ($\eta_{TH} = 0.36$) relative to the potential offered by the higher-temperature liquid-metal coolant ($\eta_{TH} > 0.4$).
- ♦ Higher COE results at low net power because of the economy of scale (a generic problem).

- ♦ No direct benefit is apparent from multiplexing FPCs based on resistive coils.
- ♦ Technology (radiation-hardened coils) and cost (i.e., Joule losses) disincentives for divertors must be balanced against physics performance incentives; this balance is judged as favorable for divertors in the ST geometry.
- ♦ By fission reactor standards²⁸, this resistive-coil FPC design is still large, but is smaller than the STARFIRE⁷ and MARS²⁰ designs by a factor of four.
- ♦ A number of critical plasma-engineering issues remain to be resolved for the low-aspect-ratio, high-elongation ST.
 - Stability beta limit (extrapolation of Troyon-Gruber scaling⁹)
 - Transport in a high- β plasma without auxiliary heating (applicability of neo-Alcator scaling²⁵)
 - Current drive by q-profile modification (OFCD)^{19,24}

A number of positive indications are apparent from this study and are listed as follows:

- ♦ Few-piece maintenance appears feasible for the TFC-central column/inboard-shield assemblies, divertor assemblies, and outboard blanket/shield assembly.
- ♦ Adequate tritium breeding appears possible from the outboard blanket alone.
- ♦ The MPD is improved over the earlier STARFIRE⁷ and MARS²⁰ designs (~ 200 kWe/tonne versus 50 kWe/tonne at the same net-electric power).
- ♦ Moderate magnetic fields exist at coils: 7.2, 4.9, and 6.9 T, respectively, at the TFC central column, DFCs, and EFCs.
- ♦ Attractive divertor performance is promised from a technological viewpoint (moderate first-wall and blanket perturbation, moderate heat flux, accessible, maintainable, low-field coils).
- ♦ Cost-optimized design requires plasma confinement at a level ($\tau_E \approx 0.7$ s, $\chi_E = 0.56$ m²/s) that is 1/3 Kaye-Goldston scaling²⁹ or 1/6 neo-Alcator scaling²⁵.
- ♦ Large plasma currents along with the strong neo-classical resistivity enhancement promises ohmic heating to ignition, although RF-startup will deliver even more power.

Given an efficient current-drive scheme (e.g., OFCD), the resistive-coil ATR/ST is more attractive in large net-power units ($\geq 1,000$ MWe), despite high beta values. The resistive-coil TFC central column is the major power consumer. Parametric systems analysis of superconducting ST systems suggests even larger

physical size and lower power density, with comparable COEs reflecting a tradeoff between Q_E and direct cost. The ATR/ST would derive economic benefit from higher neutron wall loadings. A number of influences were identified as driving the ATR/ST to the physical size and MPD listed in Table 1.5.-I and are listed as follows:

- ♦ Impurity control by optimal (minimum EFC current) double-null divertor design forces greater vertical elongation; a single-null divertor operating at higher heat flux should be examined.
- ♦ High-beta stability may require higher plasma triangularity, forcing somewhat higher aspect ratio and also impacting the divertor design.
- ♦ Use of integrally cooled first wall forces lower neutron wall loading, I_w ; even lower radiation fractions and increased values of I_w , possibly with separately cooled first wall, would benefit the design.
- ♦ If OFCD is not feasible, the use of fast-wave current-drive will require lower density and higher temperature plasmas and a reoptimization of the minimum-COE design point; such designs may be economically feasible if the cost of RF power is sufficiently low (≤ 1 \$/W).
- ♦ Cost, recirculating-power, and current-density tradeoffs limit mass power density for the ST tokamak; higher MPD and Q_E are possible at higher COE.

The main goal of this study is to assess the merit of further more-detailed study of the ST approach. Realizing that experimental evidence of the extrapolability of the present tokamak database to the ST regime is needed, this approach will benefit from continued study in the following areas:

- ♦ Optimize stability beta limits and configuration at somewhat higher plasma aspect ratio and triangularity.
- ♦ Emphasize systems with larger net power (~ 1000 MW_e), lower recirculating power ($Q_E > 4$), and higher neutron wall loading ($I_w \sim 6$ MW/m²).
- ♦ Examine the impact of a cryogenic TFC central column (presently the major power consumer).
- ♦ Investigate feasibility of a limiter impurity control as a possible way to reduce FPC size (i.e., plasma elongation) and ease the bottom-side torus maintenance scheme.
- ♦ Investigate a single-null magnetic divertor as a possible way to reduce FPC size and facilitate maintenance access.
- ♦ Continue resolution of a range of important engineering issues:

- MHD flow/pressure drop in blanket (particularly two-dimensional effects).
- Resolve limits of neutron wall loading imposed by integrally cooled first walls.
- Design in more detail high-current-density, remotely demountable TFC joints.
- Better model edge-plasma processes and continue engineering of divertor chamber.
- Engineer current-drive system with emphasis in both OFCD and alternative (fast-wave) current-drive schemes.
- Continue FPC neutronics optimization (central column and DFC lifetimes).
- Develop single-piece maintenance schemes for small, integrated FPCs.

2. PHYSICS BASIS

More so than cost and engineering constraints, the credibility of any reactor projection for magnetic fusion depends on the physics basis upon which the plasma behavior or its impact on key engineering systems surrounding the plasma is determined. This physics basis, expressed in terms of computational models used to define the optimal ATR/ST reactor designs, is described in this section. In order of presentation, the following areas are summarized: Equilibrium (Sec. 2.1.); Stability (Sec. 2.2.); Transport (Sec. 2.3.); Plasma Simulations (Sec. 2.4.1.); Current Drive (Sec. 2.4.2.); and Divertors (Sec. 2.5.). Figure 1.3.-1 gives the relationship between these models and related engineering and costing models used to describe optimal ATR/ST systems. Unless stated otherwise, SI units are used throughout this report, with plasma temperature expressed in keV.

2.1. Plasma Equilibrium

2.1.1. Models.

2.1.1.1. Plasma Equilibrium Magnetics. Ideal magnetohydrodynamic (MHD) equilibrium calculations were performed in order to establish the poloidal-field coil (PFC) locations and currents necessary to provide equilibrium to the ST plasma. In addition, the equilibrium calculations allow a preliminary stability analysis to be made through the application of a circular-plasma form of the Mercier criterion:³⁰

$$\frac{r}{4} \left(\frac{1}{q} \frac{dq}{dr} \right)^2 + \frac{2\mu_0}{B_\phi^2} \left(\frac{dp}{dr} \right) (1 - q^2) > 0, \quad (2.1.-1)$$

where p is the local plasma pressure, $q \equiv rB_\phi/R_TB_\theta$ is the safety factor, $\mu_0 = 4\pi(10)^{-7}$ H/m, B_ϕ and B_θ are the local toroidal and poloidal magnetic fields, and r and R_T are the minor and major plasma radii. From Eq. (2.1.-1) the general constraints of $dq/dr > 0$, $dp/dr < 0$, and $q(0) > 1$ are derived and applied to this study. It is recognized that Eq. (2.1.-1) is an approximate expression for the Mercier criterion for axisymmetric tokamaks with large aspect ratio, low beta, and circular cross section. The more general constraints would

be $dq/d\psi > 0$, $dp/d\psi < 0$ and $q(0) > 1$. Furthermore, the β obtained from the Mercier criterion will generally be optimistic and must further be evaluated in terms of ballooning and kink stability, as is discussed in Sec. 2.2.

The code NEQ^{10, 11} was modified and used to perform the equilibrium calculations. This two-dimensional model calculates free-boundary, ideal-MHD equilibrium in current-carrying diffuse pinch (tokamak) on a rectangular grid in the R-z plane using the method of successive over-relaxation. The NEQ code can accommodate plasmas with circular, elliptical, or triangular (bean) cross sections. The PFC set is simulated with filamentary coils from which peak fields at the PFC surface and the associated inductances can be estimated. Additions to NEQ were made in order to calculate a) profile-peaking factors for use in the parametrics systems code (Fig. 1.3.-1 and Sec. 3.1.1.); b) RFP-related reversal parameter, F, and pinch parameter, Θ , as well as the μ -profile for use in the current-drive modeling (Sec. 2.4.2.1.), where $F \equiv \bar{B}_\phi(a)/\langle B_\phi \rangle$, $\Theta \equiv \bar{B}_\theta(a)/\langle B_\phi \rangle$, $\mu(r) \equiv \mu_0 \vec{j} \cdot \vec{B}/B^2$, $\bar{B}_{\theta, \phi}(a)$ are last-closed-flux-surface-averaged quantities, $\langle B_\phi \rangle = \phi/\pi r_p^2$, r_p is a circularized plasma radius ($\approx a\sqrt{K}$), and ϕ is the toroidal flux in the plasma; c) field-line connection lengths for use in modeling the magnetic divertors (Sec. 2.5.); and d) magnetic fields beyond the equilibrium grid used for the plasma in order to model the MHD flow of the liquid-metal blanket (Sec. 4.4.). Each calculation is described in this section.

2.1.1.2. Profile Factors. The profile-peaking factors express the dependency of the plasma power densities for fusion, Bremsstrahlung, and ohmic dissipation on local values of the magnetic field, current density, particle density, and temperature. These profile factors are used in the zero-dimensional models that drive the systems model (Sec. 3.1.). Generally, the volume-averaged power density is given by

$$p_i \equiv \langle p_i(r) \rangle = f_i [\langle B(r) \rangle, \langle j(r) \rangle, \langle n(r) \rangle, \langle T(r) \rangle] , \quad (2.1.-2)$$

where the subscript "i" denotes fusion, radiation, or ohmic power densities. The systems model calculates volume-averaged power densities, p_i , using average parameters; all profile information is contained in a profile enhancement factors g_i , where

$$p_i = g_i f_i(B, j, n, T) . \quad (2.1.-3)$$

The average field, current density, plasma density, and plasma temperature used in Eq. (2.1.-2) are defined as follows:

$$B \equiv B_{\phi V}(0) , \quad (2.1.-4A)$$

$$j \equiv I_{\phi}/A_p , \quad (2.1.-4B)$$

$$n \equiv \frac{2\pi}{A_p} \int_0^{r_p} n(r) r dr , \quad (2.1.-4C)$$

$$T \equiv \frac{2\pi}{n A_p} \int_0^{r_p} T(r) n(r) r dr , \quad (2.1.-4D)$$

where I_{ϕ} is the toroidal plasma current, $B_{\phi V}(0)$ is the on-axis vacuum toroidal field, and A_p is the plasma minor cross-sectional area. The profile factors are then defined as

$$g_i \equiv \frac{2\pi}{p_i A_p} \int_0^{r_p} p_i(r) r dr . \quad (2.1.-5)$$

The calculation of the various profile factors requires a flux-surface average of a quantity "y" [e.g., $B(R,z)$ and $j(R,z)$], which is given by

$$y(\psi) = \frac{\oint_{\psi} y(R,z) d\ell}{\oint_{\psi} d\ell} , \quad (2.1.-6)$$

where $d\ell$ is an infinitesimal element of length along a poloidal flux surface contour in the R - z plane labeled ψ . The corresponding circularized minor radius, r , of the ψ flux surface is given by

$$r = [V(\psi)/2\pi R]^{1/2} , \quad (2.1.-7)$$

where V is the plasma volume contained within the surface and R is the major-radius of the centroid of the flux surface under consideration. The circularized radius and associated plasma volume of the last flux surface are designated as r_p and V_p , respectively. The expressions reported in Appendix A of Ref. 2 for the fusion, Bremsstrahlung, and ohmic power densities were used. In the calculation of the ohmic power profile-peaking factor, g_{OHM} , a neoclassical resistivity factor, $S(\epsilon)$, was introduced to account for the effect of trapped particles on the plasma resistance,³¹ where

$$S(\epsilon) = 1 + 0.65\epsilon - 0.5[(2 + 0.65\epsilon)^2 - 4(\epsilon - 1)^2]^{1/2} , \quad (2.1.-8)$$

and $\epsilon = r/R_T$. Figure 2.1.-1 gives a plot of Eq. (2.1.-8). In this case,

Fig. 2.1.-1. Enhancement of resistivity because of neo-classical trapped-particle effects.³¹

$$g_{OHM} = \frac{2\pi \int_0^{r_p} \eta_{||} [n(r), T(r)] [j_{\phi}^2(r) + j_{\theta}^2(r)] S(\epsilon) r dr}{\eta_{||}(n, T) I_{\phi A_p}^2}, \quad (2.1.-9)$$

where $\eta_{||}$ is the classical Spitzer resistivity. It is possible that this neo-classical correction cannot be extrapolated to the low-aspect-ratio ST regime. The local values of the magnetic field, current density, and pressure are determined by the NEQ code to complete the algorithm used to compute profile enhancement factors, g_i .

A low-frequency current drive mechanism has been suggested^{19,32} for RFPs, wherein low-amplitude oscillations of both poloidal- and toroidal-field components combine with the natural tendency for the RFP plasma/field configuration to relax to a $\beta = 0$, $\mu \equiv \mu_0 \vec{j} \cdot \vec{B} / B^2 = \text{constant}$ (force-free) Taylor minimum-energy state.²³ Analyses of this oscillating-field current drive (OFCD) or "F- Θ pumping" for RFP experiments^{19,24} and reactors² have been reported. In order to extend this concept to the highly paramagnetic ST tokamak plasma configuration (Sec. 2.4.2.1.), it is convenient to use the flux-surface-averaged results from the NEQ code to compute F , Θ , and $\mu(r)$, where

$$F \equiv \bar{B}_{\phi}(a) / \langle B_{\phi} \rangle \quad (2.1.-10)$$

$$\Theta \equiv \bar{B}_{\theta}(a) / \langle B_{\theta} \rangle \quad (2.1.-11)$$

$$\mu(r) / \mu(0) \equiv \bar{j}(r) \cdot \bar{B}(r) / \bar{j}(0) \cdot \bar{B}(0), \quad (2.1.-12)$$

again, with $\bar{B}_{\phi}$, $\bar{B}_{\theta}$, $\bar{j}(r)$ being flux-surface-averaged quantities. These expressions are used to estimate OFCD efficiencies for μ profiles derived from the ST geometry; these profiles when expressed as flux-surface averages are similar to those predicted for a Taylor-like near-minimum-energy state³³ in which $\beta > 0$. In this case, $\mu(r)$ is constant within the central plasma and decreases more-or-less linearly to zero near the cold plasma edge, where the highly resistive edge-plasma cannot support large current densities (Sec. 2.1.2.).

2.1.1.3. Field-Line Connection Lengths. Field-line connection lengths are required to estimate the divertor configuration and efficiency. The calculation of connection lengths first requires the location of the separatrix. The separatrix for a given coil configuration was located using the NEQ code by

tracing successive poloidal flux surfaces, starting at the equatorial plane on the outboard side; increments of 1% of the poloidal flux at the plasma surface were used in this search. The first flux surface that intercepts the equilibrium (plasma) grid boundary before closing upon itself was defined as the separatrix. The magnetic field and poloidal flux were calculated on a new computational grid used in the original calculation, which extends beyond the grid used in the equilibrium calculation to include the divertor coils and chambers; use of the same technique in the NEQ code for calculating fields and fluxes at grid points external to the plasma permitted the tracing of the separatrix into the divertor chamber up to the neutralizer-plate location. In addition, this method provides magnetic-field components for use in modeling the MHD flow of the liquid-metal blanket (Sec. 4.4.). The outboard scrape-off boundary was established by tracing the flux surface that is 40 mm beyond the intersection of the outboard leg of the separatrix with the equatorial plane; this surface defined the first-wall configuration outboard of the plasma. The flux surface traced for the inboard scrape-off passed through a point that was 40 mm further inboard of the TFC/shield central column in the equatorial plane. The connection lengths were calculated on flux surfaces located midway into these scrape-off layers for both inboard and outboard locations. The prescription for calculating the connection length, L , between top and bottom divertor throats is given by

$$L = 2 \int_{l_1}^{l_2} d\ell \left[1 + \frac{B_\phi^2}{B_R^2 + B_z^2} \right]^{1/2}, \quad (2.1.-13)$$

where a) l_1 denotes the intersection of the appropriate flux surface within the equatorial plane, b) l_2 denotes the point on the flux surface having the same height above the equatorial plane as the field nulls, which corresponds to the point where the flux surface enters the divertor chamber, and c) B_R , B_z , and B_ϕ are respectively the radial, vertical, and toroidal components of the magnetic field on the flux surface being tested. A second connection length between top and bottom divertor neutralizer plates is calculated using a prescription similar to that given by Eq. (2.1.-13); this latter calculation was used to determine the divertor chamber geometry.

2.1.2. Equilibrium Results. The initial estimates of the ATR/ST reactor configuration for $P_E = 500 \text{ MWe}(\text{net})$ were determined from the systems code using guidance provided from Ref. 4 for the equilibrium plasma shape. The NEQ code coupled with the previously described additions and modifications was then used to determine the optimum ST configuration in conjunction with the COE-minimizing parametric systems code (Fig. 1.3-1, Sec. 3.1.). The embodiment of the ST concept^{4,5} adopted for the ATR/ST study uses magnetic divertors for impurity control based on the positive experimental results obtained in present-day tokamaks.^{26,34,35} These experiments use a poloidal-field divertor in a closed configuration, which necessitates divertor coils being placed relatively close to the plasma; these coils, therefore, are interlocked with the TFCs. The PFC (i.e., EFCs and DFCs) configuration adopted for the ATR/ST study uses a minimal number of coils; a pair of DFCs and a pair of EFCs are positioned symmetrically above and below the equatorial plane (Fig. 1.5.-3). The use of this quadrupole shaping field, as opposed to the conventional dipole vertical field usually associated with a naturally elongated ST ($\kappa \approx 2$), increases κ and, therefore, beta as expected from conventional tokamak scaling [Eqs. (1.2.-1,2)].

The NEQ code was first used to determine the elongation for the ATR/ST coil configuration. The coil configuration used for determining the elongation is shown in Fig. 2.1.-2. The radial positions of the DFCs and EFCs were fixed at the positions shown in order to ensure neutral-to-positive plasma triangularity for reasons of stability (Sec. 2.2.) and to provide adequate room for blanket, shield, and TFCs. The height of the DFC location was maintained as a constant relative to the poloidal-field null, which provided an $\sim 1\text{-m}$ clearance between the field null and the DFC. The plasma diameter measured in the equatorial plane was maintained at $a(1 + \kappa) = 3 \text{ m}$. The vertical distance, z_1 , between the EFCs and the equatorial plane provides the principal means of determining κ and was used as the only independent variable in establishing the natural elongation, which is defined for present purposes as the elongation at which the PFC (i.e., EFC + DFC) currents are minimized. Reference 6 has adopted the characterization of "strongly elongated" for the quadrupole-field case. The value of the safety factor, q , at the circularized radius r_p was held approximately constant during the variation of z_1 to preserve the position of the plasma surface relative to the separatrix. The plasma pressure, toroidal flux profiles, and $B_{\phi V}(0)$ were adjusted to obtain $dq/dr > 0$ for $r \leq r_p$ and $q(0) \geq 1$. In addition, the poloidal beta was adjusted to maintain an

Fig. 2.1.-2. Schematic of the coil configuration used to determine the natural vertical plasma elongation for the ATR/ST.

approximately constant value of $\beta \equiv 2\mu_0 p / B_{\phi V}(0)$, which also satisfied the following interpretation⁴ of Troyon-Gruber scaling⁹

$$\beta \leq 0.033 I_{\phi}(\text{MA}) / B_{\phi V}(0) a . \quad (2.1.-14)$$

The results of the z_1 parameterization are summarized in Fig. 2.1.-3. The minimum PFC current occurs at the minimum obtainable EFC height, resulting in a natural elongation of $\kappa \approx 3$ for the ATR/ST. Lower values of z_1 and, hence, higher κ values were not possible because NEQ would not yield a converged solution or the q profile did not change monotonically with minor radius. In

Fig. 2.1.-3. The effect of varying EFC height on plasma elongation, κ , and EFC current. Natural elongation occurs at the minimum in PFC current.

varying z_1 , the plasma power, which is proportional to $(\beta B^2)^2 V_p g_{DT}$, could not be held constant without violating the constraints imposed on the q profile for the β values constrained by Eq. (2.1.-14). In order to preserve the plasma power, the plasma minor diameter and major radius would have to be changed approximately inversely with κ ; this variation would not affect the natural elongation, but the PFC current, I_{PFC} , becomes a more sensitive function of z_1 . A natural elongation of $\kappa = 3$ was reported in Ref. 6 for a similar quadrupole shaping field.

On the basis of the results shown on Fig. 2.1.-3, an elongation of $\kappa \approx 2.9$ and an aspect ratio of $A = 1.8$ were chosen. Table 2.1.-I gives a summary of the NEQ-generated equilibrium results. Some uncertainty exists as whether to use

the vacuum toroidal field or the paramagnetic toroidal field in extrapolating the Troyon-Gruber beta scaling to the ST regime. Both results are reported here, but it is emphasized that the plasma current, pressure profiles, size, and fusion power remain constant, irrespective of the choice of beta definition. Flux-surface, modulus-B, and vertical-field profiles are given in Fig. 2.1.-4 for this design point, whereas Fig. 2.1.-5 gives the ST flux surfaces and PFC locations. Profiles along the major radius and as a function of a flux coordinate, χ , for fields, currents, safety factor, pressure, and $\mu = \mu_0 \vec{j} \cdot \vec{B} / B^2$ are given in Figs. 2.1.-6. These results are as given directly from the NEQ code. For purposes of deriving profile averages and simulating OFCD, flux-surface averages expressed as a function of circularized minor radius prove more convenient; Figs. 2.1.-7 show these profiles. The results given on Table 2.1.-I and Figs. 2.1.-4 through 2.1.-7 are for conditions close to the 500-MWe(net),

TABLE 2.1.-I
ATR/ST PLASMA EQUILIBRIUM MODEL (NEQ) RESULTS

♦ INPUT	
- Major toroidal radius, $R_T(m)$	2.70
- Minor plasma radius, $a(m)$	1.50
- Vertical elongation, κ	2.9
- On-axis vacuum toroidal field, $B_{\phi V}(T)$	2.30
- Toroidal plasma current, $I_\phi(MA)$	40.0
- EFC/DFC positions (Fig. 2.1.-2)	
-- Radial position, $R_1/R_2(m)$	7.50/2.40
-- Elevation, $z_1/z_2(m)$	1.50/5.75
♦ OUTPUT	
- Triangularity, δ	+0.03
- On-axis safety factor, $q(0)$	1.12
- Edge-plasma safety factor, $q(a)$	2.25
- Volume-averaged beta relative to $B_{\phi V}(0)$, β	0.291
- Volume-averaged beta relative to paramagnetic $B_\phi(0)$	0.134
- Paramagnetic toroidal-field enhancement, $B_\phi(0)/B_{\phi V}(0)$	1.79
- Reversal and pinch parameters, F/Θ	0.75/0.47
- Troyon-Gruber parameter, $I_{NV} \equiv \mu_0 I_\phi / a B_{\phi V}(0)$	14.68
- Troyon-Gruber parameter, $I_N \equiv \mu_0 I_\phi / a B_\phi(0)$	8.2
- Plasma (hoop approximation) self-inductance, $L_p(\mu H)$	1.8

minimum-COE baseline design (Fig. 1.5.-3). These results represent considerable iteration between NEQ and the cost-optimizing systems model, as constrained by the Troyon-Gruber beta limits. The specific value of beta depends, however, on the field being evaluated, which for the ST leads to some ambiguity and possible confusion. The NEQ code has been used to compute a number of beta values, together with the newly added calculation of the reversal parameter, F, and the pinch parameter, Θ , which are summarized in Table 2.1.-II for a case near the ATR/ST design point.

TABLE 2.1.-II
FLUX-SURFACE-AVERAGED BETA AND OTHER VALUES
COMPUTED FOR ATR/ST USING THE NEQ CODE

NEQ FORTRAN VARIABLE	DEFINITION	VALUE
presva	$p = \langle \text{plasma pressure} \rangle$	1.23 MPa
betae	$\beta(\text{eng}) = 2\mu_0 p / B_{\phi V}^2(0)$	0.293
betax	$\beta(\text{max}) = 2\mu_0 p_{\text{max}} / B_{\phi V}^2(0)$	0.882
betaz	$\beta(\text{tor}) = 2\mu_0 p / B_{\phi}^2(0)$	0.092
betat	$\beta(\text{tot}) = 2\mu_0 p / B^2(0) $	0.089
betap	$\beta_{\Theta} = \beta(\text{pol}) = 2\mu_0 p / B_{\Theta}^2(a)$	0.225
betav	$\beta(\text{vol}) = 2\mu_0 p / \langle B_{\phi}^2 \rangle$	0.134
btor	$B_{\phi V}(0)$	2.30 T
btsavg	$\langle B_{\phi}^2 \rangle$	3.27 T
btsqavg	$\langle B_{\phi}^2 \rangle$	11.57 T ²
qsrf(msrf)	$q(\psi) = d\phi/d\psi$, edge safety factor	2.25
al	ℓ = plasma poloidal circumference	19.1 m
vprime	$V'(\psi) = 2\pi \oint dl / B_{\Theta}(a) = dV(\psi)/d\psi$	7.7 m/T
bpvprm	$B_{\Theta}(a) = 2\pi \ell / V'(\psi)$	1.55 T
btvprm	$\bar{B}_{\phi}(a) = \frac{2\pi}{V'(\psi)} \oint \frac{dl}{B_{\Theta}(a)} B_{\phi}(a)$	2.53 T
fpar	$F = \bar{B}_{\phi}(a) / \langle B_{\phi} \rangle$, reversal parameter	0.75
tpar	$\Theta = B_{\Theta}(a) / \langle B_{\phi} \rangle$, pinch parameter	0.47

Fig. 2.1.-4. Flux surfaces, modulus-B, and vertical-field profiles for the 500-MWe(net) ATR/ST reactor design with double-null poloidal divertors as determined from the NEQ code.

2.2. Plasma Stability

Maximizing the beta value is a key to economic competitiveness of the resistive-coil ATR/ST. The maximum allowable beta, however, is in large part determined by plasma stability. The principal concerns with respect to stability are a) ballooning modes, b) kink modes or free surface modes, c) vertical stability modes, and d) tearing modes. The ballooning and kink modes are considered the principal design constraints for the ST. The modified Troyon-Gruber limit⁹ is based on a ballooning instability and can be written as

Fig. 2.1.-5. Flux surfaces and coil locations for the 500-MWe(net) ATR/ST reactor design with double-null poloidal divertors as determined from the NEQ code.

$$\beta < 0.033 I_{\phi}(\text{MA}) / B_{\phi V}(0) a , \quad (2.2.-1)$$

where β is the maximum permitted volume averaged beta, $I_{\phi}(\text{MA})$ is the total current, $B_{\phi V}(T)$ the vacuum toroidal field at the center of the plasma, and $a(\text{m})$ the minor plasma radius in the equatorial plane. Using the values appropriate to the ATR/ST design [$I_{\phi} = 40 \text{ MA}$, $a = 1.5 \text{ m}$, and $B_{\phi V}(0) = 2.3 \text{ (4.1-T parametric field)}$] gives $\beta < 0.38$, which is greater than the 0.34 equilibrium-beta limit as well as the $\beta = 0.29$ design point. This stability limit, however, is somewhat

Fig. 2.1.-6A. Profiles in major radius of fields, current densities, safety factor, pressure, and μ for the 500-MWe(net) ATR/ST reactor design with double-null poloidal divertors as determined by the NEQ code.⁶

REFERENCES FOR APPENDIX D

1. R. L. Hagenson, R. A. Krakowski, C. G. Bathke, R. L. Miller, M. J. Embrechts, N. M. Schnurr, et al., "Compact Reversed-Field Pinch Reactors (CRFPR): Preliminary Engineering Considerations," Los Alamos National Laboratory report LA-10200-MS (August 1984).
2. K. F. Schoenberg and R. F. Gribble, "Oscillating Field Current Drive for Reversed-Field Pinch Discharges," Proc. IEEE Mtg. on Plas. Sci., Ottawa (May 17-19, 1982).
3. K. F. Schoenberg, C. J. Buchenauer, R. J. Massey, J. G. Melton, R. W. Moses, R. A. Nebel, and J. A. Phillips, "F- θ Pumping and Field Modulation Experiments on a Reversed Field Pinch Discharge," Phys. Fluids 27, 548 (1984).
4. M. K. Bevir and J. W. Gray, "Relaxation, Flux Consumption and Quasi Steady State Pinches," Proc. RFP Theory Workshop, Los Alamos National Laboratory, April 28 - May 2, 1980, Los Alamos National Laboratory report LA-8944-C, p.176 (January 1982).
5. M. K. Bevir, C. G. Gimblett, and G. Miller, "Quasi-Steady-State Current Drive by Plasma Relaxations," Proc. IAEA Technical Meeting on Non-Inductive Current Drive in Tokamaks, Culham Laboratory (April 1983).
6. J. B. Taylor, "Relaxation of Toroidal Plasma and Generation of Reversed Magnetic Field," Phy. Letts. 33, 1139 (1974).
7. J. B. Taylor, "Relaxation of Toroidal Discharges to Stable States and Generation of Reversed Magnetic Field," IAEA 5th Inter. Conf. on Plasma Phys. and Contr. Nucl. Fus. Res., Tokyo, Japan, 1, 161 (1974).
8. J. B. Taylor, "Relaxation and Magnetic Reconnection in Plasma," UKAEA Culham Laboratory report CLM-P765 (December 1985) (submitted to Revs. Mod. Phys.).
9. R. W. Conn, "The Reversed-Field Pinch as a Compact Fusion Reactor: A Program in Conceptual Reactor, Design, Technology Assessment, and Development Pathway Analysis," University of California at Los Angeles report PPG-969 (April 1986).

Fig. 2.1.-6B. Profiles in flux function, $x = (\psi - \psi_0)/(\psi_L - \psi_0)$, of toroidal-flux function, pressure functions, fields, and inverse aspect ratio for the 500-MWe(net) ATR/ST reactor design with double-null poloidal divertor as determined by the NEQ code.

Fig. 2.1.-7A. Flux surface-averaged profiles of current-density and field profiles for the 500-MWe(net) ATR/ST reactor design with double-null poloidal divertors as determined by the NEQ code.

Fig. 2.1.-7B. Flux-surface-averaged profiles of current density, pressure, safety factor, and μ for the 500-MWe(net) ATR/ST reactor design with double-null poloidal divertors as determined by NEQ code.

optimistic. This section addresses these issues, although stability limits expressed by Eq. (2.2.-1) are generally applied throughout this study.

2.2.1. Beta Limits Based on $n = 1$ Free-boundary Instability. The Troyon-Gruber beta scaling was suggested⁹ based on numerical optimizations imposed by the appearance of an $n = 1$ free-boundary instability neglecting the possible stabilizing effect of a conducting shell. The $n = 1$ free-boundary instability appears at a slightly lower β than the optimized ballooning limit. The Troyon-Gruber study is particularly useful in that published experimental data are used to compare both the parametric dependence and the absolute value of the β limit.

As shown in Fig. 2.2.-1, general agreement exists between the proposed scaling and the highest β achieved in tokamak experiments. For those experiments exceeding the upper limit depicted on Fig. 2.2.-1 (JFT-2, ISX-B, T-11) questions concerning the experimental measurement of β can be raised, as delineated in Ref. 9. The three experiments of interest to the ST study are PDX, ASDEX, and Doublet-III, since these experiments operate with divertors. These three experiments fall between the upper and lower ranges given by

$$\beta < (0.025-0.031) \mu_0 I_\phi / a B_{\phi V}(0) . \quad (2.2.-2)$$

The ASDEX experiment is closest to the ATR/ST design point in terms of stability, since the divertors are located directly above and below the plasma in both cases. The ASDEX plasma has low triangularity ($\delta \approx 0.1$), while PDX has low large triangularity ($\delta = 1.0$), as does Doublet-III. Designs with high elongation and low aspect ratio may require sufficient triangularity to achieve high beta values. This constraint would mean that the double-null divertor and x-point locations selected for the ATR/ST need to be moved further inboard. It appears implicitly that the Troyon limit using a coefficient of $\beta/I_N = 0.020$ applies to designs with low triangularity, while the $\beta/I_N = 0.025$ Troyon limit may apply to designs with high triangularity. For the $\beta/I_N = 0.020$ Troyon limit, the β limit for the ATR/ST reactor relative to $B_{\phi V}(0)$ is $\beta < 0.29$, which just equals the design value; use of the paramagnetic on-axis field, $B_\phi(0)$, would set the corresponding beta limit at 0.16, which must be used in conjunction with $B_\phi(0)$ rather than $B_{\phi V}(0)$ in evaluating plasma power density, with plasma power density of course being invariant.

Fig. 2.2.-1. Summary⁹ of discharges from a range of modern tokamaks giving record beta values expressed as a function of $I_N \equiv \mu_0 I_\phi / B_\phi a$. The data linked by an arrow are identical discharges that have been analyzed twice. The two horizontal lines at $\beta/I_N = 0.020$ and 0.025 delimit the range of maximum betas found in the numerical simulation.

2.2.2. Other Beta Scaling Relationships. In an effort to reduce the discrepancies between various scaling laws and numerical studies for more extreme geometries, a new beta scaling law has been proposed recently.^{18,36} This scaling law is based on a best fit comparison of extensive numerical studies of the $n = \infty$ ballooning instability using the ERATO code.³⁷ Both earlier and more-recent scalings are summarized below, where again $I_N \equiv \mu_0 I_\phi / a B_\phi(0)$.

Lausanne-Culham^{38,39} scaling:

$$\beta = 0.032 I_N, \quad (2.2.-3)$$

GA scaling:⁴⁰

$$\beta = \frac{0.27 \kappa^{1.2} (1 + 1.5\delta)}{q_a^{1.1} A^{1.3}} \quad (2.2.-4)$$

JAERI scaling:⁴¹

$$\beta = 0.30 \frac{\kappa^{1.5}}{A q_a} \left[1 + 0.9(\kappa - 1)\delta - 0.6 \frac{\kappa^{0.75}}{q_a} + 14 (\kappa - 1) (1.85 - \kappa) \frac{\delta^{1.5}}{q_a^4} \right] \quad (2.2.-5)$$

Nagoya (new) scaling:³⁶

$$\beta = 0.037 I_N (1 - b_1 I_N) , \quad (2.2.-6)$$

where

$$b_1 = 0.0517 \frac{A}{\sqrt{\kappa}} \left[1 - \frac{(\kappa - 1 + 0.05\delta^*)}{\sqrt{\kappa}} \left(\frac{1 + 1.5\delta^*}{\kappa} \right)^{2.5} \right] , \quad (2.2.-7)$$

q_a is the safety factor evaluated at the last flux surface, $\delta^* = c^*/a^*$ is the triangularity, and other parameters are further defined¹⁸ in Fig. 2.2.-2.

All scalings given above, with the exception of Lausanne-Culham scaling, include the effects of elongation and triangularity. According to Ref. 18, the Lausanne-Culham scaling overestimates the beta values of elongated configurations without sufficient triangularity, while the betas of strongly dee-shaped and indented configurations are under-estimated. The Nagoya scaling for ATR/ST design with $I_N = 8.2$, $b_1 = 0.048$ gives $\beta = 0.162$; this lower beta limit reflects the low triangularity in the ATR/ST design. Diverted plasmas may offer greater stability than the Nagoya scaling, however, because the separatrix

surface was not explicitly included. Furthermore, aspect ratios below two were not explicitly considered by the Nagoya group,^{18,36} with these results representing an extrapolation.

Figure 2.2.-3 plots both the upper and lower Troyon-Gruber limits [Eq. (2.2.-2) with coefficient equal to 0.02 and 0.025, respectively, reflecting the range on Fig. 2.2.-1], as well as the Nagoya scaling [Eq. (2.2.-6)] for a range of triangularity and the ATR/ST design point ($A = 1.8$, $\kappa = 3.0$). This

Fig. 2.2.-2. Generalized plasma shape and definition of geometric terms used in Eqs. (2.2.-2) - (2.2.-5).^{18,36} Indentation, $i \equiv d/a$, has not been considered for the ATR/ST.

Fig. 2.2.-3. Plot of Troyon-Gruber⁹ and Nagoya¹⁸ stability-related beta limits. The baseline ATR/ST design point is also shown.

design point is also indicated on Fig. 2.2.-3 for I_N evaluated using either the on-axis vacuum or paramagnetic toroidal fields.

2.2.3. Ballooning Stability in the Vicinity of the Separatrix. Further evidence of the importance of trying to position the divertors as far inboard as possible in order to increase triangularity is provided by the study of Bishop, et al.⁴² Using a model tokamak equilibrium, significant stabilization from the strong global shear near the separatrix was not found. Instead, marginal stability is controlled mainly by the poloidal position of the x-point. The approach used in Ref. 40 calculated the stability at a given flux surface in terms of equilibrium quantities given solely on that flux surface. Quantities in the expression for ballooning stability are expanded in terms of distance

from the separatrix flux surface and in powers of the inverse aspect ratio. An equation is obtained which determines the critical poloidal beta in terms of the current density and the shape of the flux surfaces.

Qualitative agreement of the limiting β with previous results was reported. These results apply for divertors directly above and below the plasma (i.e., ATR/ST, ASDEX); the average beta can be higher. For divertors located more towards the inboard side of the torus (PDX), the limiting beta near the separatrix increases to 0.37. The reason for the increase in limiting β as the divertors are moved inboard is that the field line lingers in the vicinity of the x-point as it traces out a flux surface. This behavior will improve stability if the lingering occurs in regions of good curvature (i.e., the inboard side of the torus); conversely stability will be worsened if the field-line lingers in regions of bad curvature (i.e., outboard side of the torus). The connection length between the inside and the outside of the torus is near the separatrix, since the poloidal field becomes very small in the neighborhood of the x-point.

2.2.4. Other Stability Considerations. Assuming stabilization is not provided by a conducting shell, the $n = 1$ external kink mode is the most unstable mode predicted for typical tokamak equilibria. For $q_a > 2$, beta is limited by the ballooning mode if the conducting shell is reasonably far from the plasma surface (i.e., $a_w/a \leq 1.5$, where a_w is the shell radius). For $q_a \leq 2$, the $n = 1$ external kink is unstable, even with a close-fitting shell. In this case, the external kink limits q_a to above 2, and the ballooning mode limits the attainable beta.

The use of large vertical elongation ($\kappa > 2$) and low aspect ratio has been proposed⁴³ to access $\beta > 0.15$ plasmas that are stable to all ideal MHD instabilities. A closely fitting conducting shell (primarily for vertical stability) and the absence of strong resistive MHD instabilities, however, may be required.

A related issue is associated with the means by which divertors allow the H-mode operation in neutral-beam-heated tokamak plasmas. Three possible phenomena have been proposed:⁴⁴ a) enhanced tearing mode stability caused by current profiles with pedestals at the separatrix; b) neoclassical transport within a banana width of the separatrix; and c) an electrostatic thermal barrier effect along magnetic field lines. Earlier work⁴⁵ points out the importance of flattening current profiles near mode rational surfaces for $q_a \geq 2$.

2.2.5. Impact of Stability Limits on ATR/ST Design. The lower Troyon-Gruber (low-triangularity) beta limit and the Nagoya scaling limit are, respectively, 0.291 and 0.162 for the ATR/ST design. Because of space limitations at the inboard side of the plasma, inward movement of the divertor was also limited; hence, for $A = 1.8$ only a triangularity of $\delta \approx 0.03$ was possible. To bring the equilibrium beta more into agreement with the Troyon-Gruber limit, therefore, the plasma current was increased to the $I_\phi = 40$ -MA design value.

An alternative remedy to increasing the plasma current would be to increase the major radius and form the plasma into a dee-shape so that the triangularity can be increased at the expense of increased aspect ratio. Although higher β values may be possible with a dee-shaped equilibrium relative to the highly-elongated low-triangularity equilibrium, the reactor cost may not be reduced significantly because of increased coil currents and reduced FPC mass power density. A dee-shaped equilibrium at higher aspect ratio, however, would allow for the inclusion of ohmic-heating coils, which may be important for startup (Sec. 2.4.1.).

Vertical stability also may present a concern to the ST. Since the plasma is strongly elongated, a large negative decay index of the poloidal field can lead to a strong destabilizing vertical force. Two divertor coils may be required to stabilize the vertical instability passively. Furthermore, double divertors may be desirable for reduced divertor heat fluxes. No evidence could be found in the literature, however, that a double-null poloidal divertor would provide added protection against the vertical stability compared to a single-null poloidal divertor, and the former design choice should be re-examined by future design studies.

In summary, the overall stability of the ST, its impact on the reactor design, and its connection with the present tokamak database is not well resolved. The combination of low aspect ratio and (efficient) poloidal divertors precludes high plasma triangularity and, therefore, may cause stability problems, as predicted by the high- n ballooning mode scaling suggested by the Nagoya group.^{18,36} These problems are considerably reduced, however, if the paramagnetic toroidal-field on-axis is used instead of the vacuum field. Lastly, the selection of the double-null poloidal divertor is made primarily on the basis of heat-flux considerations rather than control of the vertical stability.

2.3. Transport

As for most optimized fusion reactor designs, the specification of ignition and total power dictates the global energy confinement time. Following the cost-optimization procedure described in Sec. 3.1., therefore, an optimum confinement time, $\tau_E(\text{OPT})$, results. This confinement, however, must be reconciled with prediction made either from experiment or theory, $\tau_E(\text{PHYS})$, and if $\tau_E(\text{OPT}) \geq \tau_E(\text{PHYS})$ it is unlikely that the minimum-cost design will be allowed by physics constraints. Hence, the need for a physics-based transport scaling arises primarily in measuring either the credibility (i.e., ignition margin) for a given reactor design or suggesting areas where improved physics may be required. Expressions for $\tau_E(\text{PHYS})$ are also required for the transient (startup) plasma simulations (Sec. 2.4.) used to estimate the accessibility of the minimum-cost steady-state designs predicted by the systems code. For these purposes, a global physics scaling based either on Neo-Alcator⁴⁶ (NA) or Kaye-Goldston⁴⁷ (KG) results was used throughout this study. These scaling relationships are reproduced below

$$\tau_E(\text{PHYS/NA}) = 0.192(10)^{-20} R_T^2 n_e \quad (2.3.-1A)$$

$$\tau_E(\text{PHYS/KG}) = 5.341(10)^{-35} \frac{I_\phi^{2.95} R_T^{2.55} n_e^{0.62}}{B_\phi^{2.98} a^{3.93} \beta^{1.38} \kappa^{0.71}} \quad (2.3.-1B)$$

The expression for the Kaye-Goldston scaling given above has been increased by a factor of two over that reported in Ref. 47 in order to reflect an H-mode scaling expected for divertor operation.

If the $\beta/I_N = 0.035$ Troyon-Gruber beta limit as well as the Murakami density limit,⁴⁸ $n/10^{20} = 0.25/q_a j_\phi$, are used in Eqs. (2.3.-1), and the results expressed in terms of $\chi_E(\text{m}^2/\text{s}) \equiv (3/16)a^2/\tau_E$, the following relationships result

$$\chi_E(\text{NA}) = \frac{7.67 a \kappa^{0.71}}{A^2 q_a I_\phi} \quad (2.3.-2A)$$

$$X_E(\text{KG}) = \frac{12.81 q_a a^{1.64}}{A I_\phi^{0.6}}, \quad (2.3.-2B)$$

where I_ϕ is in MA units. The geometry dependence of X_E in both cases is a reflection of neoclassical effects, and both scalings show the benefits to confinement of high plasma currents.

2.4. Plasma Simulations

The engineering feasibility of the resistive-coil ST reactor (Sec. 4.) was evaluated on the basis of the minimum-cost design projected from the systems code (Sec. 3.); at the present level of a scoping study, detailed plasma simulation and associated design-point reconciliation were not performed. Nevertheless, modeling of the plasma startup, burn, and current-drive requirements of the baseline design (Table 1.5.-I) at the scoping level was performed in order to identify and quantify key problems and issues for future study. If possible, the baseline design was adjusted somewhat to accommodate concerns raised by these simulations, but a self-consistent realignment of the ATR/ST design point remains as future work. This section presents the physics basis for the plasma simulation as well as interim results for the baseline design point [Table 1.5.-I, $P_E = 500 \text{ MWe}(\text{net})$].

2.4.1. Startup/Burn. The baseline design derived from the systems code (Sec. 3.1.) uses a steady-state plasma model. The power and transient plasma parameters required to access this steady-state can be determined only through time-dependent simulations. These startup studies were performed at two levels: a) scoping calculations using a simplified plasma model (Sec. 2.4.1.1.) and b) detailed calculations using a coupled plasma/circuits code (Sec. 2.4.1.2.) originally developed for other reactor studies.² The models and interim results for both startup simulations are reported in this section.

2.4.1.1. Scoping Calculations.

Model. A simplified plasma model was written to simulate the ATR/ST startup using either oscillating-field (OFCD) or fast-wave (FWCD) current drive with the intent of determining the RF current drive, auxiliary heating, and other requirements for startup. A series of studies using that code was carried out on an interim design similar to that reported in Table 1.5.-I

[$n = 1.27(10)^{20}$ instead of $1.21(10)^{20} \text{ m}^{-3}$; $I_\phi = 40.9$ instead of 39.8 MA ; $P_F = 1,875$ instead of $1,682 \text{ MWt}$].

The startup model treats the plasma as two separate but interacting electron and ion fluids. First, the design temperature, T_0 , density, n_0 , and current, $I_{\phi 0}$, as well as a characteristic startup time, τ_0 , are specified. The details of the circuit and plasma/circuit interaction are lumped into the time constant τ_0 . Fueling and current ramp-up algorithms are then selected. The specific startup mode is determined by the heating source, power requirements, and overall cost, each being determined by considerations of plasma equilibrium, stability, and transport previously described. The following current ramp-up and fueling algorithms were used:

$$I_\phi = I_{\phi 0} \sin(2t/\tau_0) \quad 0 \leq t < \frac{\pi}{4} \tau_0 \quad (2.4.-1)$$

$$I_\phi = I_{\phi 0} \quad t > \frac{\pi}{4} \tau_0$$

$$n = n_0 (T_i/T_0)^{0.75} \quad T_i < T_0 \quad (2.4.-2)$$

$$n = n_0 \quad T_i \geq T_0$$

A minimum rate of electron temperature rise may also be specified as either a constant, such as T_0/τ_0 , or as a function of the reactor conditions. If intrinsic plasma heating (i.e., current-drive heating plus fusion alpha-particle power) is insufficient to guarantee ignition, this minimum condition on the rate of electron-temperature increase, $\dot{T}_e$, provides a criterion for calculating the required auxiliary-heating power. For all cases, ECRH is assumed to provide auxiliary power. On the other hand, if it is certain that intrinsic heating will suffice for ignition, as is generally the case if an FWCD is used, then the plasma temperature is simply allowed to rise at the corresponding self-determined rate.

The following electron and ion power-balance equations are solved:

$$\dot{T}_e = \frac{f_{\alpha e} P_{\alpha} + P_{CUR} + P_{ECRH} - P_{BR} - P_{he} - P_{e \rightarrow i}}{C} - \frac{T_e}{\tau_{Ee}} \quad (2.4.-3)$$

$$\dot{T}_i = \frac{f_{\alpha i} P_{\alpha} + P_{e \rightarrow i} - P_{hi}}{C} - \frac{T_i}{\tau_{Ei}}, \quad (2.4.-4)$$

where $C = 1.5nk_B V_p$, $k_B = 1.602(10)^{-16}$ J/keV, V_p is the plasma volume, and A_p is the plasma cross section. Table 2.4.-I gives the expressions used for each term in the above equations. Generally, $Z_{eff} = Z_{AV} = 1$ was assumed, the classical Spitzer resistivity was enhanced by trapped-particle effects [Eq. (2.1.-3)], and the plasma inductance, L_p , was assumed constant at the value reported by the NEQ code (Sec. 2.1.). All profile factors, g_j ($j = BR, DT, OHM$) were determined as described in Sec. 2.1.1.2. In the expression for the ohmic-heating profile enhancement factor,

$$g_{OHM} = \frac{\eta_{NC}(j_{\phi}^2 + j_{\theta}^2) dV_p}{\eta(T_e)(I_{\phi}/A_p)^2}, \quad (2.4.-5)$$

the neoclassical resistivity, η_{NC} , is given in Ref. 31 and approximated by the following expression:

$$\frac{\eta(T_e)}{\eta_{NC}} = 1 + 0.7 - 0.5[(2 + 1.3\varepsilon)^2 - 4(\varepsilon - 1)^2]^{0.5}, \quad (2.4.-6)$$

where $\varepsilon = r/R$ is the local inverse aspect ratio and $\eta(T_e)$ is the Spitzer classical resistivity evaluated at the density-weighted average electron temperature. For the ST, $0 \leq \varepsilon \leq 0.6$, and strong neoclassical effects are predicted, as is seen from Fig. 2.1.-1. The results given on Fig. 2.4.-1 are for both Neo-Alcator and Kaye-Goldston transport scalings [Eqs. (2.1.-1)]. Because the RF power required to drive the plasma current provides a heating source greater than that required for ignition by either Kaye-Goldston or Neo-

TABLE 2.4.-I

EXPLANATION OF TERMS USED IN ELECTRON AND ION ENERGY EQUATIONS(a)

Electron/ion equilibration, $P_{e \rightarrow i} = C(T_e - T_i)/\tau_{eq}$

$$n\tau_{eq} = 1.186(10)^{18} T_e^{3/2}/Z_{eff}^2$$

$$Z_{eff} = \sum n_j Z_j^2 / \sum n_j Z_j$$

Cold particle heating, $P_{he,i} = C T_{e,i} \dot{n}$

Bremsstrahlung radiation, $P_{BR} = g_{BR} 5.37(10)^{-37} Z_{eff}^2 n^2 T_e^{1/2} V_p$

$$g_{BR} = 1.48 \text{ (profile, Sec. 2.1.)}$$

$$Z_{AV} = \sum n_i Z_i / \sum n_i$$

Alpha-particle power, $P_\alpha = g_{DT} (n^2/4) \langle \sigma v \rangle_{DT} 5.6(10)^{-13} f_\alpha V_p$

$$f_\alpha = 1. \text{ (trapping fraction)}$$

$$g_{DT} = 1.68 \text{ (profile, Sec. 2.1.)}$$

$$f_{\alpha e} + f_{\alpha i} = 1$$

$$f_{\alpha e} = \exp[-0.026 T_e] + 0.002 T_e$$

Electron/ion energy confinement, $\tau_{Ee,i}$

$$\tau_{Ei} = 4\tau_{Ee}$$

$$(T_e + T_i)/\tau_E = T_i/\tau_{Ei} + T_e/\tau_{Ee}$$

$$\tau_E = \tau_E(\text{PHYS/NA}) \text{ or } \tau_E(\text{PHYS/KG}) \text{ from Sec. 2.3.}$$

Heating from current drive, P_{CUR}

$$\spadesuit \text{ OCFD, } P_{CUR} = P_{OHM}$$

$$P_{OHM} = g_{OHM} I_\phi^2 3.3(10)^{-8} Z_{eff} V_p / A_p T_e^{3/2}$$

$$g_{OHM} = 22.3 \text{ (profile plus neoclassical effects)}$$

$$\spadesuit \text{ FWCD}$$

$$P_{CUR} = P_{RF} - P_{IND}$$

$$P_{RF} = f(n,T) [I_\phi + (L_p/R_p) \dot{I}_\phi]$$

$$P_{IND} = L_p I_\phi \dot{I}_\phi$$

$$f(n,T) = C_1 n R_T / T_e, \text{ where } C_1 = 8.9(10)^{20} \text{ for Alfvén waves} \\ \text{and } 13.5(10)^{20} \text{ for lower-hybrid (LH) waves}^{49}$$

(a) SI/keV units are used throughout this table.

Alcator scaling, the RF requirements for ignition are essentially independent of the scaling law.

The power delivered to the electrons by the current-drive mechanism is P_{CUR} . In the case of OFCD, $P_{OHM} = g_{OHM} n(T_e) j_\phi^2 V_p$, where $j_\phi \equiv I_\phi / A_p$ and g_{OHM} is calculated as described above and in Sec. 2.1.1.2. Any reactive power associated with OFCD is not included in P_{CUR} . In the case of FWCD, the RF power, P_{RF} , drives a current $I_{RF} \equiv P_{RF} / f(n, T)$, where $f(n, T)$ is given in Table 2.4.-I for Alfvén and lower-hybrid waves. Since I_{RF} must supply both a resistive and inductive component during startup,

$$\begin{aligned} P_{RF} &\equiv f(n, T) I_{RF} = f(n, T) [I_\phi + (L_p / R_p) \dot{I}_\phi] \\ &= (P_{OHM} + P_{IND}) f(n, T) / I_\phi R_p , \end{aligned} \quad (2.4.-7)$$

then $P_{CUR} = P_{RF} - P_{IND}$, with the parameter $f(n, T) / I_\phi R_p$ generally in the range 3-10; this latter ratio expresses the intrinsic inefficiency of FWCD. Similar inefficiencies may also exist for the (unproven) OFCD if the resistive "anomaly" factor g_{OHM} is increased above that estimated by Eq. (2.4.-6). Furthermore, whereas the efficiency of delivering P_{RF} to the plasma can be estimated for FWCD, the generally large reactive power that may have to be carried by the OFCD system, and the cost of maintaining high-Q drive-circuit operation for that otherwise low-technology system remain to be determined.

Equations (2.4.-3) and (2.4.-4) are solved for $\dot{T}_e$ and $\dot{T}_i$, with P_{ECRH} set equal to zero. If $\dot{T}_e$ is greater than a minimum limit, P_{ECRH} remains zero and the plasma temperature rises according to $\dot{T}_e$ and $\dot{T}_i$ as calculated. If $\dot{T}_e$ is less than a minimum limit, then $\dot{T}_e$ is set equal to that limit, and Eq. (2.4.-3) is used to determine the required amount of ECRH power needed to allow such a temperature rise. The plasma temperature is then advanced in time, and the startup simulation continues until $t > \tau_0$. Both Neo-Alcator [Eq. (2.3.-1A)] and Kaye-Goldston [Eq. (2.3.-1B)] scalings give confinement times greater than that required for stable ignition in the ST configuration. Specifically, $\tau_E = 0.7$ s for stable ignition, whereas $\tau_E(\text{PHYS/KG}) = 1.88$ s, and $\tau_E(\text{PHYS/NA}) = 4.6$ s. In this case, the plasma would be super-ignited, with temperatures and β values rising to unrealistically high values. It is assumed, therefore, that a critical β value, β_c , exists above which confinement degrades. Specifically, $\tau_E = \tau_E(\text{PHYS})$ for $\beta < \beta_c$ but $\tau_E = \tau_E(\text{PHYS})(\beta_c / \beta)^3$ for $\beta > \beta_c$. In order for the

ATR/ST reactor simulation to settle at the $T \approx 15$ keV and $\beta = 0.29$ design point, $\beta_c(\text{NC})$ was set at 0.157 and $\beta_c(\text{KG})$ was set at 0.21.

Results. A series of simulations were made to study the ATR/ST reactor startup. Density was increased in proportion to $T^{0.5}$ and $T^{0.75}$, current was ramped-up in accord with Eq. (2.4.-1), and characteristic times were chosen between $\tau_0 = 120$ and 1000 seconds.

Given $g_{\text{OHM}} = 22.26$ (Table 2.4.-I), no case was found in which any auxiliary heating was required beyond the FWCD or OFCD power needed to drive the plasma current. The fact that even OFCD current-drive heating (i.e., only ohmic heating) is sufficient to ignite the ST reactor is a result of the large plasma

Fig. 2.4.-1. Peak startup power requirements as a function of startup time, current-drive mechanism, fueling algorithm ($n \propto T^v$), and transport scaling (no difference between NA and KG scaling).

current density combined with the factor of ~ 7 increase in resistivity predicted by neoclassical theory (Fig. 1.5.-1).

Because of the uncertainty involved in neoclassical theory, a study was undertaken to determine the sensitivity of this result to variation in g_{OHM} . The results are displayed in Fig. 2.4.-2. If g_{OHM} is decreased to a minimum value of 2.8 (i.e., no neoclassical effects), a $\tau_0 = 500$ -s startup using OFCD requires 19 MW of ECRH for Neo-Alcator scaling or 6 MW for Kaye-Goldston

Fig. 2.4.-2. Peak startup power requirements as a function of ohmic-heating profile factor ($g_{OHM} \approx 22.3$ for full neoclassical effect and 2.8 for no neoclassical effect) as a function of startup time for OFCD.

scaling. For an OFCD startup without ECRH, a value for g_{OHM} of ~ 19 is required under Neo-Alcator scaling, or $g_{OHM} \geq 9$ under Kaye-Goldston scaling. The fact that the Kaye-Goldston scaling gives more favorable ignition results than the Neo-Alcator scaling results from the inverse dependence on β , which gives excellent confinement times at the low temperatures where ECRH tends to be required.

Figure 2.4.-3 gives a typical transient for an LH-wave startup using the

Fig. 2.4.-3. Typical transient for LH startup based on baseline 500-MWe(net) design.

500-MWe(net) baseline design. If FWDC using LH waves were required to maintain the plasma current, the expense of the added recirculating power, as measured in terms of direct capital and recirculating power costs, would lead to a design point re-optimization giving higher plasma temperatures and lower density; this issue is explored quantitatively in Sec. 2.4.2.2. On the basis of this scoping calculation of the ATR/ST startup requirements, the following general conclusions are made.

- ♦ For $g_{OHM} \geq 10$, ohmic heating alone is sufficient for ignition, but
- ♦ RF current-drive startup provides significant heating that is sufficient for ignition for a range of transport scaling
- ♦ The fueling algorithm can reduce peak RF power requirements, with $n \propto T_i^V$ and $v \sim 1$ being preferred
- ♦ Startup times below $\tau_0 \approx 200$ s require unrealistically large RF power levels
- ♦ It is possible to optimize n and T to minimize RF power requirement for both startup and current-sustenance phases (Sec. 2.4.2.2.)
- ♦ Approximately 10 m^2 or $< 3\%$ of outboard first-wall area is required to supply peak RF power needed for startup

2.4.1.2. Detailed Plasma/Circuit Simulation. A detailed time-dependent simulation of the ATR/ST couples the external electrical driving circuit to a profile-averaged zero-dimensional plasma model. This model was originally developed for the CRFPR² and is applied here to the ATR/ST baseline case. The analysis of the poloidal-field circuit described in Sec. 2.1. is used. This analysis includes the plasma as a nonlinear circuit element. Plasma profiles and transport properties are used to generate self-consistent plasma equilibrium configurations that are adjusted as the DT/DD-fueled burn proceeds. The concentration and energies of species from the constituent reactions (H, ³He, ⁴He, T) are followed in time with a Fokker-Planck slowing-down formulation.

The RFP plasma simulation code used to model the ATR/ST accounts for effects related to magnetic-field and plasma profiles by performing integral averages over the plasma cross section at selected time intervals. Magnetic field, $\vec{B}(r)$, and current-density profiles, $\vec{j}(r)$, are calculated using the Maxwell equations, these profiles being computed self-consistently with input plasma density, temperature, and $\mu(r)$ profiles. With these one-dimensional equilibrium relationships, profile form factors are derived numerically and used to simulate a one-dimensional "point-model" code.

The burn model provides the following specific information²:

- ♦ Time history of currents, voltages, powers, and energies in the poloidal-field-coil (PFC) and toroidal-field-coil (TFC) circuits, with the PFC circuit driving the burn computation. Applying appropriate circuit switching sequences and voltages while incorporating the plasma as a nonlinear circuit element allows an accurate simulation of reactor operation.
- ♦ Self-consistent, one-dimensional (cylindrical) plasma equilibria using experimentally derived plasma density, $n(r)$, temperature, $T(r)$ and current-density [i.e., $\mu(r)$] profiles. Profile updating is performed whenever the plasma beta or boundary conditions have significantly changed over a given period of time.
- ♦ Complete time history of plasma properties is generated, including parameters that are constrained to satisfy stability requirements (particularly β_0 , θ , and F). This calculation includes profile averaging of all plasma quantities versus time for a DT- or DD-fueled burn.
- ♦ Temperature and density fractions of all energetic ionic species are determined. These non-Maxwellian superthermal plasma species (H, D, T, ^3He , and ^4He) are described by a Fokker-Planck formalism with the Maxwellian component being subtracted from each slowing-down distribution and merged into a single background ion population.
- ♦ Complete reactor energy balance and a listing of all system energy requirements that culminate in a final expression for the engineering Q-value, Q_E , or recirculating power fraction, $\epsilon = 1/Q_E$, are computed. Results are displayed for a pulsed-plasma system, as directly simulated by the computer code and assuming the reactor performance at code termination represents a steady-state burn configuration.
- ♦ The option exists to model the reversed-field pinch, spheromak, tokamak, EBT, or stellarator assuming appropriate experimental temperature, density, and current profiles are available. If the plasma minor cross section is noncircular, these profiles must be mapped into a circular surface having an effective circular cross section equivalent to the actual surface being modeled.
- ♦ A simultaneous, time-dependent thermal response of the first wall during startup, burn, and plasma quench is generated. This one-dimensional heat-transfer calculation is made for a two-region first wall (i.e., coated or tiled surfaces) that offers a variety of coolant choices.
- ♦ A time-dependent mechanical/structural response of the first wall is computed simultaneously with the burn simulation, producing a one-dimensional stress history of the same structure used in the above-mentioned heat-transfer calculation.
- ♦ A file is created for interactive use with a standardized reactor costing code (Appendix B).

Table 2.4.-II lists the ATR/ST starting conditions, geometry, and electrical parameters. The Neo-Alcator transport scaling given by Eq. (2.3.-1) was used with a soft beta limit [i.e., τ_E reduced by $(\beta_c/\beta)^3$ if $\beta > \beta_c$, where $\beta_c = 0.131$]. The steady-state field and current-density profiles to which the simulation evolved are given in Fig. 2.4.-4. The evolution of the ATR/ST startup transient on the F- Θ diagram is shown on Fig. 2.4.-5 along with a number of near-minimum-energy trajectories computed for the RFP; the significant paramagnetic steady-state burn value results in $F = 0.76$ and $\Theta = 0.47$. Figure 2.4.-6 gives the voltages and currents required to achieve the ATR/ST steady-state, whereas Fig. 2.4.-7 gives the plasma temperature, density, and beta transients. Lastly, the conditions of the final steady plasma are summarized in Table 2.4.-III.

2.4.2. Steady-State Current Drive. At full current the baseline ATR/ST design supports $W_M = 12$ GJ and $L_p I_\phi = 71.9$ Wb; given that these conditions can be achieved by the means discussed in Sec. 2.4.1., an inductive pulse-burn would sustain the burn for only $L_p/R_p \approx 200$ s. Combined with considerations of total power balance, thermal fatigue, and the cost of pulsed power and thermal energy storage, the low-aspect-ratio characteristics of the ST forces serious consideration of steady-state current drive. The baseline design assumes the efficacy of OFCD at a level suggested for the RFP.^{2,19,32} The strong paramagnetism of the ST equilibrium configuration, resulting in RFP-like μ profiles (Fig. 2.1.-7B) and a position on the Taylor F- Θ diagram (Fig. 2.4.-5) which has non-zero slope, provides a base for further investigation of this assumption. Although the ATR/ST design point has been optimized on the basis of efficient OFCD, the impact of FWCD based on either Alfvén or lower-hybrid waves (as for the startup, Sec. 2.4.1.1.) on the ATR/ST design point is also subject to preliminary examination.

2.4.2.1. Oscillating-Field Current Drive (OFCD). Inductive but oscillatory (i.e., not consumptive of electromagnetic flux) means of steady-state current drive have been proposed for the RFP.^{2,32} Unlike the conventional tokamak, the toroidal and poloidal currents in the RFP are closely coupled, and it is possible in principle to emulate a time-average toroidal voltage within the plasma without requiring a time-varying magnetic flux on the average to link the torus. It is proposed that a near-minimum-energy relaxed state may also exist for the highly paramagnetic ST, and a relaxation mechanism may exist that could also be used to drive net current. The minimum-energy RFP state is defined^{23,50}

TABLE 2.4.-II

STARTING CONDITIONS USED TO SIMULATE ATR/ST BASELINE STARTUP

Initial ion temperature, T_{i0} (keV)	0.15
Initial electron temperature, T_{eo} (keV)	0.15
Minimum plasma-to-wall radius, $x = r_p/r_w$	0.95
Initial plasma current, $I_{\phi 0}$ (MA)	4.52
Initial bias field, $B_{\phi 0}$ (T)	2.30
Initial toroidal plasma beta, β	0.0016
Fraction of tritium in initial ion density, f_T	0.50
Initial ion filling density at r_w , $n_{i0}(10^{20}/m^2)$	0.35
Initial ion line density, $N_{i0}(10^{20}/m)$	7.26
Impurity fraction of n_{i0} for $Z = 8.00$	0.0001
Initial value of Z_{eff}	1.00
Plasma current risetime, τ_R (s)	0.10
First-wall radius, r_w (m)	2.69
First-wall thickness, δ_{FW} (mm)	5.00
Hole fraction of first wall ^(a) , f_H	0.10
Resistivity of first wall ^(a) , η_{FW} (ohm-m)	4.00
Plasma aspect ratio, $A = R_T/a$	1.83
Plasma major toroidal radius, R_T (m)	2.70
Vacuum chamber volume (m^3)	360.
Blanket volume (m^3)	365.
Shield volume (m^3)	128.
Plasma vertical elongation, κ	3.00
Initial toroidal field, $\langle B_{\phi} \rangle \approx B_{\phi V}(')(T)$	2.30
Initial toroidal-field energy, $W_{\phi}(G.^{\circ})$	5.93
Initial toroidal-field-coil current, I_{TF} (MA-turn)	31.05
Initial toroidal-field-coil dissipation, P_{TFC}^{Ω} (MW)	250.
Volume of toroidal-field coil (m^3)	13.4
Efficiency of the thermal cycle, η_{TH}	0.36
Blanket multiplication factor, M_N	1.27
Auxiliary power as a fraction of net electric power, f_{AUX}	0.07

(a)Used for estimate of cyclotron radiation reflection.

Fig. 2.4.-4. Steady-state field and current-density profiles at end of ATR/ST baseline simulation for $I_{\phi} = 40$ MA, $R_T = 2.7$ m, and $r_w = 2.69$.

Fig. 2.4.-5. Evolution of the ATR/ST startup transient expressed on the RFP F- Θ diagram. Shown also are a number of estimates of near-minimum-energy RFP states for $I_\phi = 40$ MA, $R_T = 2.70$ m, and $r_w = 2.69$ m.

Fig 2.4.-6. Current and voltage traces during the ATR/ST startup for $I_\phi = 40$ MA, $R_T = 2.70$ m, and $r_w = 2.69$ m.

Fig. 2.4.-7. Time dependence of plasma density, temperatures, and betas for ATR/ST startup simulation for $I_\phi = 40$ MA, $R_T = 2.7$ m, and $r_w = 2.69$ m.

TABLE 2.4.-III

SUMMARY OF FINAL, STEADY-STATE ATR/ST PARAMETERS AS PREDICTED BY
TIME-DEPENDENT PLASMA/CIRCUIT SIMULATIONReactor energies (MJ/m)^(a)

Final plasma	23.41
Charged particle	2248.87
DT neutron	8948.00
DD neutron	4.78
Ohmic dissipation	177.09
Auxiliary plasma heating	0.00
Electron conduction	2650.58
Ion conduction	587.39
Bremsstrahlung	173.09
Line(oxygen)	0.00
Cyclotron radiation	2.68
Plasma toroidal field	66.99
Plasma poloidal field	106.74
Quench energy	197.14

Reactor powers (MW/m)^(a)

Total thermal	126.03
Charged particle	20.63
DT neutron	82.07
DD neutron	0.05
Ohmic dissipation	0.99
Auxiliary plasma heating	0.00
Electron conduction	16.43
Ion conduction	3.94
Bremsstrahlung	1.19
Line(oxygen)	0.00
Cyclotron	0.03

Final reactor characteristics

Circularized first-wall radius, r_w (m)	2.69
Major toroidal radius, R_T (m)	2.70
Toroidal energy, $W_{B\phi}$ (GJ)	37.52
Poloidal energy, $W_{B\theta}$ (GJ)	166.23
Plasma current, I_p (MA)	40.00
EFC current, I_c (MA)	103.58
Poloidal flux, ϕ (V s)	121.14
Plasma current density, j_ϕ (MA/m ²)	1.95
Plasma Q-value, Q_p	10.69
Lawson parameter, $n\tau_E$ (10 ²⁰ s/m ³)	1.42
Engineering Q-value, Q_E	3.03
Plant efficiency, η_p	0.24
Neutron wall loading, I_w (MW/m ²)	3.28
Total thermal power, P_{TH} (MWt)	2138.10
Gross electric power, P_{ET} (MWe)	769.72
Circulating power, P_c (MWe)	253.92
Net electric power output, P_E (MWe)	515.80 ^(b)

(a)Expressed per unit length of plasma.

(b)Actual design is $P_E = 500$ MWe(net).

primarily by holding the toroidal flux, ϕ , and the magnetic helicity, $K = \int \vec{A} \cdot \vec{B} dV_p$, invariant within a conducting shell surrounding the plasma, where $\vec{A}$ ($\vec{B} = \nabla \times \vec{A}$) is the magnetic vector potential and the integration is performed over the volume enclosed by the conducting shell. The magnetic helicity is simply a measure of the degree to which flux tubes within the conducting shell are interlinked. The helicity remains invariant, and a global near-minimum-energy state is maintained while an external circuit parameter (e.g., voltage, V_θ , applied to a TFC) is varied to change the toroidal flux external to the plasma. Intrinsic plasma processes related to turbulence and/or resistive instabilities generate voltages and currents within the plasma in order to increase or reduce poloidal flux to maintain the helicity constant and the plasma in a near-minimum-energy state. This nonlinear coupling between plasma and magnetic fields through the Taylor F- θ diagram (Fig. 2.4.-5) can be used to "rectify" current oscillations created at external coils into a net steady-state current within the plasma. This "F- θ pumping" is envisaged to transform toroidal magnetic flux (poloidal currents) into toroidal currents (poloidal magnetic flux) through the plasma relaxation responsible for maintaining the near-minimum-energy configuration.

Little analysis and design beyond that described in Refs. 2, 19, 32 are available to describe OFCD for either experiment or the reactor. This section summarizes a methodology by which an engineering evaluation of OFCD can be made. In developing and evaluating these expressions along the lines described in Refs. 19 and 32, the circuit parameters, definitions, and approach summarized in Ref. 2 are used. The plasma is described in terms of K , ϕ , and W_M , and the relationship between these parameters and the circuit variables (i.e., resistances, inductances, currents, voltages) constitutes the overall current-drive model. The time dependence of K , ϕ , and W_M results directly from the Maxwell equations, as described in Ref. 2, and the following electrical-engineering analogue of the OFCD equation results:

$$V_\phi = I_\phi R_p + [L_p + \frac{\theta}{2} \frac{dL_p}{d\theta}] \dot{I}_\phi + [\frac{1-F}{\epsilon\theta} - \frac{\epsilon\theta^2}{2L_0} \frac{dL_p}{d\theta}] V_\theta, \quad (2.4.-8)$$

where $V_{\phi,\theta}$ are the toroidal and poloidal voltages applied to the plasma, $R_p = 2g_{OHM}\eta(T_e)/\epsilon^2 R_T$ is the plasma resistance, L_p is the plasma internal

inductance (not including vacuum toroidal flux), $F = \bar{B}_\phi(r_p)/\langle B_\phi \rangle$, $\Theta = \bar{B}_\theta(r_p)/\langle B_\theta \rangle$, $\langle B_\phi \rangle \equiv \phi/\pi r_p^2$ is an average toroidal field, and $L_0 = \mu_0 r_p^2/2R_T$ is the vacuum toroidal inductance. If a) the coupling of fields is sufficiently strong to make L_p a function of Θ , and b) if a mechanism exists to allow the plasma near-minimum-energy state to be relaxed to some point in F - Θ space (Fig. 2.4.-5) on a time scale of the plasma relaxation, τ_R , then oscillations of $V_{\phi,\theta}$ in proper phase at a frequency less than $\sim 2\pi/\tau_R$ can give a net time-averaged current, $\langle I_\phi \rangle$, with $\langle V_\phi \rangle = 0$ (i.e., no net flux change). The development of this model is discussed further in Appendix D.

The relaxation mechanisms required for OFCD can be postulated for the RFP, but have yet to be identified for the tokamak in general or for the ST in particular. Nevertheless, a Taylor-like state^{23,50} can be assumed for the ST ($\vec{\nabla} \times \vec{B} = \mu \vec{B} = \mu_0 \vec{j}$, with $\mu \approx \text{constant}$), $\mu = \mu_0 \vec{j} \cdot \vec{B}/B^2$ can be calculated as a function of a circularized flux radius (Fig. 2.1.-7), the equivalent F - Θ curve estimated for the finite-beta plasma, and L_p determined as a function of Θ . The OFCD equation can then be solved and the conditions for $\langle \dot{I}_\phi \rangle = \langle \dot{K} \rangle = 0$ displayed as a function of toroidal flux swing, $\delta\phi/\phi_0$, toroidal voltage swing, $\delta V_\phi/V_\phi$, and drive frequency, ω . Although the current-drive terms in Eq. (2.4.-8) are small compared to the RFP (e.g., $dL_p/d\Theta$, L_p , and Θ are smaller, F is closer to unity), the ST resistance is also smaller, and a weaker OFCD effect for the ST would not have to be as strongly excited.

In evaluating Eq. (2.4.-8) to determine the flux changes, field oscillations, and power flows associated with OFCD as applied to the ATR/ST, the relationship between F and Θ , as well as the dependence of field profiles on Θ in order to determine L_p and $dL_p/d\Theta$, must be determined. This preliminary model allowed only the use of the circularized, one-dimensional model described in Appendix A of Ref. 2., with the resolution of attendant discrepancies between this model and the NEQ results requiring additional work. The circularized plasma model is described, and comparisons are made with a limited number of NEQ calculations.

In the one-dimensional, circularized model, the Maxwell equations ($\vec{\nabla} \times \vec{B} = \mu_0 \vec{j}$, $\vec{j} \times \vec{B} = \vec{\nabla} p$) are solved by specifying the current density both parallel and perpendicular to the magnetic field as follows:

$$\vec{j}_\parallel = \mu \vec{B} \quad (2.4.-9A)$$

$$\vec{j}_\perp = \vec{\nabla} P \times \vec{B} / B^2, \quad (2.4.-9B)$$

where $\mu(r) = \mu_0 \vec{j} \cdot \vec{B} / B^2$. The working equations in one-dimensional circularized form expressed in cylindrical toroidal and poloidal co-ordinates, ϕ and θ , are respectively,

$$-\frac{\partial B_\phi}{\partial r} = \mu_0 j_\theta = \mu B_\theta + \mu_0 \frac{\partial P}{\partial r} \frac{B_\phi}{B^2} \quad (2.4.-10A)$$

$$\frac{1}{r} \frac{\partial}{\partial r} (r B_\theta) = \mu_0 j_\phi = \mu B_\phi - \mu_0 \frac{\partial P}{\partial r} \frac{B_\theta}{B^2}. \quad (2.4.-10B)$$

These equations are solved for field and current-density profiles, and hence F , L_p , and $dL_p/d\theta$ versus θ subject to the specification of $T(r)$, $n(r)$, $\mu(r)$ and the following constraints

$$\beta_\theta = \frac{\int_0^{r_p} P 2\pi r dr}{A_p B_\theta^2(r_p) / 2\mu_0} \quad (2.4.-11A)$$

$$I_\phi = \int_0^{r_p} j_\phi 2\pi r dr. \quad (2.4.-11B)$$

The functional dependence of $\mu(r)$ is determined by the NEQ results (Fig. 2.1.-7B), but the simultaneous matching of θ , F , $\mu(0)$, and β_θ between the NEQ and the circularized model is inhibited by an absence of toroidal effects in the low-aspect-ratio ST geometry. The actual iteration procedure specified $\beta_0 \equiv P(0)/(B_\phi^2(0)/2\mu_0)$ and $\theta_0 \equiv (r_p/2)(\partial B_\theta/\partial r)_0/B_\phi(0)$, and for a given $n(r)$, $T(r)$, and $\mu(r)$ Eqs. (2.4.-10) are solved for given values of β_θ and θ . This solution procedure iterates on β_0 and θ_0 until the design values of β_θ and θ are achieved. The resulting profiles then are used to determine g_{OHM} , F , L_p , and $dL_p/d\theta$ for use in the OFCD model [Eq. (2.4.-8)]. Table 2.4.-IV summarizes key OFCD parameters determined by NEQ, and Fig. 2.4.-8 gives a range of F - θ curves generated from the circularized, one-dimensional equations. The adjustable parameters available to maximize agreement between NEQ and the circularized

TABLE 2.4.-IV
KEY NEQ RESULTS THAT MUST BE MATCHED BY THE OFCD MODEL

On-axis toroidal field, $B_\phi(0)(T)$	4.12
On-axis paramagnetism, $B_\phi(0)/B_{\phi V}(0)$	1.79
On-axis current density, $j_\phi(0)(MA/m^2)$	2.80
Poloidal beta, β_θ	0.23
On-axis Taylor parameter, $\mu(0)(m^{-1})$	0.85
Pinch parameter, $\Theta = \bar{B}_\phi(r_p)/\langle B_\phi \rangle$	0.47
"Reversal" parameter, $F = \bar{B}_\phi(r_p)/\langle B_\phi \rangle$	0.76
Toroidal-field energy, $W_{B\phi}(GJ)$	1.53
Poloidal-field energy, $W_{B\theta}(GJ)$	0.70
Average toroidal field, $\langle B_\phi \rangle / \langle B_\phi^2 \rangle^{1/2} (T)$	3.27/3.40

Fig. 2.4.-8. Taylor diagram generated for the ATR/ST using the circularized one-dimensional model described by Eqs. (2.4.-10), which has been evaluated using the normalized μ -profiles generated by NEQ (Fig. 2.1.-7B) and for a range of poloidal beta values, β_θ . Shown also is a partial F - Θ curve generated by NEQ, with the design point also being indicated.

modes are essentially β_θ and $\mu(0) = \mu_0 j_\phi(0)/B_\phi(0) = 2\Theta_0/r_p$. A comparison of the circularized OFCD model with the NEQ-generated results indicates differences that presumably are attributed to toroidal effects at low aspect ratio. The relationships given in Fig. 2.4.-8 are used (Sec. 4.5.3.6.) to obtain preliminary OFCD design data for the ATR/ST. A major difference between the ATR/ST and the RFP regions on Fig. 2.4.-8 is that for the former the plasma inductance (corrected for the vacuum toroidal flux) decreases with increasing Θ , which is opposite in behavior than for RFP-like near-minimum-energy profiles. Figure 2.4.-9 gives a comparison of L_p and $dL_p/d\Theta$ estimated from NEQ with those predicted for an RFP²; in view of the major role played by these internal inductance changes in OFCD and associated plasma relaxations, significant differences are expected between the ST and RFP configurations.

While the mechanism and efficacy of OFCD in RFP is poorly understood, the understanding decreases when applied to the tokamak.²⁴ Hence, the attractive results suggested in Table 2.4-IV must be balanced by the somewhat less than transparent understanding of OFCD in general. In order to understand the uncertainties of applying OFCD to the tokamak, it is helpful to develop first a mechanistic picture for current drive in the RFP. One method for explaining OFCD in RFPs considers the evolution of the q-profile for one OFCD cycle, as is shown in Fig. 2.4.-10. Initially the near-minimum-energy RFP state is characterized by a q-value that is less than unity on axis, with q falling to zero near the plasma edge and reversing at the plasma surface. In order to affect OFCD in the RFP, the external toroidal- and poloidal-field circuits are oscillated about steady state values, with the most efficient pumping (i.e., maximum current for minimum reactive power) occurring when the toroidal and poloidal voltages are ninety degrees out of phase. A decrease in the external poloidal flux, ψ , and an increase in the amplitude of the external toroidal flux, ϕ , will result in more negative F values (i.e., deeper reversal); this deeper reversal appears as a compression phase in Fig. 2.4.-10. The plasma under these conditions is unstable to a series of resistive MHD modes with high toroidal mode numbers, n, all of which have a poloidal mode number, m = 1. These instabilities drive a Kadomtsev reconnection process (Fig. 2.4.-10), resulting in a flattening of the central portion of the q profile and increasing the poloidal flux.^{51,52} The Kadomtsev reconnection process may also occur during the compression phase and need not be a distinct phase, as is shown in Fig. 2.4.-10. The plasma is then decompressed as the external circuits approach

Fig. 2.4.-9. Dependence of ATR/ST plasma inductance on $\Theta = \bar{B}_\theta(r_p) / \langle B_\phi \rangle$ for both circularized and NEQ models. Comparison with the compact RFP reactor (CRFPR)² is also given.

Fig. 2.4.-10. Schematic representation of OFCD for RFPs and tokamak in terms of q -profile evaluation.

the end of a period; the external poloidal flux is increasing, and the toroidal flux is decreasing at this point in the OFCD cycle. With the value of q at the plasma edge reset to the initial value, the plasma then relaxes by means of a double reconnection⁵² (i.e., dynamo effect) into the initial near-minimum-energy state.

An analogous series of q profiles are shown in Fig. 2.4.-10 for a tokamak. The major difference between the RFP and the tokamak is the process whereby poloidal flux is created on axis; Kadomtsev reconnection creates destroys poloidal flux in an RFP, whereas it destroys poloidal flux in a tokamak. An as yet unspecified process, therefore, is required to create poloidal flux on axis in a tokamak. The application of OFCD to tokamaks, therefore, requires the identification of a process for generating flux in a tokamak.

A scheme for increasing poloidal flux on axis has been proposed.⁵³ This scheme requires tailored ECRH heating to create an off-axis current channel; a non-monotonic q profile results, as is shown in Fig. 2.4.-11. Kadomtsev reconnection process ($m = 1$, $n = 1$) will return a monotonic q profile, as is shown in Fig. 2.4-11. In order to obtain an increase in poloidal flux, the shaded area A_2 must be greater than shaded area A_1 . The non-monotonic q profile is subject to double tearing modes of any m and n value;⁵⁴ this process will limit the value of A_2 and thereby affect the feasibility of this scheme. It is important to note that while RFPs "live" off instabilities, tokamak risk death

Fig. 2.4.-11. Use of q -profile tailoring by off-axis selective (ECRH) heating to drive current in tokamaks.⁵³

by instabilities should current disruption and loss of gross confinement result. Hence, OFCD mechanisms that in one way or another modify q profiles tend to be more onerous for the tokamak. Until such a non-disruptive mechanism is better defined, therefore, it is prudent to also examine the implications of FWCD on the ATR/ST reactor design.

2.4.2.2. Other Current-Drive Options. The low-aspect-ratio ATR/ST will almost certainly require some form of current drive. As shown in the previous section, OFCD represents an attractive form of current drive, ideally requiring $I_\phi^2 R_p = 17$ MW for the 40-MA design point. Considerable uncertainty exists, however, as to whether the theory^{3,2} of OFCD as developed for reversed-field pinches applies to tokamaks. Consideration of the following options is given below: bootstrap current, Alfvén wave (AW), lower-hybrid (LH), and relativistic electron-beam (REB) current drive. Most of these options use interactions between fast-waves and the plasma to alter distribution functions or simply to drive momentum in a preferred direction. After reviewing these options, an estimate is made of the degree to which the ATR/ST baseline design (Table 1.5-I) would be altered if FWCD were required.

Bootstrap Current (BC). Neoclassical theory predicts the existence of a "bootstrap current" caused by radial diffusion. An expression for this bootstrap current density is given by^{5,5}

$$j_{BC} = - \frac{\epsilon^{1/2}}{B_\theta} \left(\frac{\partial P}{\partial r} \right). \quad (2.4.-12)$$

The low aspect ratio and high β that characterizes the ST suggest a large bootstrap current; the ST concept, in fact, may be unique, in that the bootstrap current may suffice to provide the entire plasma current. As an example, the following close approximation to the ATR/ST design-point equilibrium are assumed: $j_{BC} = I_\phi / \pi r_p^2$ a constant, $p(r) \approx p_0 [1 - (r/r_p)^2]$ with $p_0 \approx 2$ MPa, $\epsilon^{1/2} = 0.75$, and $B_\theta \approx \mu_0 I_\phi / 2\pi r = \mu_0 j_\phi r / 2$. For these conditions, a bootstrap current density of $j_{BC} = 2.19$ MA/m² results, which for $A_p \approx 21.2$ m² amounts to 46.4 MA, compared to the reference $I_\phi \approx 40$ MA for the ATR/ST design point. The bootstrap current has not been definitively observed in any tokamak experiments; however, tokamak experiments to date have relatively large aspect ratio and low β and may not be appropriate for the development and observation of the

bootstrap current. The ST experiment proposed by ORNL⁵⁶ may well be the optimal device for definitively determining the existence of the bootstrap current.

Alfvén-Wave (AW) Current Drive. Of all the proposed forms of RF current drive, the compressional Alfvén wave is by far the most efficient, outperforming the LH wave by about a factor of two on an A/W basis. For Alfvén-wave current drive, Ehst⁴⁹ gives

$$P_{RF}^{AW} = 8.9 I_{\phi} R_T (n/10^{20}) / T, \quad (2.4.-13)$$

whereas Fisch and Karney⁵⁷ give

$$P_{RF}^{AW} = 1.5 I_{\phi} R_T (n/10^{20}) / \beta^{1/2} T, \quad (2.4.-14)$$

where β is the local value. Finally, for a special Alfvén wave in which $k_{\perp} \gg k_{\parallel}$, a condition which can easily be accomplished in tokamaks, a detailed calculation by Li⁵⁸ gives

$$P_{RF}^{AW} = 7.5 I_{\phi} R_T (n/10^{20}) / T. \quad (2.4.-15)$$

For the $I_{\phi} \approx 40$ -MA ATR/ST design, these three relations give the following requirements for steady-state current drive: 94, 53, and 80 MW. If the ATR/ST design were reoptimized from $T = 15$ keV to 21 keV to enhance wave coupling, the respective FWCD powers would be reduced to 45, 25, and 38 MW.

The Alfvén waves would be launched with a frequency of 10-20 MHz. Sources in the megawatt range are currently available at these frequencies. Either antennae, coils, or ridged waveguides may be used to launch the waves. In an experiment done on a large-aspect-ratio stellarator,⁵⁹ steady-state Alfvén-wave-driven currents have been created with an efficiency about twice that predicted by Eq. (2.4.-14). Alfvén waves, unlike lower-hybrid waves, do not saturate at higher plasma density.

The main problem with Alfvén-wave current drive revolves around trapped-particle effects in toroidal geometry and possible resonance with and damping of the wave by fast alpha particles. Both Eqs. (2.4.-13) and (2.4.-14) are based

on consideration of waves with $k_{\parallel} > k_{\perp}$. Hence, the Alfvén velocity, v_A , is much less than the electron thermal speed, v_{the} , and for ATR/ST $v_A \approx 0.1v_{the}$, $v_A \approx 2v_{\alpha}$ [300 keV], and $v_A \approx v_{\alpha}$ [1.2 MeV], where v_{α} is the alpha-particle velocity; a part of the alpha-particle distribution, therefore, will resonate with the wave and drain energy from it. Most of the wave energy and momentum trapped by the alpha particles will, in turn, be passed onto the bulk electron population and should thereby create some current. As the bulk electrons are highly collisional, however, this current-drive mode is expected to be inefficient, and the net impact of the alpha particles is to decrease the FWCD efficiency.

A quantitative measure of this effect can be obtained by comparing the Landau damping rates of electrons and fast alpha particles and weighting these rates by the comparative population size. The alpha particles are described by an inverse-energy distribution function, and the electrons have a Maxwellian distribution. This calculation has not been carried out for ATR/ST, but Li has shown⁵⁸ the efficiency to be reduced by up to a factor of three for a typical tokamak reactor. This reduced efficiency can be avoided, however, by pulsing the current drive with tritium injection. In fact, if the current drive is pulsed for short times simultaneously with the tritium injection and then left off while the alpha particles are thermalizing from an inverse-energy down to a Maxwellian distribution (such a time would be ~ 2 -7 seconds and is much less than the plasma L/R decay time), then the current drive would be applied only in the presence of alpha particles with $v_{\alpha} > v_A$ and, in fact, would be amplified by resonating with the alpha particles. Such an effect could reduce the current-drive power by a factor of two or more, since $P_{\alpha} = 400$ MW is much greater than P_{RF}^{AW} .

Trapped-particle effects in toroidal geometry present a much more serious problem. These effects are expected to be especially strong in ATR/ST, but are ignorable in the successful experiment reported in Ref. 59 (stellarator with aspect ratio = 14). The Alfvén waves have low phase velocity, and, therefore, interact most strongly with electron processing slowly around the torus, since these electrons have $v_{\perp} \gg v_{\parallel}$. Such particles are precisely those which are most susceptible to trapping in banana orbits. Fisch and Karney, however, advance⁵⁷ arguments that such effects will not impair the Alfvén wave drive. Ultimately, however, this question would have to be resolved by experiment.

It has been suggested⁶⁰ that injecting the Alfvén waves from the inboard, high-field side of the torus may solve the problems described above, since all particles circulating through the high-field region are by definition untrapped. Calculations to elaborate this suggestion, however, have not been presented, and in any case launching Alfvén waves from the inboard side of ATR/ST is not expected to be a simple matter, given the exceptionally tight geometrical constraints at low aspect ratio.

An alternative solution has been proposed by Li, Ross, and Mahajan in Refs. 58 and 61, where it is proposed to launch Alfvén waves with $k_{\perp} \gg k_{\parallel}$. For this wave, the phase velocity is far above the maximum alpha-particle velocity, and coupling to the alpha particles is expected to be insignificant; this wave also couples most effectively to particles with $v_{\parallel} \gg v_{\perp}$, so that trapped-particle effects should also be ignorable. Mahajan also suggests⁶¹ that it is possible with this wave to create a wave splitting, in which a uni-directional wave can be launched from a single launcher without the use of phased waveguides; a significant simplification of the reactor design is expected in this case. Generally, the Li-Ross-Mahajan Alfvén-wave current drive appears to be an option for ATR/ST current drive.

Lower-Hybrid (LH) Current Drive. The LH wave has been proven experimentally to be an effective means of current drive in tokamaks, and, in fact, has been used in PLT⁶² to start the tokamak without the use of OHCs. Lower-hybrid drive experiences no problem with resonating alpha particles or trapped-particle effects, but it does have a density limit set by $\omega_{ce} > \omega_{pe}$; this condition can be expressed as $\beta < 0.008T(\text{keV})$, where β is the local value, and is marginally satisfied by the ATR/ST baseline design (Table 1.5.-I). Lower-hybrid current drive, therefore should be possible, provided care is taken in the detailed design of the system. Lower-hybrid waves can be easily launched from waveguides and have been launched with intensities up to 150 MW/m^2 . The STARFIRE tokamak reactor design,⁷ therefore, was conservative when a waveguide power density of 15 MW/m^2 was suggested, this power density nevertheless being sufficient for the ATR/ST. The main problem with LH drive is that of efficiency. Peng⁴ gives the following expression for the drive power:

$$P_{\text{RF}}^{\text{LH}} = 13.5 I_{\phi} k_T (n/10^{20}) / T, \quad (2.4.-16)$$

while Fisch and Karney give⁵⁷

$$P_{RF}^{LH} = 0.93 I_{\phi} R_T (n/10^{20}). \quad (2.4.-17)$$

The results of applying these relationships to the $I_{\phi} = 40$ -MA ATR/ST baseline design gives $P_{RF}^{LH} = 152$ and 130 MW, respectively, while if the design were reoptimized (lower density, $T = 28$ keV instead of 15 keV) the respective RF power would be 59 and 84 MW. It can be seen that the LH wave is less efficient than the Alfvén wave as applied to the ATR/ST.

Relativistic Electron Beams (REB). Reference 63 presents a detailed design for a REB current-drive system for the FED-A reactor as well as calculations indicating that the current-drive power for such a system can be taken approximately as

$$P^{REB} = 2 I_{\phi}^2 R_p, \quad (2.4.-18)$$

where R_p is the plasma resistance, which for the ATR/ST baseline gives 34 MW consistent with a 17 -MJ REB pulse energy with an interpulse period of 0.5 s. The REB current drive has been demonstrated experimentally on several tokamaks and has been used successfully for startup without the use of OH coils.⁶⁴

Although the low-power requirement makes REBs attractive, a number of obstacles to implementation can be identified. Firstly, the REB systems involve the repeated (one or more times per second) firing of capacitor banks of megavolt range, giving rise to questions of reliability when applied to a commercial reactor. Secondly, theory indicates that the REB current will only penetrate a few centimeters into the plasma, leading to a hollow current profile and disagreement with the ATR/ST design (Fig. 2.1.-7A). Deeper penetrations can be achieved if very high voltages (~ 50 MV) can be used. A combination of REB to provide current in the outer plasma with AW drive to provide current at the plasma core may offer some advantages in power savings, albeit at the cost of additional complexity. One hope in dealing with the hollow current profile is that the REB-created currents will diffuse anomalously rapidly into the plasma core or be transported into the central plasma through MHD activity.

Thirdly, the very dense REB current leaving the cathode may perturb the tokamak magnetic geometry; these effects must be taken into account in the total design of the magnetic configuration, especially in planning the divertor channels. In this connection, it should be noted that the REB design suggested in Ref. 63 placed the cathode on the underside of a limiter plate and below the plasma. Because divertors are used in the ATR/ST design, such a convenient arrangement is not possible, although a similarly clever arrangement may exist for the divertor configuration.

Reoptimization for Fast-Wave Current Drive (FWCD). A quantitative incorporation of FWCD into the comprehensive systems code (Sec. 3.1.) was not within the scope of this study. The simplified model described in Sec. 2.4.1.1., however, was coupled with approximate reactor scaling relationships and used to estimate the impact of FWCD on the OFCD-based design summarized in Table 1.5.-I. Because of the larger recirculating power required by FWCD schemes, a series of simulations were made to determine a temperature-density combination that would minimize the RF and coil power requirement, thereby maximizing Q_E and minimizing the recirculating power fraction. A full cost optimization was not performed, however.

Neo-Alcator transport scaling and a 500-s ramp-up time (Fig. 2.4.-3) was assumed. All results were normalized to a reactor with $\beta = 0.29$ and $P_\alpha \approx 400$ MW in order to facilitate comparison with the baseline design. Hence, given a normalization ratio $x \equiv (400/P_\alpha)(\beta/0.29)^2$, the following scalings are used to relate to the ATR/ST baseline: $B_{\phi V}(0) \propto x^{1/4}$; $I_\phi \propto x^{1/4}$; $P_\Omega^{TFC} \propto x^{1/2}$; $P_{RF} \propto x^{3/4}$; $n \propto x^{1/2}$. Recirculating power requirements aside from ohmic losses in the TFCs, P_Ω^{TFC} , and current drive, P_{RF} , were included (as in the more detailed, cost-optimizing models of Sec. 3.1.) as 7% of the gross electric power. The current-drive power, P_{RF} , was assumed to be delivered at an efficiency 50% and along with the fusion power was recovered at an efficiency of $\eta_{TH} = 0.35$. All results were obtained for $P_E = 500$ -MWe(net) through iteration of the simplified model described in Sec. 2.4.1.1. and scaled as described above.

The results of this scoping study are presented in Fig. 2.4.-12. It is seen that for both AW and LH current drive, Q_E is maximized by moving the design away from the baseline [$n = 1.27(10)^{20} \text{ m}^{-3}$, $T_i = 15.5 \text{ keV}$, $T_e = 14.5 \text{ keV}$] to densities of $\sim 9(10)^{19} \text{ m}^{-3}$ and ion temperatures in the range 25 keV (AW) to 28 keV (LH). The results for net electric power normalized to 500 MWe are

Fig. 2.4.-12. Sensitivity of ATR/ST baseline design [$P_E = 500\text{-MWe}(\text{net})$, $I_\phi = 40\text{ MA}$, $R_T = 2.7\text{ m}$, $\beta = 0.29$] to ion temperature when FWCH using either Alfvén and LH waves (instead of OFCD) is used.

summarized in Table 2.4-V. Generally, the direct application of FWCD to the OFCD baseline results in a decrease in the already marginal Q_E -value [for $P_E = 500\text{-MWe}(\text{net})$]. A significant part of this diminished Q_E can be recovered by re-optimizing at lower density and higher temperature. It is seen that going to higher temperatures and lower densities increases T_i and T_e , as well as the ratio T_i/T_e . This latter result is a consequence of $\tau_{eq} \propto T_e^{3/2}/n$ and is a beneficial effect, since the fusion output is increased for a given plasma pressure. A more detailed study would include the cost impact of FWCD beyond

TABLE 2.4-V
IMPACT OF FAST-WAVE CURRENT DRIVE (FWCD) ON BASELINE ATR/ST DESIGN

	OFCD	FWCD(AW)		FWCD(LH)	
	Baseline	Baseline	Reoptimized	Baseline	Reoptimized
T_e (keV)	15	14.6	19.4	14.7	21.3
T_i (keV)	15	15.4	24.6	15.3	28.5
$n(10^{20}/m^3)$	1.27	1.35	0.9	1.43	0.88
$B_{\phi V}(0)$ (T)	2.36	2.43	2.43	2.5	2.53
Q_E	2.9	2.2	2.5	1.96	2.3
P_{RF} (MW)	30	108	58	180	83
P_{RF} (MW)	17	94	45	152	59

the decreased Q_E value shown in Table 2.4.-V, which for cost much above ~ 1 \$/W would be significant.

2.5. Divertor Physics

2.5.1. Magnetics. The ATR/ST design is the result of iterations between the systems code (Sec. 3.), which determines the gross features of the design, and the models used to describe the various subsystems (Fig. 1.3.-1), which determine in more detail the performance characteristics of those subsystems. In the case of the magnetics, the system code indicates, after several iterations with the two-dimensional equilibrium model,^{10,11} that the optimum configuration is described by the gross characteristics given in Table 1.5-I. These features were then used as input to the NEQ code to determine the plasma, divertor, and PFC (EFC + DFC) configuration. In addition, the systems code provided either initial guesses for input into or target values for PFC locations and current densities as well as plasma beta, density, volume, and elongation.

The final (baseline) results from the NEQ code are summarized in Figs. 2.5.-1 - 2.5.-6 and listed in Table 2.5.-I. These results emerged after considerable interaction between NEQ, the systems code, as well as other models. with the systems code using more realistic input and monitoring the conditions required for minimum cost. The plasma, scrapeoff, and PFC configuration are shown in Fig. 2.5.-1.

TABLE 2.5.-I
MAGNETICS AND PLASMA PARAMETERS FOR THE 500-MWe(net) ATR/ST
BASELINE CASE AS DETERMINED BY THE NEQ CODE^{10,11}

Parameter	Value
Plasma major/minor radius, $R_T/a(m)$	2.7/1.50 ^(a)
Toroidal plasma current, $I_\phi(MA)$	40.9 ^(a)
Flux-surface-averaged minor radius, r_p	2.60
Volume-averaged toroidal plasma beta, $\beta^{(b)}$	0.29
Critical Troyon-Gruber beta, β_c	0.29
Plasma volume/surface area, $V_p(m^3)/A_p(m^2)$	333.5/323.4
On-axis vacuum/paramagnetic toroidal field, $B_{\phi V}(0)/B_\phi(0)(T)$	2.36/4.10 ^(a)
Troyon parameter, $I_N = \mu_0 I_\phi / B_{\phi V}(0)a$	14.52
Magnetic axis, $R_m(m)$	2.9
Plasma elongation, κ	2.9
Plasma triangularity, δ	0.029
On-axis/edge safety factor, $q(0)/q(r_p)$	1.12/2.25
Pinch parameter, $\Theta = B_\theta(r_p)/\langle B_\phi \rangle^{(c)}$	0.47
Reversal parameter, $F = B_\phi(r_p)/\langle B_\phi \rangle^{(c)}$	0.76
Plasma self inductance, $L_p(\mu H)$	1.80
Fusion power profile-factor, g_{DT}	1.68
Ohmic power profile-factor, $g_{OHM}^{(d)}$	22.3/2.81
Bremsstrahlung power profile-factor, g_{BR}	1.43
EFC major radius/vertical height, $R_{EFC}/z_1(m)$	7.50/1.50
EFC current, $I_{EFC}(MA)$	12.03
DFC major radius/vertical height, $R_{DFC}/z_2(m)$	2.40/5.75
DFC current, $I_{DFC}(MA)$	-6.50
Peak field at PFC, $B_{CM}(T)$	6.86
Divertor connection length, $L(m)^{(e)}$	24.8/18.4
Plasma surface area, $A_s(m^2)^{(e)}$	95.4/229.0
Average plasma density/temperature, $n(10^{20}/m^3)/T(keV)$	1.34/15.

^(a)Values fed to NEQ from cost-minimizing systems code (Sec. 3.).

^(b)Based on on-axis vacuum toroidal field, $B_{\phi V}(0)$.

^(c)RFP parameters, $F \equiv$ reversal parameter, $\Theta =$ pinch parameter, $\langle B_\phi \rangle = \phi/A_p$, where ϕ is the toroidal flux and $A_p = \pi r_p^2$.

^(d) g_{OHM} with and without trapped particle effects, respectively.

^(e)Inboard/outboard values.

Fig. 2.5.-1. The plasma, scrape-off layer, and PF configuration for the 500-MWe(net) ATR/ST baseline case as determined by the NEQ code.^{10,11}

Fig. 2.5.-2. The radial profile of the flux-surface-averaged safety factor for the 500-MWe(net) ATR/ST baseline case as determined by the NEQ code.

Fig. 2.5.-3. The radial profile of the flux-surface-averaged pressure for the 500-MWe(net) ATR/ST baseline case as determined by the NEQ code.

Fig. 2.5.-4. The radial profile of the toroidal, j_ϕ , and poloidal, j_θ , flux-surface-averaged current density for the 500-MWe(net) ATR/ST baseline case as determined by the NEQ code.

Fig. 2.5.-5. The radial profile of the toroidal, B_ϕ , and poloidal, B_θ , flux-surface-averaged magnetic field for the 500-MWe(net) ATR/ST baseline case as determined by the NEQ code.

Fig. 2.5.-6. The radial profile of the flux-surface-averaged value of $\mu \equiv \mu_0 \mathbf{j} \cdot \mathbf{B} / B^2$ for the 500-MWe(net) ATR/ST baseline case as determined by the NEO code.

The outermost flux surface serves as the plasma boundary and is positioned as close to the separatrix as is numerically possible. The first-wall boundary determined by an 0.04-m-thick scrape-off layer is also shown in Fig. 2.5.-1, with the outer scrapeoff boundary ending at the anticipated position of a divertor neutralizer plate. The PFC cross sections correspond to an average current density of 15 MA/m². The 1-m space between the plasma and the DFC is provided for shielding, whereas the 1.4-m space between the plasma and EFCs is used for tritium breeding, shielding, and TFCs. The TFC configuration is dictated primarily by minimum-cost considerations quantified through the systems code.

The radial q profile shown in Fig. 2.5.-2 complies with the stability constraints, $q(C) > 1$ and $dq/dr > 0$, with a minimum $dq/dr = 0.0005 \text{ m}^{-1}$. The radial pressure profile given in Fig. 2.5.-3 shows a pronounced on-axis peaking and gives relatively large power peaking factors ($g_{DT} = 1.68$, $g_{BR} = 1.43$). This pressure profile was also used to generate a distributed neutron source for the neutronics calculations (Sec. 4.2.). The radial plasma current-density profiles are given in Fig. 2.5.-4. The relatively large plasma current near the cold

(resistive) plasma edge is the main contributor to the large profile factor for ohmic heating power ($g_{OHM} = 2.81$ without trapped-particle effects). The low aspect ratio and large poloidal variations in the toroidal field along a flux surface result in a large resistivity enhancement because of trapped particles (~ 8.0). The flux-surface-averaged poloidal-field profile given in Fig. 2.5.-5 near the plasma edge shows only a portion of the precipitous decrease to zero that occurs at the separatrix. The current-density (Fig. 2.5.-4) and magnetic-field (Fig. 2.5.-5.) profiles are used to calculate the $\mu = \mu_0 \vec{j} \cdot \vec{B} / B^2$ profile shown in Fig. 2.5.-6. A μ profile that is flat over most of the central plasma cross section, as shown in Fig. 2.5.-6, is an indicator of a near-minimum-energy state^{23,50} which is an essential element in the OFCD (Sec. 2.4.2.1.).

Important results of the NEQ code are the radial, vertical, and toroidal magnetic-field components (i.e., B_R , B_z , and B_ϕ , respectively) presented on an extended computational grid for use in the thermalhydraulics design of the self-cooled liquid-metal blanket (Sec. 4.4.). The grid consists of 163 points in major radius in the range $0.9 \leq R \leq 9.0$ m and 213 points vertically in the range $-8.43 \leq z \leq 8.43$. The results of the field component calculation are shown in Fig. 2.5.-7 and are used to estimate the optimal coolant configuration.

2.5.2. Edge-Plasma Transport. A simplified scrapeoff model⁶⁵ is applied in analytic form to the ATR/ST divertors following the procedure used in Ref. 8 to determine the heat flux to the divertor first wall and divertor chamber. This model assumes that the particle and energy transport in the scrapeoff are described by the following equations:

$$D \frac{d^2 n}{dx^2} = \frac{n}{\tau_{||}} \quad (2.5.-1)$$

$$\chi \frac{d}{dx} \left(n \frac{dT}{dx} \right) + 3D \frac{d}{dx} \left(T \frac{dn}{dx} \right) = \frac{2\gamma n T}{\tau_{||}} , \quad (2.5.-2)$$

where D is the position-independent particle diffusion coefficient, χ is the heat conduction coefficient, n is the plasma density in the scrapeoff, T is the scrapeoff plasma temperature, $\gamma = 2 + 1/4 \ln(m_i/m_e)$ is the enhancement of the energy flux resulting from a surface sheath potential, m_i and m_e are the masses of the ion and electron species, and x is the penetration distance into the

Fig. 2.5.-7. A display of the direction and magnitude of the magnetic field for the 500-MWe(net) ATR/ST baseline case. The radius of the circles indicates the magnitude of the toroidal field, B_ϕ , at the center of the circle. The arrows indicate the direction of the poloidal field, B_θ , at the center of the circle, and the length of the arrow indicates the magnitude of the poloidal field.

scrapeoff measured from the separatrix. The parallel confinement time, $\tau_{||}$, is taken as follows to be the time an ion traveling along a field line between divertor throats:

$$\tau_{||} = L/V_{TH} , \quad (2.5.-3)$$

where V_{TH} is the ion thermal velocity and L is the field-line connection length

between divertor openings calculated directly from the NEQ simulation of the divertor configuration. The solutions to Eqs. (2.5.-1) and (2.5.-2) for $\tau_{\parallel} = \text{constant}$ (D and χ are also assumed independent of spatial co-ordinate) are $n(x) = n_0 e^{-x/\lambda_n}$ and $T(x) = T_0 e^{-x/\lambda_T}$, respectively, where

$$\lambda_n = (D\tau_{\parallel})^{1/2} , \quad (2.5.-4)$$

$$\eta = \frac{\lambda_n}{\lambda_T} = \frac{[(\alpha + 3)^2 + 4\alpha(2\gamma - 3)]^{1/2} - (\alpha + 3)}{2\alpha} , \quad (2.5.-5)$$

and $\alpha \equiv \chi/D$. The density and temperature at the separatrix are given by

$$n_0 = \frac{n_i \tau_{\parallel} V_p}{\tau_{pi} (1 - R_{Le}^*) \lambda_n A_s} , \quad (2.5.-6)$$

and

$$T_0 = \frac{P_{TR} (1 - f_{RAD}) (1 + \eta) \tau_{pi} (1 - R_{Le}^*)}{\gamma n_i A_s} , \quad (2.5.-7)$$

where n_i is the average plasma ion density, $P_{TR}(MW)$ is the transport power, A_s and V_p are the plasma surface area and volume, respectively, and τ_{pi} is the particle confinement time. Since atomic processes (e.g., radiation, recombination, charge exchange) are not included in this model, a radiation fraction, f_{RAD} , and a recycle coefficient, R_{Le}^* , have been introduced into Eqs. (2.5.-6) and (2.5.-7). The effective plasma temperature at the first wall, which is important in estimating sputtering, is $T_w = T_0 e^{-x_l/\lambda_T}$, where x_l is the distance between the separatrix and the first wall. The heat flux incident on either the inboard or outboard divertor chamber wall is given by

$$q_{DIV} = \frac{P_{TR} (1 - f_{RAD}) [1 - e^{-x_q(1 + \eta)/\lambda_n}] f_{I0}}{A_{DIV}}, \quad (2.5.-8)$$

where A_{DIV} is the total area within either the inboard or outboard divertors available for uniform heat absorption. The fraction of the power transported from the bulk plasma that enters either the inboard or outboard scrapeoff is represented by f_{I0} . The calculation of f_{I0} is based on the ratio of either inboard or outboard plasma surface area to the total plasma surface area. An even greater fraction of the particles and energy is expected to exit the plasma through the outboard surface, however, because of the region of bad curvature on the outboard side.

The divertor and first-wall configuration shown in Fig. 2.5.-8 was used for a first estimate of the various areas and volumes used for the edge-plasma transport and energy-partition calculations. The difference between the inboard and outboard divertor chambers arises from the need to have low current densities and, hence, low Joule losses in both the TFC central column and the PFCs. The divertor displaces 15% of available blanket volume, and 13% of the first-wall area; these losses are not significant from the viewpoint of energy recovery and tritium breeding (Sec. 4.2.).

A list of typical parameters used for the divertor transport calculations, as well as typical results, are given in Table 2.5.-II. An 0.04-m-thick scrapeoff appears as sufficient to ensure practically all of the energy entering the scrapeoff as conduction and convection also enters the divertor ($\eta_D > 0.999$). The assumed radiation fraction of $f_{RAD} = 0.3$ is consistent with clean plasma expected for operation with magnetic divertors.²⁶ A high recycle coefficient, $R_{Le}^* = 0.9$, was assumed to ensure wall-plasma temperatures at or below a first-wall self-sputtering threshold of ~ 50 eV. The longer divertor connection length on the inboard side of the plasma is a result of the much stronger toroidal field at that position. The average heat flux incident on the divertor chamber walls is estimated to be a maximum of 1.46 MW/m^2 on the outboard side, suggesting a somewhat enlarged single-null divertor may be possible while still maintaining acceptable average heat fluxes. When the actual heat-flux distribution within the divertor chamber is known, however, the double-null configuration is expected to have a reduced peaking problem because of splitting the plasma exhaust into four streams instead of two for the single

Fig. 2.5.-8. The ATR/ST divertor and first wall configuration used in the divertor transport calculations.

null. The final decision regarding the number of nulls awaits a more detailed, spatially resolved heat-flux calculation in the divertor chamber.

TABLE 2.5.-II

TYPICAL DESIGN PARAMETERS FOR THE ATR/ST BASELINE DIVERTOR DESIGN

<u>Parameter</u>	<u>Value</u>
Scrapeoff thickness, x_q (m)	0.04
Radiation fraction, f_{RAD}	0.3
Recycle coefficient, R_{Le}^*	0.9
Plasma density, n_i ($10^{20}/m^3$)	1.28
Particle confinement time, $\tau_p = 2.5\tau_E$ (s)	1.86
Transport power, P_{TR} (MW)	391
Plasma surface area, A_S (m^2)	95.4/229.0 ^(a)
Divertor chamber area, A_{DIV} (m^2)	63.0/132.5
Divertor connection length, L (m) ^(a)	24.8/18.4
Edge-plasma density, n_o ($10^{18}/m^3$) ^(a)	5.7/4.9
Edge-plasma temperature, T_o (keV)	1.5
Wall-plasma temperature, T_w (keV) ^(a)	57.8/34.5
Average divertor heat flux, q_D (MW/ m^2) ^(a)	1.28/1.46
Divertor efficiency, η_D ^(b)	0.9997/0.9999
Blanket loss ^(c) , %	14
First-wall loss ^(c) , %	13

(a) Inboard/outboard values.

(b) Fraction of conduction/convection energy loss into respective scrapeoff that enters the divertor chamber.

(c) Based on total available area/volume that does not include the TFC/inboard-shield central column.

3. PARAMETRIC SYSTEMS STUDIES

3.1. Model

As seen in Fig. 1.3.-1, a Parametric Systems Analysis (PSA) computer code provides a central interface for the coordination of a number of auxiliary design activities in addition to its primary use as an exploratory tool of the ATR/ST design space leading to the identification of optimized, resistive-coil design points and related sensitivity calculations. The present code derives generally from an earlier code developed for CRFPR calculations^{2,66} and provides an essentially common methodology and database to facilitate the comparison of conceptual design points (Sec. 5.4.). An interim version of the present code involved a straightforward adaptation of the original CRFPR code to a D-coil tokamak geometry incorporating a similar search and optimization of the overall coil thickness, trading equipment costs of the PFC against the cost of recirculating power to offset coil Joule losses.

As indicated on Fig. 3.1.-1, the systems code identifies optimal parameters in a set of nested search loops centered on a convergence operation for the engineering Q-value, $Q_E = 1/\epsilon$, and the specified net electric power, P_E , where ϵ is the recirculating power fraction. For a given total coil thickness, δ_c , this inner iteration searches for the value of Q_E that yields the specified P_E as the split between the TFC and PFC geometry varies, subject to the constraints of equal (but unspecified) coil current densities and the matching of fixed engineering and physics parameters indicated in Table 3.1.-I. The total coil thickness δ_c that produces a minimum-COE design for an otherwise fixed geometry, including plasma minor radius, is first determined after convergence of the set (Q_E, P_{TH}) for a given P_E . The outer-loop optimum is then determined as a function of plasma half-width, a , which shows a higher-order (lower) COE minimum. Generally, a is used as a display variable, with the respective minimum-COE design corresponding to a particular value of P_E and plasma aspect ratio, $A = R_T/a$. The outermost loop then varies the aspect ratio in search of an even lower minimum-COE system, although within realistic bounds, the optimum in A is relatively flat for the ATR/ST. These fully-cost-optimized design points are then examined as a function of P_E and the physics, engineering, and economic variables listed on Table 3.1.-I. A focusing of the present study to the ST configuration, involving by definition the absence of an OHC set and a central TFC column filling the available space, allows the elimination of one level of nesting of search loops in the procedure described on Fig. 3.1.-1. The

Fig. 3.1.-1. Parametric Systems Analysis (PSA) procedure used for the ATR/ST study as adapted from CRFPR methods.^{2, 66}

TABLE 3.1.-I

LIST OF FIXED AND VARIED PARAMETERS USED IN ATR/ST PARAMETRIC STUDIES

<u>Plasma Parameters</u>	<u>Value(Base Case)</u>
Plasma minor radius, $a(m)$	Varied (1.50)
Plasma major toroidal radius, $R_T(m)$	Varied (2.70)
Plasma aspect ratio, $A = R_T/a$	Varied (1.8)
Plasma average density, $n(10^{20}/m^3)$	Varied (1.2)
Plasma average temperature, $T(keV)$	$15[T(r) \propto p(r)^{1/2}]$
Profile factors, g_i	NEQ (Sec. 2.1.)
Ignition parameter, $n\tau_E(10^{20} \text{ s}/m^2)$	Varied (0.96)
Pinch parameter, Θ	Varied (0.5)
Reversal parameter, F	Varied (0.75)
Plasma/wall radius ratio, $x = r_p/r_w$	0.85
Poloidal beta, β_e	Varied (0.23)
Average toroidal beta, β	Varied (0.29)
<u>Engineering Parameters</u>	
Thermal conversion efficiency, η_{TH}	0.35 ^(a)
Blanket neutron energy multiplication, M_N	1.2 ^(b)
Outboard blanket/shield thickness, $\Delta b_o/\Delta s_o(m)$	0.8/0.2 ^(c)
Inboard blanket/shield thickness, $\Delta b_i/\Delta s_i(m)$	0.0/0.2
Energy transfer and storage efficiency, η_{ETS}	1.0 ^(d)
Auxiliary power fraction, f_{AUX}	0.07
Coil parameters	
♦ electrical resistivity, $\eta_c(10^{-8} \text{ ohm m})$	1.8
♦ conductor filling fraction, λ_c	0.7
Net electrical power output, $P_E(MWe)$	Varied (500.)
<u>Economic Parameters^(e)</u>	
Return on investment (%/yr)	15
Operating cost (%/yr)	2
First-wall/blanket life, $I_w\tau(MWyr/m^2)$	15
Plant factor, $p_f = (365 - \tau_u - \tau_w)/365$	≥ 0.76
♦ unscheduled, $\tau_u(\text{days/yr})$	60
♦ scheduled, $\tau_s(\text{days/yr})$	
> 1 replacement/yr	$28p_f I_w/(I_w\tau)$
< 1 replacement/yr	28

(a)Increased to 0.36 on the basis of Sec. 4.4. results for the final design-point determination.

(b)Increased to 1.27 on the basis of Sec. 4.2. results for the final design-point determination.

(c)Increased to 1.1 m on the basis of Sec. 4.2. results for the final design-point determination.

(d)Implied long-pulsed or steady-state operation.

(e)Based primarily on the DOE/OFE guidelines and modified by subsequent large reactor studies. All costs are referenced to 1980 dollars. Detailed description of the cost database is presented in Appendix B.

remainder of this section describes the physics (Sec. 3.1.1.), engineering (Sec. 3.1.2.), and costing models incorporated into the ATR/ST systems code, with differences from the Ref. 8 description being emphasized.

3.1.1. Physics. The ATR/ST systems model begins with a steady-state, point-plasma model, corrected for profile effects. The toroidal plasma with major toroidal radius, R_T , has an approximately elliptical cross-section (Fig. 2.1.-2.) defined by a half-width, a , in the equatorial plane and vertical half-height, b , related by the vertical elongation parameter $\kappa \equiv b/a$. The plasma aspect ratio is $A \equiv R_T/a = 1/\epsilon$. Higher-order perturbation to the plasma boundary includes triangularity, δ , which is small, and indentation, i , which is negligible for cases of present interest. The plasma shape factor, S , which is the ratio of the plasma minor perimeter to the perimeter of a circular plasma ($\kappa = 1$) with the same a , is approximated by

$$S \equiv \left(\frac{1 + \kappa^2}{2} \right)^{1/2}. \quad (3.1.-1)$$

Including the correction for triangularity, δ , the plasma cross-section area $A_p \equiv \pi r_p^2$, where r_p is the circularized (average) plasma radius, becomes^{6,7}

$$A_p \equiv \pi a^2 \kappa \left[\frac{2J_1(\delta)}{\delta} \right], \quad (3.1.-2)$$

where $J_1(x)$ is first-order Bessel function of the first kind. The corresponding major radius, R'_T , of the centroid of the plasma cross section is given by^{6,7}

$$R'_T \equiv R_T \left(1 - \frac{0.233\delta}{A} \right), \quad (3.1.-3)$$

such that the plasma volume, $V_p \equiv 2\pi R_T A_p$, is given by

$$V_p = 2\pi^2 R_T a^2 \kappa \left(1 - \frac{0.233\delta}{A} \right) \left[\frac{2J_1(\delta)}{\delta} \right]. \quad (3.1.-4)$$

It is convenient to define a filling fraction $x = r_w/r_p$ for the circularized plasma in a toroidal chamber with major radius, R_T , where r_w is the circularized minor radius of the first wall. The parameter x is chosen to anticipate the eventual first-wall geometry in relating the volumetric fusion power to the average 14.1-MeV neutron first-wall loading; separate NEWLIT¹² computations are performed to determine actual neutron wall-loading variations (Sec. 4.2.2.5.).

The average plasma DT fusion (17.58 MeV/fusion) power density, P_F/V_p , is given by

$$P_F/V_p (\text{W/m}^3) = 7.04(10)^{-13} n_i^2 \langle \sigma v \rangle_{DT} g_{DT} \quad (3.1.-5A)$$

$$= 8.85(10)^5 \beta^2 B_{\phi V}(0)^4 g_{DT} \quad (3.1.-5B)$$

In the above equation, n_i is the average DT ion density, $\langle \sigma v \rangle_{DT}$ is the DT fusion reactivity, g_{DT} is the fusion-power profile correction factor (Sec. 2.1.1.2.), β is the average toroidal beta, and $B_{\phi V}(0)$ is the central vacuum toroidal magnetic field. The $\langle \sigma v \rangle_{DT}(T)$ values used in this study are based on the recent Los Alamos experimental measurement and temperature-dependent fitting function^{6,8} in the range $0 < T < 20$ keV, with typical results summarized in Table 3.1.-II.

TABLE 3.1.-II
MAXWELLIAN-AVERAGED DT FUSION CROSS SECTIONS^{6,8}

T(keV)	$\langle \sigma v \rangle_{DT} (\text{m}^3/\text{s})$	$\langle \sigma v \rangle_{DT}/T^2 (\text{m}^3/\text{s keV}^2)$
0.5	$5.58(10)^{-29}$	$2.23(10)^{-28}$
1.0	$6.71(10)^{-27}$	$6.71(10)^{-27}$
5.0	$5.78(10)^{-24}$	$2.31(10)^{-25}$
10.0	$1.12(10)^{-22}$	$1.12(10)^{-24}$
13.5	$2.21(10)^{-22}$	$1.215(10)^{-24} (\text{max})$
15.0	$2.71(10)^{-22}$	$1.20(10)^{-24}$
20.0	$4.29(10)^{-22}$	$1.07(10)^{-24}$
25.0(a)	$5.95(10)^{-22}$	$9.52(10)^{-25}$

(a) Out of nominal range of fitting function.

It is convenient for oscillating-field current-drive calculations (Sec. 2.4.2.1.) to define the RFP-like magnetic parameter F and Θ , where the reversal parameter is $F = \bar{B}_\phi(a)/\langle B_\phi \rangle$, the pinch parameter is $\Theta = \bar{B}_\theta(a)/\langle B_\theta \rangle$, and " $\bar{X}(a)$ " denotes the plasma-surface average of quantity X and " $\langle Y \rangle$ " denotes the volume-average of quantity Y . The NEQ equilibrium code^{10,11} has been modified to generate these surface- and volume-averaged parameters (Sec. 2.1.). The edge-plasma safety factor $q_a = q(a)$ is approximately given by

$$q_a = \epsilon \frac{B_{\phi V}(0)S}{\bar{B}_\theta(a)} = \epsilon S \frac{F}{\Theta} . \quad (3.1.-6)$$

The vacuum toroidal magnetic field produced by a set of N discrete TFCs carrying a current $I_{c\theta}$ is approximated by

$$B_{\phi V}(R) = \frac{\mu_o N I_{c\theta}}{2\pi R} , \quad (3.1.-7)$$

such that the on-axis vacuum toroidal field is given by

$$B_{\phi V}(R_T) \equiv B_{\phi V}(0) = \frac{\mu_o N I_{c\theta}}{2\pi R_T} , \quad (3.1.-8)$$

and the peak magnetic field on the TFC central column, B_{CM} , is approximated by

$$B_{CM} \equiv B_{\phi V}(R_{cp}) = \frac{\mu_o N I_{c\theta}}{2\pi R_{cp}} , \quad (3.1.-9)$$

where R_{cp} is the radius in the equatorial plane of the TFC centerpost.

The plasma-surface-averaged poloidal magnetic field, $\bar{B}_\theta(a)$, is approximated by

$$\bar{B}_\theta(a) = \frac{\mu_0 I_\phi}{2\pi a S}, \quad (3.1.-10)$$

where I_ϕ is the toroidal plasma current and S is the plasma shape factor given by Eq. (3.1.-1) above, with a numerical value of $\bar{B}_\theta(a)$ being determined by the NEQ equilibrium code (Sec. 2.1.).

The plasma self-inductance, L_p , is approximated by

$$L_p(H) = \mu_0 R_T \left[\ln \left(\frac{8R_T}{a\kappa^{1/2}} \right) + \frac{\ell_i}{2} - 2.0 \right], \quad (3.1.-11)$$

where $\ell_i \equiv \langle B_\theta^2 \rangle / \bar{B}_\theta^2(a)$ is the plasma internal inductance per unit length normalized to $\mu_0 = 4\pi(10)^{-7}$ H/m. The vertical field, B_V , required to maintain the plasma toroidal equilibrium becomes

$$B_V(T) = - \frac{\mu_0 I_\phi}{4\pi R_T} \left[\ln \left(\frac{8R_T}{a\kappa^{1/2}} \right) + \Lambda - \frac{1}{2} \right], \quad (3.1.-12)$$

where $\Lambda \equiv (\beta_\theta + 1/2 \ell_i - 1)$.

A scaling relationship based on MHD computation has been suggested,⁴ such that

$$I_\phi(\text{MA}) = [5aB_{\phi y}(0)/q_a][C_I \epsilon / (1 - \epsilon^2)^2] S^2, \quad (3.1.-13)$$

where C_I is a fitting parameter approximated by $C_I = (1.22 - 0.68\epsilon)$ for the non-diverted plasmas of interest to the ORNL preliminary design calculations for the ST device.⁴ For the present ATR/ST design at $A = 1.8$, this fitting parameter would be $C_I = 0.84$, with substitution of NEQ-generated parameters into

Eq. (3.1.-14) yielding the value $C_I^* = 1.20$ used in the systems code. The average toroidal and poloidal beta values are related by⁶⁹

$$\beta = \beta_\theta \left[\frac{C_I}{q_a} \frac{\epsilon S}{(1 - \epsilon^2)^2} \right]^2 \quad (3.1.-15A)$$

$$= \beta_\theta \left[\frac{I_\phi(\text{MA})}{5aB_{\phi V}(0)S} \right]^2, \quad (3.1.-15B)$$

where β is taken to simultaneously satisfy the lower Troyon-Gruber scaling relationship⁹

$$\beta = 0.02514 \frac{\mu_o I_\phi}{aB_{\phi V}(0)}. \quad (3.1.-16)$$

Neglecting corrections for $Z_{\text{eff}} > 1$, the plasma pressure balance can be written in the form

$$I_\phi = \left[\frac{2 p}{\beta_\theta \mu_o} \right]^{1/2} 2\pi a S, \quad (3.1.-17)$$

where $p = 2nk_B T$.

The systems code searches for minimum-COE design points satisfying the ignition condition while balancing the ohmic and fusion-product power inputs against radiation and transport losses, such that

$$P_Q + P_\alpha = P_{\text{RAD}} + P_{\text{TR}} \quad (3.1.-18)$$

at a profile-corrected Lawson parameter,⁷⁰ $n\tau_E$, value consistent with the stipulated average plasma operating temperature, T , typical values being summarized on Table 3.1.-III. The plasma ohmic power, P_Q , is given by

TABLE 3.1.-III
IGNITION LAWSON PARAMETER VALUES^(a)

<u>T(keV)</u>	<u>$n\tau_E$(s/m³)</u>	<u>$n\tau_E T^2$(keV² s/m³)</u>
10	1.60(10) ²⁰	1.0(10) ²²
15	0.96(10) ²⁰	2.15(10) ²²
20	0.91(10) ²⁰	3.64(10) ²²
25	0.96(10) ²⁰	5.98(10) ²²
30	1.00(10) ²⁰	9.00(10) ²²

(a)Ref. 70, consistent with $n(r)$, $T(r) \propto [1 - (r/a)^2]$.

$$P_Q = I_{\phi}^2 R_p g_{OHM} = \eta_{||} j_{\phi}^2 V_p g_{OHM} , \quad (3.1.-19)$$

where the classical plasma resistivity is given by

$$\eta_{||}(\text{ohm m}) = 9.61(10)^{-10} Z_{\text{eff}}^0 n \Lambda / \gamma_E T_e^{3/2} , \quad (3.1.-20A)$$

$$\gamma_E = 0.582 + 0.418 [(Z_{\text{eff}} - 1)/Z_{\text{eff}}]^2 . \quad (3.1.-20B)$$

3.1.2. Engineering. Given a stipulated target net electrical power output, P_E , the thermal power output, P_{TH} , and recirculating power fraction, $\epsilon \equiv 1/Q_E$, are determined for a nominal value of the thermal conversion efficiency, η_{TH} , such that $P_E = \eta_{TH}(1 - \epsilon)P_{TH}$. The gross electrical power output is $P_{ET} = \eta_{TH}P_{TH}$. A fraction $f_{AUX} = 0.07$ of P_{ET} is allocated for primary-loop pumping power and other auxiliary functions, such that $P_{AUX} = f_{AUX}P_{ET}$. The plasma ohmic power, P_Q , resistive-coil Joule dissipation in the respective coil sets, P_{TFC}^Q and P_{DFC}^Q , and possible current-drive power, P_{CD} , complete the components of recirculating power for the ATR/ST. The engineering Q-value figure of merit, Q_E , can be written as

$$Q_E = 1/\epsilon = \frac{n_{TH}(M_N P_N + P_{RAD} + P_{TR} + P_Q)}{P_Q + P_{AUX} + P_{TFC}^Q + P_{DFC}^Q + P_{CD}}, \quad (3.1.-21)$$

where M_N (1.27 for the present design, Sec. 4.2.2.) is the blanket fusion-neutron energy multiplication.

The average 14.1-MeV-neutron first-wall loading, I_w , is given by

$$I_w (\text{MW/m}^2) = \frac{P_{TH} x}{(2\pi)^2 A r_p^2 (M_N + 0.25)}, \quad (3.1.-22)$$

and $x \equiv r_p/r_w = 0.859$ for the present design (Sec. 4.2.2.5.).

The systems code uses temperature-dependent coil resistivities, ρ , for four materials of interest: copper, AMAX-copper, aluminum, and lithium, the latter being applicable to the integrated blanket/coil (IBC) concept.⁷¹ The empirical representation of resistivity as a function of conductor temperature $T(K)$ are given by^{72,73}

$$\rho_{Cu} (\text{ohm m}) = [1.700 + 0.006923(T - 295)]10^{-8} \quad (3.1.-23A)$$

$$\rho_{AMAX} (\text{ohm m}) = [1.993 + 0.006923(T - 295)]10^{-8} \quad (3.1.-23B)$$

$$\rho_{Al} (\text{ohm m}) = [2.740 + 0.011641(T - 295)]10^{-8} \quad (3.1.-23C)$$

$$\rho_{Li} (\text{ohm m}) = [21.934 + 2.598(10)^{-2}(T - 273) + 2.581(10)^{-6}(T - 273)^2 - 1.819(10)^{-9}(T - 273)^3]10^{-8} \quad (3.1.-23D)$$

The copper-coil operating temperature is assumed to be 36.5°C to give $\rho_{Cu} = 1.8(10)^{-8}$ ohm m, consistent with Refs. 2 and 8. Typically, the effective resistivity is increased by $1/\lambda_c$, where $\lambda_c \approx 0.7$ is a conductor filling fraction. Changes in the systems code to allow for recovery of the power deposited in a lithium IBC have not been implemented.

The systems code initially incorporated a somewhat idealized representation of the final ATR/ST geometry illustrated in Fig. 3.1.-2. For example, early iterations assumed a constant-radius TFC central column, rather than the graded version shown in Fig. 3.1.-2, and a D-shaped TFC, rather than the essentially window-frame coil finally adopted. Joule dissipation is reported separately for the central-column and outboard TFC legs. Major modelistic differences and other parameters deriving from either neutronics (e.g., M_N , Sec. 4.2.) or thermalhydraulics (e.g., η_{TH} , Sec. 4.4.) were reconciled prior to the final ATR/ST design-point presentation. Also, early editions of the systems code incorporated a PbLi (5% HT-9 structure) outboard blanket thickness $\Delta b_o = 0.60$ m and a shielding thickness $\Delta s_i = 0.20$ m.² The final outboard blanket/shield

Fig. 3.1.-2. Elevation view of an ATR/ST quadrant showing major subcomponents.

design thickness evolved to be $\Delta b = 1.09$ m, including the first wall, based on neutronics performance (Sec. 4.2.).

The height of the TFC central column is determined by the vertical build of the elongated plasma, a shielded divertor-coil, divertor-chamber assembly, and associated coolant manifolds (Sec. 4.7.1.), which are not shown in Fig. 3.1.-2. The outboard TFC legs ($N_{TF} = 12$) clear the outboard shield and fall inside the EFC rings. The geometrical configuration of the TFCs is illustrated in Fig. 3.1.-3. The cross-sectional area of the TFC central column in the equatorial plane is $\pi \delta_{cp}^2$ allocated to N_{TF} discrete coils on the outboard TFC legs. The radial and toroidal dimensions of the TFC outboard legs are, respectively, $\delta_{CR} = f_{CR} \delta_{cp}$ and $\delta_{CT} = f_{CT} \delta_{cp}$. It is desirable to fatten the TFC outboard legs ($f_{CR}, f_{CT} > 1$) in order to reduce the current density and decrease the Joule losses. For the ATR/ST baseline design with square cross-section TFC outboard legs, $f_{CR} = f_{CT} = 2.50$.

The iteration procedure depicted in Fig. 3.1.-1 determined the coil current density as the TFC size is varied parametrically to yield the required on-axis vacuum toroidal field, $B_{\phi V}(0)$, consistent with the target net power output, P_E . The superconducting EFCs ($j = 15$ MA/m²) and resistive DFCs ($j = 7.5$ MA/m²) are scaled in position and current from the NEO equilibrium configuration (Sec. 2.1.) using relationship summarized in Table 3.1.-IV. Drained blanket,

Fig. 3.1.-3. Schematic illustration of the relationship of the TFC outboard-leg cross section to the TFC central-column cross-section in the equatorial plane.

TABLE 3.1.-IV
POLOIDAL-FIELD COIL SCALING RELATIONSHIPS^(a)

R_{DFC}/R_T	0.89
R_{EFC}/R_T	2.78
I_{DFC}/I_ϕ	0.159
I_{EFC}/I_ϕ	0.294

$$\overline{(a)}A = 1.8, \kappa \approx 3, \delta = +0.03, I_\phi/B_{\phi V}(0) = 17.4 \text{ MA/T}$$

shield, and coil masses are calculated using homogenized densities $\rho = 7.75, 7.0, 7.3$ tonnes/m³, respectively. The FbLi eutectic breeder/coolant material density is 9.4 tonnes/m³. The FPC mass, M_{FPC} , is used to compute mass, M_{FPC} , utilization M_{FPC}/P_{TH} (tonne/MWt) and mass power density, MPD (kWe/tonne), figures of merit (Appendix C). The resistive coil density is consistent with a composition of 70 v/o copper conductor, 10 v/o stainless-steel structure, 10 v/o MgO insulation, and 10 v/o (drained) water coolant. A detailed design of the coil internals was not performed.

3.1.3. Costing. The cost database and methodology used in the present ATR/ST has been adapted from that of the CRFPR⁸ to facilitate comparisons between these concepts which share a broad underlying motivation and many features. Details of the costing procedure are discussed in Appendix B. A costing model is central to the process illustrated in Fig. 3.1.-1 insofar as the ultimate object function is the COE design points. Parametric and sensitivity results leading to the identification of ATR/ST design points are discussed in Sec. 3.2. The systems code produces detailed and summary cost tables (Appendix B). Aspects of a simplified maintenance/availability model leading to the determination of the plant factor are summarized in Table 3.1.-I. Costs are reported in 1980 dollars, with a factor of 1.348 taking these costs to January, 1986, values.^{74,75} It is possible to update a particular reported ATR/ST cost to a more recent base year using the appropriate cost escalation factor summarized in Table 3.1.-V, pending a detailed, item-by-item consideration and update of each costed account.

3.2. Parametric Results

3.2.1. Sensitivity/Tradeoffs. This section summarizes the systems analyses performed in the course of this study and leading to the baseline ATR/ST design.

TABLE 3.1.-V
COST ESCALATION FACTORS

<u>Year</u>	<u>5%/yr</u>	<u>6%/yr</u>	<u>7%/yr</u>	<u>GNP⁷⁴</u>	<u>FEDC⁷⁵</u>	<u>Sheffield¹⁵</u>
1980	1.0000	1.0000	1.0000	1.0000	1.0000	1.0000
1981	1.0500	1.0600	1.0700	1.0953	1.0830	1.1000
1982	1.1025	1.1236	1.1449	1.1623	1.1504	1.2100
1983	1.1576	1.1910	1.2250	1.2069	1.1971	1.3129
1984	1.2155	1.2625	1.3108	1.2523 ^(a)	1.2390	1.3982
1985	1.2763	1.3382	1.4026	1.2994 ^(b)	1.2824 ^(c)	--
1986	1.3401	1.4185	1.5007	1.3482 ^(b)	1.3272 ^(c)	--

(a)Estimated.

(b)Extrapolated using estimated 1983-84 rate: 1.0376.

(c)Extrapolated using 1983-84 rate: 1.0350.

Features of the computational model are discussed in Sec. 3.1. with fixed and varied parameters being summarized on Table 3.1.-I.

The dependence of 500-MWe(net) ATR/ST performance of the early D-coil configuration (i.e., prior to adoption of picture frame, variable cross-section model) on aspect ratio, A, and resulting average toroidal beta, β , are shown on Fig. 3.2.-1. As A was varied in the range 1.6-2.4, the plasma minor radius traces a locus of minimum-COE design points that itself is relatively insensitive to changes in A. The lower β values at higher A are offset by a lower $B_{CM}/B_{\phi V}(0)$ ratio. An interim design point (IDP) with A = 1.8 was selected for further calculation and refinement (i.e., estimated diverted-plasma magnetics using the NEQ code, Secs. 2.1., 2.5.), leading to higher β values and higher plasma elongation. The analytic dependence of β on A used in Fig. 3.2.-1 derives from Eqs. (3.1.-15). The NEQ code was not exercised over a range of A values to confirm this scaling.

Figure 3.2.-2 illustrates representative ATR/ST parametric results for a near-optimum aspect ratio, A = 1.8. Net reactor output power, P_E , is varied from 250-1200 MWe, exhibiting the nuclear economy of scale built into the cost database (Appendix B). At low values of a, space is restricted for the toroidal-field-coil (TFC) centerpost, resulting in higher TFC current density, increased Joule losses, higher recirculating power (i.e., lower Q_E) and, therefore, higher COE. For large values of a, the physical size of the FPC increases, resulting in lower values of 14.1-MeV neutron first-wall loading

TABLE 3.2.-I
REPRESENTATIVE ATR/ST DESIGN POINTS

Net electric power, P_E (MWe)	500.	1000.
Plasma minor dimensions, a/b (m)	1.50/4.50	1.50/4.50
Plasma major toroidal radius, R_T (m)	2.70	2.70
Plasma aspect ratio, $A = R_T/a$	1.8	1.8
Plasma volume, V_p (m ³)	358.	358.
Average plasma density, $n(10^{20}/m^3)$	1.21	1.63
Average plasma temperature, T (keV)	15.	15.
Plasma energy, W_p (GJ)	0.32	0.43
Field energy, W_B (GJ)	12.	16.
Total thermal power, P_{TH} (MWt)	2,047.	3,710.
Recirculating power fraction, $1/Q_E$	0.32	0.25
Thermal conversion efficiency, η_{TH}	0.36	0.36
Net plant efficiency, $\eta_p = \eta_{TH}(1 - 1/Q_E)$	0.24	0.27
Neutron first-wall loading, I_w (MW/m ²)	3.24	5.87
Plasma power density, P_F/V_p (MW/m ³)	4.7	8.5
Average toroidal beta, β	0.291	0.291
Field at plasma, $B_{\phi V}(T)^{(a)}$	4.10(2.30)	4.77(2.67)
Field at coil, $B_{CM}(T)$	6.9	8.0
Plasma thermal diffusivity, χ_E (m ² /s)	0.53	0.72
Plasma current, I_{ϕ} (MA)	39.8	46.2
Plasma current density, j_{ϕ} (MA/m ²)	1.9	2.2
FPC volume, V_{FPC} (m ³)	2,102.	2,120.
FPC mass, M_{FPC} (tonne)	6,344.	6,492.
FPC power density, P_{TH}/V_{FPC} (MWt/m ³)	1.0	1.75
FPC mass utilization, M_{FPC}/P_{TH} (tonne/MWt)	3.1	1.75
Mass power density, $1000P_E/M_{FPC}$ (kWe/tonne)	79.	154.
FPC unit cost (\$/kg)	45.	45.
(FPC Cost)/(total direct cost), FPC/TDC	0.24	0.20
Unit direct cost, UDC(\$/kWe) ^(b)	2,441.	1,485.
Cost of electricity, COE(mills/kWeh) ^(b,c)	61.4/116.0	37.8/71.1

(a)Values in parentheses are on-axis vacuum fields.

(b)1980 dollars.

(c)Constant/then-current dollars based on a 6-year construction time and a 76% plant availability.

Fig. 3.2.-1. Dependence of an early ATR/ST configuration COE performance on plasma aspect ratio, $A = R_T/a$ leading to the selection of an interim design point (IDP) $A = 1.8$ for subsequent calculations.

(NWL), lower FPC power density and higher FPC direct costs for fixed net power output. Minimum-COE design points are indicated for each net-power curve in Fig. 3.2.-2, with 1200-MWe-class ATR/STs yielding COE values comparable to or below that for the STARFIRE design⁷; the minimum-cost ATR/ST design has a higher recirculating power fraction (0.25 versus 0.17) that trades off with a significantly smaller, less-costly FPCs.

The major drivers of COE at the nominal ATR/ST plant factor of $p_f = 0.76$ are the unit direct cost, UDC (\$/kWe) and the engineering Q-value, Q_E . Figure 3.2.-3A illustrates the dependence of these figures of merit for the minimum-COE design points of Fig. 3.2.-2 on net electrical output power P_E (MWe), again illustrating the nuclear economy of scale of the cost database. The corresponding curves of Fig. 3.2.-3B for fixed $a = 1.5$ m (the baseline ATR/ST geometry) show the same general trends, but somewhat larger physical size and direct cost of these devices allow thicker TFCs, lower coil current density and Joule losses, and higher Q_E values. The final thermal conversion efficiency,

Fig. 3.2.-2. Dependence of ATR/ST constant-dollar (1980) cost of electricity (COE) on plasma radius a for fixed aspect ratio $A \equiv R_T/a = 1.8$ and various values of net electric power output P_E (MWe). Minimum-COE design points for each P_E value are indicated by open circles. Contours of constant 14.1-MeV neutron first-wall loading are indicated by darkened lines. The baseline ATR/ST design point is shown and the STARFIRE⁷ ($A = 3.6$) cost is shown for comparison.

$\eta_{TH} = 0.36$ (Sec. 4.4.2.), and blanket neutron energy multiplication, $M_N = 1.27$ (Sec. 4.2.2.), determined by specific subsystem analyses have been used here; the preliminary values of Table 3.1.-I and Fig. 3.2.-2 are thereby updated.

3.2.2. Design-Point Alternatives/Selection. A design point at 500 MWe(net) was selected for further elaboration to characterize tokamak operation at moderate unit sizes and with a neutron wall loading ($I_w = 5.7$ MW/m² peak, 3.3 MW/m² average) sufficiently low to be consistent with an outboard blanket using a flowing PbLi eutectic breeder/coolant material that also cools the first wall. Parameters of this design are summarized in Table 3.2.-I together with a 1000-MWe design having similar geometry. This design, at the expense of higher I_w , surpasses the MFAC Panel X mass power density, MPD threshold value^{16,17} of 100 kWe/tonne. The baseline 500-MWe case falls short of this target but improves significantly upon the STARFIRE⁷ value of ~ 50 kWe/tonne. A 600-MWe

Fig. 3.2.-3. Dependence of ATR/ST cost of electricity, COE, unit direct cost, UDC, and engineering Q-value, Q_E , on net electrical power output, P_E for a) minimum-COE design points, and b) constant $a = 1.5$ -m, lower- I_w design points.

design, comparable to MINIMARS,²¹ yields $MPD \approx 100 \text{ kWe/tonne}$ at $I_w \approx 5 \text{ MW/m}^2$. The output summary from the systems for the 500-MWe ATR/ST design point is reproduced in Table 3.2.-II, and the corresponding cost summary is reproduced in Table 3.2.-III. The ATR/ST baseline design geometrical configuration for $N_{TF} = 12$ TFCs is illustrated in quadrant elevation and plan views in Figs. 3.2.-4. This basic configuration is used as the basis for the mechanical engineering layout and elaboration discussed in Sec. 4.5.

3.2.3. Design-Point Sensitivities

3.2.3.1. Beta. A multiplier $0 < f_\beta < 1$ was applied to the Troyon-Gruber beta scaling [Eq. (3.1.-16)] to estimate the sensitivity of the ATR/ST design point to degradations in assumed physics performance. As shown in Fig. 3.2.-5A, operation at lower beta values for a given target net power output tends to require higher magnetic fields and coil currents in a given geometry with reduced values of Q_E . The minimum-COE design point shifts to larger values of plasma radius, a , as beta is reduced in proportion to f_β . The average 14.1-MeV-neutron first-wall loading of the minimum-COE design point drops off slightly, as is shown in Fig. 3.2.-5B, given that $I_w \propto \beta^2 B^4 a$. The decrease impacts on the ATR/ST unit direct costs and COE as shown in Fig. 3.2.-6. Generally, the higher costs as f_β is decreased reflect larger physical sizes of the fusion power core at the minimum-COE points and larger balance-of-plant costs as the Q_E decreases. For f_β in the range 0.8-1.0, the departure of ATR/ST from nominal values is relatively modest, suggesting some design flexibility in response to better resolved physics scaling.

3.2.3.2. Normal-Conducting Versus Superconducting Coils. The ATR/ST has generally been conceived as a resistive-coil device in order to investigate compact fusion-power-core embodiments with high mass power density. The systems code in an early phase was modified to establish a basis for comparison between normal-conducting (NC) and superconducting (SC) ATR/ST configurations. The superconducting case involved reduction of the TFC resistivity to a negligible value to suppress the Joule dissipation, leaving the auxiliary recirculating power and a small plasma ohmic power requirement such that the engineering Q-value $Q_E \approx 14$. The resistive-coil configuration at 500 MWe(net) yielded $Q_E \approx 2$, prior to the improvements and re-optimization (i.e., variable TFC thickness) leading to the final embodiment with $Q_E \approx 3.0$. Figure 3.2.-7 compares the performance of the two options in terms of Q_E and neutron-wall loading. The curves are annotated with values of the overall (including

TABLE 3.2.-II

ATR/ST SYSTEMS CODE OUTPUT SUMMARY

Plasma Parameters

Minor plasma radius, $a(m)$	1.50
Major toroidal radius, $R_T(m)$	2.70
Average plasma radius, $r_p(m)$	2.60
Plasma aspect ratio, $A = R_T/a$	1.80
Plasma volume, $V_p(m^3)$	358.31
Plasma current, $I_\phi(MA)$	39.84
Toroidal plasma current density, $j_\phi(MA/m^2)$	1.88
Average plasma temperature, $T(keV)$	15.00
Lawson parameter, $n\tau_E(10^{20}s/m^3)$	0.96
Average plasma density, $n(10^{20}/m^3)$	1.21
Energy confinement time, $\tau_E(s)$	0.79
Thermal diffusivity, $\chi_E(m^2/s)$	0.53
DT fusion power density, $P_F/V_p(MW/m^3)$	4.67
Poloidal beta, β_θ	0.23
Toroidal beta, β	0.29
Pinch parameter, $\Theta = \bar{B}_\theta(r_p)/\langle B_\phi \rangle$	0.47
Reversal parameter, $F = \bar{B}_\phi(r_p)/\langle B_\phi \rangle$	0.76
Average plasma/first-wall radius, x	0.67
Streaming parameter, $I_\phi/N(10^{-14}A/m)$	1.56
Plasma ohmic dissipation during burn, $P_\Omega(MW)$	14.99

Poloidal-Field-Coil Quantities

Mass of DFC set (tonne)	185.97
Mass of EFC set (tonne)	538.17
Mass of PFC (DFC + EFC) set (tonne)	724.14
Current density in DFC (MA/m^2)	7.50
Ohmic dissipation during burn, $P_\Omega^{DFC}(MW)$	36.85
Volumetric heating during burn (MW/m^3)	1.45

Toroidal-Field-Coil Quantities

TFC central-column half-height (m)	8.40
Coil radius in the equatorial plane, $\delta_{cp}(m)$	0.90
Outboard/inboard TFC thickness ratios, f_{CR}/f_{CT}	2.5/2.5
TFC stress (MPa)	58.37
Average minor radius of TFC central column, $r_{c\phi}$	0.45
Mass of TFC (tonne)	3,253.93
On-axis vacuum toroidal field, $B_{\phi V}(0)(T)$	2.30
Magnetic field at the central column, $B_{CM}(T)$	6.89
Vertical field, $B_V(T)$	-1.63
Toroidal-field on-axis ripple (%)	0.18
Toroidal-field outboard ripple (%)	1.21
Maximum energy stores in the coil, $W_{B\phi}(GJ)$	5.89
Toroidal-coil current, $I_{\theta c}(MA)$	2.58
Toroidal-coil current density, $j_{c\phi}(MA/m^2)$	12.19
Central-column TFC ohmic dissipation (MW)	96.28
Outboard TFC ohmic dissipation (MW)	35.98
Total ohmic dissipation during burn, $P_\Omega^{TFC}(MW)$	132.26
Volumetric heating during burn (MW/m^3)	0.25

TABLE 3.2.-II (Continued)
ATR/ST SYSTEMS CODE OUTPUT SUMMARY

Engineering Summary

Engineering Q-value, Q_E	3.11
Ohmic Q-value, Q_T	9.08
Plasma Q-value, Q_P	111.50
RF driver power, P_{RF} (MW)	1,671.84
Total thermal power, P_{TH} (MW)	2,047.38
First-wall surface area, A_{FW} (m ²)	415.79
14.06-MeV-neutron first-wall loading, I_W (MW/m ²)	3.24
14.06-MeV-neutron blanket energy multiplication, M_N	1.27
Circularized first-wall radius, r_w (m)	3.90
Minor radius of system, r_s (m)	6.28
Blanket volume (m ³)	403.60
Inboard shield volume (m ³)	24.45
Outboard shield volume (m ³)	89.46
Divertor shield volume (m ³)	44.73
Total shield volume (m ³)	158.65
TFC central-column volume (m ³)	77.76
TFC outboard-legs volume (m ³)	367.98
Total TFC volume (m ³)	445.74
DFC volume (m ³)	25.48
EFC volume (m ³)	73.72
FPC volume (m ³)	99.20
Mass of first wall/blanket (tonne)	166.71
Mass of shield (tonne)	1,110.51
Mass of TFC set (tonne)	3,253.93
Mass of PFC set (tonne)	724.14
Total coil-set mass (tonne)	3,978.07
Mass of FPC structure (tonne)	1,038.79
Fusion-power-core mass, M_{FPC} (tonne)	6,344.09
Mass of divertor assembly (tonne)	558.46
Mass of TFC centerpost (tonne)	567.65
Mass of TFC-centerpost/inboard-shield assembly (tonne)	738.83
Mass of drained blanket/outboard-shield assembly (tonne)	792.94
Fusion-power-core mass utilization, M_{FPC}/P_{TH}	3.10
FPC mass power density, $1000P_E/M_{FPC}$ (kWe/tonne)	78.81
System power density, P_{TH}/V_{FPC} (MWt/m ³)	0.98
Blanket thickness, Δb (m)	1.09
Blanket power density (MW/m ³)	5.07
Magnetic energy recovery time, W_B/P_E (s)	2.88

TABLE 3.2.-III

ATR/ST SYSTEMS CODE COST SUMMARY (1980-DOLLARS)

Acct. No.	Account Title	Million Dollars
20.	Land and land rights	3.30
21.	Structures and site facilities	274.26
22.	Reactor plant equipment	611.57
22.1.1.	Blanket and first wall	44.22
22.1.2.	Shield	16.65
22.1.3.	Magnets	159.11
22.1.4.	Supplemental heating systems	0.00
22.1.5.	Primary structure and support	20.42
22.1.6.	Reactor vacuum systems (unless integral elsewhere)	16.60
22.1.7.	Power supply, switching and energy storage	29.48
22.1.8.	Impurity control	48.04
22.1.9.	Direct energy conversion system	0.00
22.1.10.	ECRH breakdown system	38.43
22.1.	Reactor equipment	372.95
23.	Turbine plant equipment	156.95
24.	Electric plant equipment	89.34
25.	Miscellaneous plant equipment	41.83
26.	Special materials	43.24
90.	Direct cost	1,220.48
91.	Construction facilities, equipment and services (10%)	122.05
92.	Engineering and construction management services (8%)	97.64
93.	Other costs (5%)	61.02
94.	Interest during construction (IDC)	193.65/474.38 ^(a)
95.	Escalation during construction (EDC)	0.00/285.23 ^(b)
99.	Total Cost	1,694.85/2,260.81

Thermal power, P_{TH} (MWt)	= 2,047.38
Gross electric power, P_{GT} (MWe)	= 737.06
Net electric power, P_{NET} (MWe)	= 500.00
Engineering Q-value, Q_E	= 3.11
Plant availability factor, p_f	= 0.76

Construction time = 6 yr: Constant	Then-Current
f_{IDC} 0.129	0.316
f_{EDC} 0.000	0.190

	Acct. No.	Constant	Then-Current
Unit direct cost, UDC(\$/kWe)	[90]	= 2,440.98	2,440.98
Unit base cost, UBC (\$/kWe)	[93]	= 3,002.40	3,002.40
Unit total cost, UTC(\$/kWe)	[99]	= 3,389.71	4,521.61
Capital return (mills/kWeh)		= 50.99	102.02
O&M (2.0%)(mills/kWeh)	[40-47,51]	= 9.03	12.10
B/FW replacement (mills/kWeh)	[50]	= 1.35	1.80
Deuterium fuel (mills/kWeh)	[02]	= 0.04	0.04
COE (mills/kWeh)		= 61.40	115.97

(a)Using constant/then-current-dollar f_{IDC} (b)Using constant/then-current-dollar f_{EDC}

Fig. 3.2.-4. ATR/ST baseline geometrical configuration in a) elevation and b) plan views for $N_{TF} = 12$ TFCs.

Fig. 3.2.-5. Response of 500-MWe(net) ATR/ST parametric analysis results to degradations in the Troyon-Gruber beta scaling as monitored by the factor f_β in terms of a) engineering Q-value, Q_E , and b) average neutron first-wall loading.

Fig. 3.2.-6. Response of 500-MWe(net) ATR/ST parametric analysis results to degradations in the Troyon-Gruber beta scaling as monitored by the factor f_β in terms of a) unit direct cost, UDC, and b) constant-dollar cost of electricity, COE. The dashed curve is the then-current-dollar COE curve corresponding to the $f_\beta = 1.0$ curve.

Fig. 3.2.-7. Comparison of normal-conducting (NC) and superconducting (SC) ATR/ST(901) performance in terms of a) engineering Q-value, Q_E , and b) 14.1-MeV-neutron average first-wall loading. Coil current density, j_c (MA/m²), is indicated for the two options. The SC option incorporates 1.0 m of inboard and outboard shielding compared with 0.2 m for the NC case.

conductor, coolant passages, structure, insulation, and superconductor) coil current density, j_c . The superconducting case is limited to lower neutron wall loading ($1\text{--}2\text{ MW/m}^2$) and higher plasma radius ($a > 2.3\text{ m}$) for the target $500\text{ MWe}(\text{net})$ power output because of the additional inboard shielding ($\Delta s_i = 0.2\text{ m}$ for NC, 1.0 m for SC) and outboard shielding ($\Delta s_o = 0.2\text{ m}$ for NC, 1.0 m for SC) standoff assumed.

The resulting cost comparison between the NC and SC cases is illustrated on Figs. 3.2.-8 in terms of unit direct costs and cost of electricity, COE, respectively. The minimum-COE NC design point yields somewhat lower costs than the SC cases for typical values of $j_c = 15\text{--}20\text{ MA/m}^2$. The cost differences considered alone are not large ($\sim 15\%$), but result from a dramatically distinct outcome of the usual recirculating-power versus FPC size/cost tradeoff.

3.2.3.3. Unit Coil Costs. The response to changes in the nominal homogenized coil cost ($C_c = 40\text{ \$/kg}$) was examined. Figure 3.2.-9 illustrates the sensitivity of minimum-COE design points in response to a factor of two increase (decrease) in this key parameter in terms of engineering Q-value, Q_E , and 14.1-MeV -neutron average first-wall loading. As expected, high coil unit cost forces the minimum-COE design point to smaller values of plasma radius for a given target net power output in order to reduce the size of the FPC and its impact on overall cost at the expense of slightly lower values of Q_E . Conversely, lower unit coil cost allows the minimum-COE design point to occur at a slightly larger plasma radius, with lower coil current density resulting in lower Joule dissipation and slightly higher values of Q_E .

The neutron wall loading corresponding to these minimum-COE design points varies more dramatically with a . The resulting impacts of the perturbation of unit coil costs on overall costs are illustrated in Fig. 3.2.-10 in terms of UDC and COE, respectively. The cost of the magnets is the dominant component of the FPC cost. Higher(lower) unit coil costs push the minimum-COE design point to lower(higher) values of plasma radius. Costs of the ATR/ST vary directly to coil costs but the impact on COE is not large in the vicinity of the minimum COE design point. Similar variations in unit costs for the superconducting (SC) option (Sec. 3.2.3.2.) would be expected to perturb dramatically the resulting costs insofar as the SC coils at a larger standoff from the plasma exert a greater leverage on FPC costs, with little offsetting improvement in Q_E as is seen for the NC case.

Fig. 3.2.-8. Comparison of normal-conducting (NC) and superconducting (SC) performance in terms of a) unit direct cost, UDC, and b) cost of electricity, COE. Coil current density, j_c (MA/m²) is indicated for the two options. The SC option incorporates 1.0 m of inboard and outboard shielding compared with 0.2 m for the NC case. Constant- (solid curves) and then-current-(dashed curves) dollar COE results are shown.

Fig. 3.2.-9. Response of minimum-COE design points to perturbation about the nominal homogenized unit coil cost ($C_c = 40$ \$/kg) in terms of a) engineering Q-value, Q_E , and b) 14.1-MeV-neutron average first-wall loading.

Fig. 3.2.-10. Response of performance in terms of a) unit direct cost, UDC, and b) cost of electricity, COE to perturbation about the nominal homogenized unit coil cost ($C_c = 40$ \$/kg).

3.2.3.4. RF Coupling Efficiency. The nominal (steady-state) ATR/ST configuration supposes efficient oscillating-field current drive (OFCD) to sustain the toroidal plasma current, I_ϕ . Fast-wave current drive (FWCD) is considered as a backup (Sec. 2.4.2.2.), with the understanding that RF waves couple to the plasma more efficiently at higher values of average plasma temperature, T , and lower average density. Under the assumption of a lower-hybrid current-drive efficiency⁶⁷ $(A/W) = [0.1/(n/10^{20})](7/R_T)(T/10)$ the impact of FWCD can be estimated at RF power costs at below 0.40 \$/W. The RF supply itself is assumed to have 60% efficiency. Variation in COE as a function of T using FWCD are illustrated on Fig. 3.2.-11. The nominal 500-MWe(net) ATR/ST cost curve (solid line) is shown with its minimum-COE (open circle) and lower- I_w (solid square) design points, with $P_{RF} = 0$ indicating the OFCD option. At the same nominal value, $T = 15$ keV, with RF current drive such that $P_{RF} \neq 0$, the COE

Fig. 3.2.-11. Response of parametric cost results to the temperature-dependent coupling of RF waves as the backup current drive option.

value increases considerably because of the significantly higher recirculating power associated with inefficient RF coupling. If the plasma operating temperature is assumed to rise to $T = 20$ keV, the nominal cost behavior can be recovered. Additional increases in T result in increasing values of COE as the fusion power density itself decreases with $\langle \sigma v \rangle_{DT}/T^2$ (Table 3.1.-II), offsetting the continuing improvement in RF coupling efficiency.

3.2.3.5. Multiplexing. Multiplexing (i.e., several small FPC units sharing common BOP systems at a given generating site) has been proposed as a potential cost savings measure for magnetic fusion systems.⁷⁶ Modifications of the systems code to accommodate this option were made, and typical results are displayed on Fig. 3.2.-12. Nominal station sizes of 500, 1000, and 1200 MWe(net) are considered with the single-unit ($N = 1$) baseline results indicated for comparison. The ATR/ST design point is illustrated by the solid square.

Fig. 3.2.-12. Comparison of ATR/ST constant-dollar cost performance for single-unit ($N = 1$) and multiplexed ($N = 2$) station with target output powers, $P_E = 500, 1000, \text{ and } 1200$ MWe(net).

Because of lower Q_E values for the resistive coil systems, the COE of multiplexed FPCs is substantially higher than the COE of the corresponding single-unit station. Furthermore, costs are more sensitive (i.e., less broad minima) at lower net-electric outputs. All of the minimum-COE FPCs indicated on Fig. 3.2.-12 have similar sizes (i.e., $a \approx 1.1$ m) but neutron wall loading and Q_E performance vary significantly. Higher levels of multiplexing ($N > 2$) extend these trends. The conclusion for multiplexed superconducting systems may be different, however.

3.2.3.6. Mass Power Density. The mass power density [MPD, ratio of net power output to mass of FPC (kWe/tonne)] has been proposed^{16,17} as a useful figure of merit for assessing the performance of conceptual fusion reactor designs (Appendix C). The dependence of COE on MPD is illustrated in Fig. 3.2.-13 for several target values of net power output, P_E . In addition to the expected nuclear economy of scale as a function of P_E , the curves on

Fig. 3.2.-13. Dependence of ATR/ST constant-dollar cost of electricity, COE, on the mass power density (MPD, ratio of net electric power to mass of FPC) figure of merit.

Fig. 3.2.-13 illustrate the desirability of operating with $\text{MPD} \geq 100\text{--}200 \text{ kWe/tonne}$. The highest MPD values result at the expense of increasing neutron wall loading. Above $\sim 100 \text{ kWe/tonne}$, incremental cost savings are small. Additionally, non-cost considerations may be important in this regime, however. A comparison of ATR/ST and CRFPR performance in terms of the MPD figure of merit and further discussion of this cost versus MPD tradeoff can be found in Appendix C.

4. PRELIMINARY ENGINEERING DESIGN

4.1. Design Overview

The cost-minimizing systems analysis (Sec. 3.2.) guided by the physics models (Sec. 2.) has lead to the baseline, 500-MWe(net) ATR/ST design summarized in Table 4.1.-I. Figure 4.1.-1 gives an approximate plan and elevation view based on the results of the systems code and approximate engineering estimates of key FPC subsystems. Before these estimates could be made, a number of design choices were required in order to allow a formulation and evaluation of the FPC neutronics model (Sec. 4.2.), from which evolved a better resolved FPC power balance (Sec. 4.3.) and thermal-hydraulic analyses (Sec. 4.4.). The main elements used to guide these FPC design choices are listed as follows:

- ♦ Use of double-null poloidal-field divertors for active impurity control
 - resistive divertor coils
 - separate inboard/outboard divertor channels
 - independently maintainable units including horizontal legs of TFCs, DFCs, divertor channels, and associated shielding
 - thin (~ 0.2-m-thick) water-cooled inboard steel shield operated at conditions similar to those in a pressurized-water fission reactor (PWR) (270-290 C, ~ 2000 psi)
 - water-cooled divertor plates, also operated at PWR conditions
- ♦ Use of $\text{Pb}_{83}\text{Li}_{17}$ (PbLi) self-cooled breeding blanket only on outboard side of torus
 - vertical flow to minimize MHD pressure drops
 - integrally cooled (PbLi) first wall conformed to plasma
 - thin PbLi-cooled steel shield outside blanket
 - single-piece maintenance of blanket/shield system through vertical lift
 - acceptable tritium breeding ratio (TBR)
- ♦ Resistive (copper) TFCs with breakable joints at centerpost and outboard horizontal legs
 - centerpost, horizontal legs, and outboard vertical legs, operated near room temperature, water-cooled, but protected by a thin (~ 0.20-m-thick) water-cooled (PWR conditions) steel shield

TABLE 4.1.-I
ATR/ST PARAMETER SUMMARY

Net electric power, P_E (MWe)	500.
Plasma minor dimensions, a/b (m)	1.50/4.50
Plasma major toroidal radius, R_T (m)	2.70
Plasma aspect ratio, $A = R_T/a$	1.8
Plasma volume, V_p (m ³)	358.
Average plasma density, $n(10^{20}/m^3)$	1.21
Average plasma temperature, T (keV)	15.
Plasma energy, W_p (GJ)	0.32
Field energy, W_B (GJ)	12.
Total thermal power, P_{TH} (MWt)	2,047.
Recirculating power fraction, $1/Q_E$	0.32
Thermal conversion efficiency, η_{TH}	0.36
Net plant efficiency, $\eta_p = \eta_{TH}(1 - 1/Q_E)$	0.24
Neutron first-wall loading, I_w (MW/m ²)	3.24
Plasma power density, P_F/V_p (MW/m ³)	4.7
Average toroidal beta, β	0.291
Field at plasma, B_ϕ (T) ^(a)	4.10(2.30)
Field at coil, B_{CM} (T)	6.9
Plasma thermal diffusivity, χ_E (m ² /s)	0.53
Plasma current, I_ϕ (MA)	39.8
Plasma current density, j_ϕ (MA/m ²)	1.9
FPC volume, V_{FPC} (m ³)	2,102.
FPC mass, M_{FPC} (tonne)	6,344.
FPC power density, P_{TH}/V_{FPC} (MWt/m ³)	1.0
FPC mass utilization, M_{FPC}/P_{TH} (tonne/MWt)	3.1
Mass power density, $1000P_E/M_{FPC}$ (kWe/tonne)	79.
FPC unit cost (\$/kg)	45.
(FPC Cost)/(total direct cost), FPC/TDC	0.24
Unit direct cost, UDC(\$/kWe) ^(b)	2,441.
Cost of electricity, COE(mills/kWeh) ^(b,c)	61.4/116.0

(a)Value in parentheses is on-axis vacuum toroidal field.

(b)1980 dollars.

(c)Constant/then-current dollars based on a 6-year construction time and a 76% plant availability.

Fig. 4.1.-1. Elevation and plan views of the 500-MWe(net) ATR/ST baseline design used to make preliminary engineering estimates of the FPC requirements.

- single-piece maintenance of TFC centerpost conductor
- ♦ All systems within and including TFCs positioned inside vacuum tank
- ♦ Superconducting EFCs outside primary vacuum tank but within vacuum cavity

These goals are consistent with the design suggested in Fig. 4.1.-1 by the systems code and together form an engineering model with which to examine key technology issues. Focus of this effort has been on the neutronics performance (Sec. 4.2.), engineering energy balance (Sec. 4.3.), and blanket/shield thermalhydraulics (Sec. 4.4.). The treatment of the FPC mechanical and electrical design (Sec. 4.5.) includes an overall integration (Sec. 4.5.1.), a description of the maintenance approach (Sec. 4.5.2.), as well as a brief description of other key systems (Sec. 4.5.3.). Although the treatment of the array of other FPC systems has of necessity been somewhat superficial at the level of this scoping study, the goal of this study, nevertheless, remains a reasonably self-consistent, but preliminary FPC integration from the thermal, nuclear, and maintenance viewpoints.

4.2. Neutronics

The neutronic performance of the ATR/ST was estimated with the three-dimensional Monte Carlo neutron-photon transport code MCNP.¹³ The MCNP code is a state-of-the-art neutral-particle transport code that includes the ability to model three-dimensional configurations bounded by elliptical tori. Point-wise cross-section data with linear interpolation are used. The number of points is chosen to reproduce the original cross-section tabulation to within a tolerance of 0.1-0.5%. The MCNP cross-section libraries used for the ATR/ST analysis were derived from the ENDF/B-V cross-section evaluation.⁷⁷

Calculations of the tritium breeding ratio, TBR, and nuclear heating were made for several preliminary ATR/ST configurations. The purpose of these calculations was to guide the design towards a final configuration. These preliminary calculations indicated that blanket coverage on the inboard side of the low-aspect-ratio ATR/ST was not required for adequate tritium breeding. The analysis and results given in this section, however, pertain only to the final ATR/ST configuration given in Fig. 4.1.-1.

4.2.1. Neutronics Model. The geometry used for the MCNP model is taken from Fig. 4.1.-1 and is adapted as shown in Fig. 4.2.-1. The correspondence between the cell numbers given on Fig. 4.2.-1 and material specification is

Fig. 4.2.-1. Schematic diagram of MCNP model of the 500-MWe(net) ATR/ST baseline case.

TABLE 4.2.-I
MCNP CELL ID AND INTERNAL CORRESPONDENCE

<u>Region</u>	<u>Cells</u>	<u>Material(volume %)</u>
TFC	101-103	70% Cu, 10% PCA, 10% MgO, 10% H ₂ O
DFC	124, 136, 148	70% Cu, 10% PCA, 10% MgO, 10% H ₂ O
EFC	106, 107, 111	80% Cu, 20% He
Central column shield	201-214	90% HT-9, 10% H ₂ O
Outboard shield	215-216	100% PCASS
Blanket	301-331	95.2% Pb ₈₃ Li ₁₇ , 4.8% HT-9 (90% enriched in ⁶ Li)
Vacuum	104, 401-408, 410-414,501-505	--

given in Table 4.2.-I. Each cell represents a homogeneous mixture of the components indicated in Table 4.2.-I.

A fusion-neutron source routine was written for MCNP which sampled source particles from sixteen nested plasma regions bounded by concentric elliptical tori as illustrated in Fig. 4.2.-2. The coordinates and source intensity for each elliptical torus is given in Table 4.2.-II. The source routine randomly

Fig. 4.2.-2. MCNP model with fusion-neutron source.

TABLE 4.2.-II

ELLIPTICAL TORI AND MCNP NEUTRON SOURCE SPECIFICATION

<u>Source Region</u>	<u>Central Radius(m)</u>	<u>Width(m)</u>	<u>Height(m)</u>	<u>Normalized Neutron Source</u>
1	2.9335	0.	0.	$2.2374(10)^{-1}$
2	2.9218	0.3411	0.9271	$1.8760(10)^{-1}$
3	2.9100	0.4850	1.3142	$1.5428(10)^{-1}$
4	2.8976	0.5968	1.6110	$1.2400(10)^{-1}$
5	2.8851	0.6918	1.8615	$9.6971(10)^{-2}$
6	2.8726	0.7772	2.0832	$7.3355(10)^{-2}$
7	2.8594	0.8551	2.2866	$5.3269(10)^{-2}$
8	2.8461	0.9288	2.4760	$3.6756(10)^{-2}$
9	2.8324	0.9972	2.6548	$2.3755(10)^{-2}$
10	2.8183	1.0634	2.8264	$1.4089(10)^{-2}$
11	2.8038	1.1273	2.9950	$7.4340(10)^{-3}$
12	2.7886	1.1896	3.1620	$3.3209(10)^{-3}$
13	2.7727	1.2508	3.3305	$1.1521(10)^{-3}$
14	2.7559	1.3113	3.5066	$2.6192(10)^{-4}$
15	2.7379	1.3720	3.6988	$2.6011(10)^{-5}$
16	2.7183	1.4333	3.9281	$2.7078(10)^{-7}$
17	2.6963	1.4963	4.3435	$6.0677(10)^{-17}$

selects a particular source region at a frequency appropriate with the source strength given in Table 4.2.-II; a source-particle starting position is then chosen uniformly from within that region. For determination of TBR and nuclear heating, 10,000 source particle histories were followed yielding a relative error in the TBR of $\sim 1\%$ and $\sim 5\%$ in the heating of critical components. Determination of dpa, H and He production and transmutation rates in the main FPC components required 20,000 source histories to achieve relative errors less than $\sim 10\%$.

4.2.2. Neutronics Results.

4.2.2.1. Tritium Breeding Ratio. The TBR computed for the baseline ATR/ST configuration was 1.12 with a relative error of 0.8%. As summarized in Table 4.2.-I, the blanket material used in the analysis was a homogeneous mixture of the lead-lithium eutectic $\text{Pb}_{83}\text{Li}_{17}$ and HT-9 structural material. The lithium was 90% enriched in ^6Li . The variation of the TBR with ^6Li content is

Fig. 4.2.-3. Tritium breeding ratio as a function of ^6Li enrichment.

Fig. 4.2.-4. Volumetric heating rate as a function of distance into the blanket averaged over the blanket from the equatorial plane to $z = 0.9$ m.

given in Fig. 4.2.-3. Since the space between the blanket and the outer TFC leg is premium, increasing the TBR is best achieved by increasing the ^6Li enrichment or by providing breeding-blanket coverage at the inboard side.

4.2.2.2. Nuclear Heating. The nuclear heating in all FPC components was computed using the coupled neutron-photon transport mode of MCNP. The total (recoverable) nuclear heat deposited in the blanket and shields was

TABLE 4.2.-III
NUCLEAR HEAT DEPOSITION FOR BASELINE ATR/ST

Region	Cell ^(a)	Percent 14.1-MeV Neutron Power	Volumetric Heating Rate (MW/m ³)	Cell ^(a) Volume (m ³)
TFC	101	1.091	3.49	4.58
Central	102	2.278	2.07	16.14
Column	103	1.548	0.36	63.71
	105	0.110	0.15	10.71
TFC Outboard	124,126,148	0.263	0.012	330.9
DFC	106,107	0.823	0.57	21.11
EFC	111	0.0266	0.004	94.25
TFC	201	4.641	30.08	2.26
Central-	202	9.568	22.14	6.33
Column	203	7.207	7.96	13.37
Shield	204	0.889	1.30	1.02
	205	0.251	2.50	1.47
DFC	206	0.945	2.02	6.86
Shield	207,208,209	11.211	10.99	14.94
	210,211,212	3.011	1.62	27.15
	213	1.885	11.13	2.48
Outboard	214	0.962	0.52	26.97
Shield	215	0.042	0.03	20.59
	216	0.272	0.087	45.63
First	251	0.668	17.78	0.55
Wall	253	1.191	23.25	0.75
Blanket	301	4.012	1.46	40.24
	302	25.566	6.15	60.84
	331	54.796	7.97	100.75
Total		133.257		913.60

(a)Refer to Fig. 4.2.-1.

17.8 MeV/fusion or $M_N = 1.27$ times the 14.1-MeV fusion neutron power. The nuclear heating is summarized in Table 4.2.-III. A more detailed breakdown of nuclear heating in the blanket was also calculated as input to the blanket thermalhydraulics calculations, an example of which is shown in Fig. 4.2.-4. This distribution is an average over a region of the blanket extending to 0.9 m above the equatorial plane. The value for the first-wall heating is also shown. The lower volumetric heating rate for the first wall is attributed to the absence of water coolant (moderator) in the first wall compared to designs that require separate first-wall coolants.² For the average neutron first-wall loading of $I_w = 3.3 \text{ MW/m}^2$ over the region of interest, the first-wall heating rate amounts to $4.7 \text{ MW/m}^3/\text{MW/m}^2$ averaged over a height of 0.9 m above and below the equatorial plane. In a separate calculation, a water-cooled first wall similar to the CRFPR^{2,78} was considered; a first-wall heating load of $14.6 \text{ MW/m}^3/\text{MW/m}^2$ and front blanket heat load of $9.7 \text{ MW/m}^3/\text{MW/m}^2$ resulted for the same first-wall segment shown in Fig. 4.2.-3.

4.2.2.3. Radiation Effects. A 30-group dpa cross-section set was obtained through the TRANSX1 MATXS system⁷⁹ and used with the dose-function option of MCNP. The H/He gas-production and dose rates were obtained directly from the MCNP calculations and are summarized in Table 4.2.-IV. The dpa, gas production, and dose rates for all other magnet components were significantly less than the lowest values in Table 4.2.-IV. Gas-production and dpa rates calculated for the inner shield and first wall are tabulated in Table 4.2.-V.

TABLE 4.2.-IV
DPA, H/He PRODUCTION, and DOSE RATES IN MAJOR MAGNET COMPONENTS
EXPRESSED ON THE BASIS OF A FULL POWER YEAR

	Cell ^(a)	dpa/yr	H appm/yr	He appm/yr	Copper Dose (10^{10} rad/yr)	Insulator Dose (10^{10} rad/yr)
TFC	101	0.92	13.53	2.15	111	770
Central	102	0.58	11.54	1.98	7.11	506
Column	103	0.08	0.45	0.06	14.0	0.895
TFC	105	0.02	0.28	0.06	3.92	0.267
Horizontal Conductors						
DFC	106	0.06	0.37	0.05	9.94	0.558
	107	0.47	5.54	0.89	83.1	5.55

(a)Refer to Fig. 4.2.-1.

TABLE 4.2.-V
DPA AND H/He PRODUCTION RATES IN INNER SHIELD AND FIRST WALL
COMPONENTS AFTER ONE FULL POWER YEAR

	Cell(a)	dpa/yr	H appm/yr	He appm/yr
TFC	201	18.87	704	172
Inboard	202	14.15	483	119
Shield	203	3.60	65	15
DFC	204	8.11	233	57
Inboard	205	0.96	35	8.7
Divertor Chamber	206	0.71	12	2.9
DFC	207	6.37	198	48
Shield	208	6.82	189	46
	209	1.52	34	8.1
Outboard	210	0.32	2.4	0.5
Divertor	211	1.01	34	8.3
Chamber	212	0.74	23	5.6
	213	7.74	187	46
First	251	37.42	1322	327
Wall	253	56.96	2460	614

(a) Refer to Fig. 4.2.-1.

Fig. 4.2.-5. Copper activation chains (isotopes in boxes are stable).

4.2.2.4. Transmutation of Magnet Conductor. Quantitative estimates were made of the transmutation of copper to nickel and zinc in the TFC central column (cell 101), which presents a worst case; the transmutation rates in all other coil components were less. The reactions of interest and the half-lives of the radioactive products are given on Fig. 4.2.-5 and summarized as follows:

Nickel-producing reactions:

$^{63}\text{Cu}(n,\gamma)^{64}\text{Cu}$	12.71 hr	→ $^{64}\text{Ni}(60\%)$
$^{63}\text{Cu}(n,p)^{63}\text{Ni}$	100 yr	→ ^{63}Cu
$^{63}\text{Cu}(n,\alpha)^{60}\text{Co}$	5.27 yr	→ ^{60}Ni
$^{63}\text{Cu}(n,2n)^{62}\text{Cu}$	9.74 min	→ ^{62}Ni
$^{65}\text{Cu}(n,2n)^{64}\text{Cu}$	12.71 hr	→ $^{64}\text{Ni}(60\%)$
$^{65}\text{Cu}(n,p)^{65}\text{Ni}$	2.52 hr	→ $^{65}\text{Cu}(\text{via } ^{65}\text{Ni})$
$^{65}\text{Cu}(n,\alpha)^{62}\text{Co}$	13.9 min	→ ^{62}Ni

Zinc-producing reactions:

$^{63}\text{Cu}(n,\gamma)^{64}\text{Cu}$	12.71 hr	→ $^{64}\text{Zn}(40\%)$
$^{65}\text{Cu}(n,2n)^{64}\text{Cu}$	12.71 hr	→ $^{64}\text{Zn}(40\%)$
$^{65}\text{Cu}(n,\gamma)^{66}\text{Cu}$	5.10 min	→ ^{66}Zn

Since the (n,γ) , (n,p) , n,α , and $(n,2n)$ cross sections for ^{63}Cu and ^{65}Cu are not well evaluated, these reactions were tallied for natural copper and then split according to isotopic composition. The results of the activation calculations are given in Table 4.2.-VI, which indicate that the bulk of the transmutation of copper occurs as the result of the (n,α) reaction; an average transmutation rate amounts only to $\sim 0.2\%$ per full power year.

4.2.2.5. Neutron-Wall-Loading Spatial Variations. Although the MCNP code could also provide the spatial distribution of uncollided 14.1-MeV neutrons over the first wall, it proved more convenient to use the NEWLIT code¹⁵ to monitor the consequences of design iterations. The results of this analysis for the source distribution summarized on Table 4.2.-II are given on Fig. 4.2-6, and Table 4.2.-VII summarizes the NEWLIT results. Although a large peak-to-average ratio results from the high elongation and low aspect ratio of the ATR/ST, the neutron wall loadings are considerably reduced in the critical DFC region. The

Fig. 4.2.-6. Distribution of uncollided 14.1-MeV neutrons over the first wall for the 500-MWe(net) ATR/ST baseline case as obtained from the NEWLIT code.¹²

TABLE 4.2.-VI

NICKEL AND ZINC PRODUCTION FROM NEUTRON TRANSMUTATION OF COPPER IN THE
TFC CENTRAL COLUMN (CELL 101, FIG. 4.2.-1) AFTER ONE FULL POWER YEAR

<u>Reaction</u>	<u>Average Ni/Cu</u>
^{64}Ni from $^{63}\text{Cu}(n,\gamma)$	$1.06(10)^{-3}$
^{63}Ni from $^{63}\text{Cu}(n,p)$	$5.53(10)^{-6}$
^{60}Ni from $^{63}\text{Cu}(n,\alpha)$	$1.26(10)^{-7}$
^{62}Ni from $^{63}\text{Cu}(n,2n)$	$3.05(10)^{-5}$
^{65}Ni from $^{65}\text{Cu}(n,2n)$	$8.14(10)^{-6}$
^{65}Ni from $^{65}\text{Cu}(n,p)$	$9.42(10)^{-11}$
^{62}Ni from $^{65}\text{Cu}(n,\alpha)$	$8.89(10)^{-7}$
Total	$1.10(10)^{-3}$

<u>Reaction</u>	<u>Average Zn/Cu</u>
^{65}Zn from $^{63}\text{Cu}(n,\alpha)$	$7.04(10)^{-4}$
^{64}Zn from $^{65}\text{Cu}(n,2n)$	$5.43(10)^{-6}$
^{65}Zn from $^{65}\text{Cu}(n,\alpha)$	$7.83(10)^{-4}$
Total	$1.49(10)^{-3}$

variations shown in Fig. 4.2.-6 are taken into account by the blanket (first-wall) thermalhydraulic calculations.

4.3. Fusion-Power-Core Power Balance

The power balance in the FPC is obtained by coupling the neutronic results (Sec. 4.2.), plasma/circuit simulations, and preliminary designs of key components. This global power-plant energy balance is illustrated in Fig. 4.3.-1 and is summarized in Table 4.3.-I. This power balance is based on the following features:

- ♦ Water-cooled inboard shield
- ♦ Water-cooled divertor chambers
- ♦ PbLi-cooled (poloidal-flow) blanket and outboard shield
- ♦ Ohmic and nuclear heating in the DFC and TFCs treated as waste heat.

TABLE 4.2.-VII

SUMMARY OF NEUTRON-WALL-LOADING RESULTS FROM NEWLIT^{1,2}

Fusion power, P_F (MW)	1672.
Thermal power, P_{TH} (MWt)	2047.
Average neutron wall loading, I_w (MW/m ²)	3.3
First-wall area, A_{FW} (m ²)	409.
- outboard first-wall area	261.
- inboard first-wall area	72.
- divertor chamber area	196.
Peak neutron wall loading ^(a) , $\hat{I}_w$ (MW/m ²)	5.7
Fraction of uncollided neutrons in TFC/inboard-shield central column	0.14

(a) Outboard equatorial plane

The MCNP-generated neutronics results give a breakdown of nuclear heating in various FPC components. Under the conditions listed in Table 4.3.-I and Fig. 4.3.-1, 802.0 MW or 39.1% of the total recoverable thermal power is delivered through the pressurized-water coolant loop. The unrecoverable energy deposited in the coils is 251.2 MW, 67.3% (169.1 MW) of which results from ohmic dissipation. Hence, 22.92% of the gross electric power must be recirculated to supply resistive losses in the TFC(7.93%) and DFC(4.99%) sets, and 2.0% is recirculated directly to the plasma; when combined with an added 7.3% recirculated to coolant pumps and other auxiliary plant needs, a total recirculating power fraction of $\epsilon = 0.322$ ($Q_E = 3.11$) results. It is emphasized that higher values of Q_E and lower values of ϵ result only for increased COE. The FPC thermalhydraulic design and optimization described in the following section give an overall thermal-conversion efficiency of $\eta'_{TH} = 0.367$ for this dual-medial thermal cycle, which when combined with ϵ and a nominal generator efficiency of $\eta_{GEN} = 0.98$ gives an overall plant efficiency of $\eta_p = \eta_{GEN}\eta'_{TH}(1 - \epsilon) = P_E/P_{TH} = 0.244$. Both ϵ and COE would improve if the net electric power is increased from the $P_E = 500\text{-MWe}(\text{net})$ baseline value; the thermalhydraulic aspects of this higher neutron wall loading system is also addressed in the following section.

Fig. 4.3.-1. Power balance for 500-MWe(net) ATR/ST baseline case (refer to Table 4.3.-I for notation).

TABLE 4.3.-I
ATR/ST BASELINE POWER-BALANCE SUMMARY

<u>Parameter</u>	<u>Value</u>
Plasma major radius, $R_T(m)$	2.70
Plasma minor radius, $a(m)$	1.5
Plasma vertical elongation, κ	3.0
Total first-wall area, $A_{FW}(m^2)$	410.0
Inboard area, A_{FW}^{IN}	87.9
Divertor area, A_{DIV}	67.6
Average fusion-neutron first-wall loading, $I_w(MW/m^2)$	3.3
Fusion neutron power, $P_N(MW)$	1337.5
Alpha-particle power, $P_\alpha(MW)$	334.0
Plasma ohmic power, $P_\Omega(MW)$	15.0
First-wall nuclear heating, $P_{FW}^N(MW)$	24.9
Inboard-shield nuclear heating, P_{SLD}^{NI}	301.7
Outboard-shield nuclear heating, P_{SLD}^{NO}	17.1
Divertor nuclear heating, P_{DIV}^N	228.1
Blanket nuclear heating, P_{BLK}	1128.5
Recoverable nuclear heating, $M_N P_N(MW) =$ $P_{FW}^N + P_{DIV}^N + P_{BLK} + P_{SLD}^{NI} + P_{SLD}^{NO}$	1700.3
Fusion-neutron blanket energy multiplication, M_N	1.27
Total recoverable thermal power, $P_{TH}(MW) = M_N P_N + P_\alpha + P_\Omega$	2049.7
Toroidal-field coil heating, $P_{TFC}(MW)$	203.0
♦ inboard nuclear heating, $P_{TFC}^{NI}(MW)$	67.2
♦ outboard nuclear heating, $P_{TFC}^{NO}(MW)$	3.5
♦ inboard ohmic heating, $P_{TFC}^{OI}(MW)$	96.3
♦ outboard ohmic heating, $P_{TFC}^{OO}(MW)$	36.0
Divertor-field coil heating, $P_{DFC}(MW)$	47.8
♦ nuclear heating, $P_{DFC}^N(MW)$	11.0
♦ ohmic heating, $P_{DFC}^\Omega(MW)$	36.8
Equilibrium-field coil heating, $P_{EFC}(MW)$	0.4
♦ nuclear heating, $P_{EFC}^N(MW)$	0.4
♦ ohmic heating, $P_{EFC}^\Omega(MW)$	0.0

TABLE 4.3.-I (Cont.)

Fraction plasma transport loss to divertor, f_D	0.96 ^(a)
Fraction plasma power radiated, f_{RAD}	0.30
Plasma radiation power, $P_{RAD}(MW) = f_{RAD}(P_\alpha + P_Q)$	104.8
Plasma transport power, $P_{TR}(MW) = (1 - f_{RAD})(P_\alpha + P_Q)$	244.6
Power to divertor, $P_{DIV}(MW) = f_D P_{TR} + P_{DIV}^N + P_{RAD} A_{DIV}/A_{FW}$	478.6
Total power to inboard shield, $P_{SLD}^{IN}(MW) = P_{SLD}^{NI} + (1 - f_D) P_{TR} A_{FW}^{IN}/(A_{FW} + A_{SLD}) + P_{RAD} A_{FW}^{IN}/A_{FW}$	323.4
Total power to water-cooled systems, $P_{THW}(MW) = P_D + P_{SLD}^{IN}$	802.0
Total power to first wall, $P_{FW}(MW) = P_{FW}^N + (1 - f_D) P_{TR} (1 - A_{FW}^{IN}/A_{FW} - A_{DIV}/A_{FW}) + P_{RAD} (1 - A_{FW}^{IN}/A_{FW})$	102.1
Total power to PbLi, $P_{THp} + P_{BLK} + P_{FW} + P_{SLD}^{OUT}$	1247.7
Total power to magnet coolant, $P_{TFC}(MW) + P_{PFC}(MW)$	251.2
Estimated thermal-conversion efficiency, η_{TH}	0.367
Gross electric power, $P_{ET}(MW) = \eta_{GEN} \eta_{TH}' P_{TH}$	737.9
Auxiliary power needs, $P_{AUX}(MW) = f_{AUX} P_{ET}$	53.5
Ohmic power to FPC, $P_C^Q(MW) = P_{TFC}^Q + P_{PFC}^Q + P_{DFC}^Q + P_Q$	184.1
Recirculating power, $P_C(MW) = P_{AUX} + P_C^Q$	237.6
Recirculating power fraction, $\epsilon = P_C/P_{ET}$	0.322
Plant efficiency, $\eta_p = \eta_{GEN} \eta_{TH}' (1 - \epsilon)$	0.244
Net electrical power, $P_E(MWe) = P_{ET} (1 - \epsilon)$	500.3

(a) The estimates made in Sec. 2.5.2. give $f_D = 0.996$, but the value of 0.96 was used to enforce some conservatism.

4.4. Thermalhydraulic Integration

The power flows both to and from the FPC, as described in Sec. 4.3., serve as the base for the preliminary thermalhydraulic integration of the FPC. After a brief statement of design criteria (Sec. 4.4.1.), the thermalhydraulic aspects of the PbLi blanket (Sec. 4.4.2.) and water-cooled component (Sec. 4.4.3.) are described.

4.4.1. Design Criteria. The goal of the FPC design is to maximize the overall plant efficiency subject to the design constraints listed for the individual components. Energy is supplied to the ATR/ST energy-conversion cycle

by two distinct fluid streams at different temperatures. A detailed design of the dual-media steam cycle is not within the scope of this study. The approach taken is to utilize the ideal cycle efficiency for such a cycle as given by^{2,8}

$$\eta_I = 1 - T_E \left[\frac{f_w}{(T_{wo} - T_{wi})} \ln \left(\frac{T_{wo}}{T_{wi}} \right) + \frac{(1 - f_w)}{(T_{po} - T_{pi})} \ln \left(\frac{T_{po}}{T_{pi}} \right) \right], \quad (4.4.-1)$$

where T_E is the temperature of the environment to which heat is rejected, f_w is the fraction of total thermal power that is supplied by the pressurized-water coolant, and T designates a bulk fluid temperature; the subscripts are w for water, p for PbLi, i for inlet, and o for outlet. A comparison of Eq. (4.4.-1) with predicted or measured efficiencies of other dual- and single-media thermal cycles indicates that approximately 75% of the ideal efficiency can be achieved in a real system. Although uncertainty exists in the absolute accuracy of this calculated efficiency, Eq. (4.4.-1) should accurately reflect the change in cycle efficiency that would be caused by changing fluid temperatures or the fraction of total energy carried by each fluid.

The gross electrical energy output is $P_{ET} = \eta'_{TH} \eta_{GEN} P_{TH} = \eta_{TH} P_{TH}$, where $\eta'_{TH} = 0.75\eta_I$, and P_{TH} is the recoverable thermal power. The net power output is $P_E = P_{ET} - P_C$, where the recirculating power, P_C , includes magnet and plasma power requirement, the pumping power, and other auxiliary power needs. As indicated in Table 4.3.-I and Fig. 4.3.-1, the magnet and plasma power needs represent a major part of the recirculating power (24.9% of P_{ET}), with other needs estimated to require an additional 7.3% of P_{ET} .

The goal of the FPC thermalhydraulic design integration is to achieve fluid conditions that optimize the thermal efficiency and minimize the MHD pumping power. Simultaneously, the following design constraints are maintained:

- ♦ Primary and secondary stress factor of safety, $\sigma_Y/\sigma_D \geq 2$
- ♦ PbLi blanket pressure, $\leq P_{MAX} = 150$ psi
- ♦ Pressurized-water coolant velocity, $V < V_{MAX} = 10$ m/s
- ♦ Maximum HT-9 ferritic alloy temperature for water-cooled case $\leq 600^\circ\text{C}$
- ♦ Maximum HT-9 ferritic alloy temperature in contact with PbLi $\leq T_{MAX} = 475^\circ\text{C}$

- ♦ Critical-heat-flux limits for pressurized-water coolant, $\Delta T_{SAT} \geq 10 \text{ K}$
- ♦ Thermal-conversion efficiency, $\eta_{TH} = 0.75\eta_{GEN}\eta_I$
- ♦ No structural sputtering with divertors ($f_D = 0.96$ and $f_{RAD} = 0.30$).
- ♦ PbLi blanket MHD pressure modeled in one dimension with no sandwiched insulators assumed.

4.4.2. Blanket Thermalhydraulics (PbLi) Design.

4.4.2.1. Model. The ATR/ST blanket is comprised of a liquid lead-lithium eutectic ($\text{Pb}_{83}\text{Li}_{17}$) coolant flowing through parallel channels with walls constructed from 5-mm-thick HT-9 ferritic steel. The PbLi coolant also functions as a breeder. The blanket geometry and model are shown in Fig. 4.4.-1. The PbLi coolant flows into the top of the blanket and is heated by the absorption of neutrons and gamma-rays as it flows vertically downward and exits. The PbLi also absorbs the energy flux and neutron heating from the first

Fig. 4.4.-1. Schematic diagram of the outboard blanket/shield configuration used in the ATR/ST thermalhydraulic analysis.

wall and the neutron heating from the outboard shield. This blanket/shield configuration covers only the outboard side of the torus and is envisaged as being fabricated and installed as a single unit.

The blanket is designed using a thermalhydraulic analysis similar to that used in Refs. 2 and 8. The purpose of the thermalhydraulic analysis is to determine the number and dimensions of channels and the PbLi inlet temperature and flow rate that give the maximum overall efficiency while satisfying structural and thermal constraints summarized in Sec. 4.4.1. A structural analysis of the blanket per se was not performed.

Heat Sources and Distributions. The distribution of neutron and gamma-ray volumetric heating within the first wall was computed by the MCNP code¹³ (Sec. 4.2.). The thermal flux distribution on the first wall was estimated from calculations using the NEWLIT code results¹² (Sec. 4.2.2.5.). These neutronic results were approximated by algebraic equations to facilitate the thermalhydraulic analysis. The heat flux at the first wall, q_w (MW/m²), is given by

$$\begin{aligned} q_w &= 0.485 - 0.0209z, \quad 0 \leq z < 0.75 \\ &0.551 - 0.1087z, \quad 0.75 \leq z < 2.75 \\ &0.584 - 0.0946z, \quad 2.75 \leq z, \end{aligned} \quad (4.4.-2)$$

where z (m) is the vertical distance from the equatorial plane. The neutron and gamma-ray power generation rate within the first wall, $\dot{Q}_{n\gamma}$ (MW/m³), was approximated by

$$\dot{Q}_{n\gamma}^{FW} = 54.35q_w(1.02708 + 0.0007844z + 0.01722z^2). \quad (4.4.-3)$$

The nuclear power distribution in the blanket was approximated by

$$\begin{aligned} \dot{Q}_{n\gamma}^B &= q_w(101.43 + 3.0539z + 1.6188z^2) [0.55 \exp(-29.65y) + \\ &0.45 \exp(-4.00y)] , \end{aligned} \quad (4.4.-4)$$

where $y(m)$ is the radial distance from the first wall.

Hydraulic Analysis. The pressure drop for the flow of coolant through a channel is the sum of the hydrodynamic (viscous) and magnetic components,

$$\Delta P = \Delta P_H + \Delta P_M . \quad (4.4.-5)$$

The gravitational pressure head, $\Delta P_G \approx \rho gh$, does not contribute to pump work for a closed loop and, therefore, is not included in this analysis; the hydrostatic head, however, must be included in the calculation of maximum blanket pressure and related structural design. The hydrodynamic pressure drop is computed from the following expression:

$$dP_H/dx = f_F \rho V^2 / 8a \quad (4.4.-6)$$

where x is the distance along the flow, ρ is the coolant density, V is the bulk velocity, a is the channel half thickness, and the friction factor, f_F , is given by $64/Re$ for laminar flow and $0.184/Re^{0.2}$ for turbulent flow. The flow regime depends on the Reynolds number, $Re = \rho Va/\eta$ and the Hartmann number, $H = aB/(\sigma/\eta)^{1/2}$, where B is the strength of the transverse magnetic field and η is the viscosity. The magnetic field tends to suppress turbulence in liquid-metal flow, with the transition Reynolds number given by⁸⁰ $Re_T = 500H$. The magnetic pressure drop is computed from the following expression

$$dP_M/dx = -r_l V/a^2 \{ (H^2 \tanh(H))/(H - \tanh(H)) + H^2 C/(1 + C) \}, \quad (4.4.-7)$$

where $C = (\sigma_w t_w)/(\sigma a)$ is the ratio of conductivity-thickness products of the channel walls and the flowing fluid.

The radial, poloidal, and toroidal components of the magnetic field are calculated by the NEQ code (Sec. 2.1., Fig. 2.5.-7), and the results are used in the numerical integration of Eq. (4.4.-7). Equation (4.4.-6) is also integrated numerically. The pumping power is then computed as follows:

$$P_{\text{PUMP}} = \dot{m}_p \Delta P / (\rho_p \eta_{\text{pump}}), \quad (4.4.-8)$$

where $\dot{m}_p$ is the mass flow rate and η_{pump} is the pump efficiency. The pressure drop calculated from Eq. (4.4.-7) does not include local losses that may occur in regions where the flow direction, velocity, channel dimensions, or magnetic fields are rapidly changing.^{81,82} The actual pressure drop and pumping power, therefore, may be larger than those computed here.

Thermal Analysis. The temperature distribution within the PbLi in the blanket channels is determined by the numerical solution of the energy equation

$$\rho c_p v \frac{\partial T}{\partial x} = k_{\text{eff}} \frac{\partial^2 T}{\partial y^2} + \dot{Q}_{n\gamma}, \quad (4.4.-9)$$

where x is the downstream distance, k_{eff} is the effective thermal conductivity, c_p is the specific heat of the coolant, v is the local fluid velocity, and y is the transverse distance. Neglecting fluid property variations, the velocity distribution can be approximated⁸³ by

$$v = V_{\text{MAX}}(y/a)^{1/n}, \quad (4.4.-10)$$

where n is taken to be 2 for laminar flow and is approximately 7 for turbulent flow. The effective thermal conductivity for turbulent flow includes the molecular thermal conductivity of the PbLi and an eddy conductivity resulting from turbulent mixing. The effective conductivity is related to the effective viscosity by $k_{\text{eff}} = \eta_{\text{eff}} c_p / \text{Pr}_{\text{eff}}$, where the effective Prandtl number, Pr_{eff} , is approximately 0.9 for confined flows.

The effective viscosity, η_{eff} , is based on the Prandtl mixing length theory, with the VanDriest hypothesis⁸³ being applied at the wall. In this case, η_{eff} is given by

$$\eta_{\text{eff}} = \eta + \rho (K y)^2 \left(1 - \exp[-y(\tau \rho)^{1/2} / (\eta A^+)] \right)^2 \left| \frac{dv}{dy} \right|, \quad (4.4.-11)$$

where $\kappa = 0.435$ and $A^+ = 26$ are empirical constants. The shear stress is approximated by $\tau = \eta(dv/dy)$ and is evaluated at the wall. The thermal boundary condition at the inboard surface of the first channel is given by

$$q_s = q_w + \dot{Q}_{ny} t_w', \quad (4.4.-12)$$

where t_w' is the thickness of the first wall. This expression implies that the heat flux to and energy generation within the first wall are transferred to the PbLi coolant. The channel walls internal to the blanket are assumed adiabatic.

An implicit, numerical marching procedure is used to solve Eqs. (4.4.-9) - (4.4.-12) in finite-difference form using neutronics results expressed by Eqs. (4.4.-2) - (4.4.-4) and a spline fit to the magnetic-field data displayed in Fig. 2.5.-7. The bulk PbLi temperature at the exit is computed as an integrated average temperature at the exit channel cross-section.

The maximum temperature, T_M , at the structure/PbLi interface is a major design constraint, occurs at the inside wall, and is always higher than the bulk temperature at any given blanket cross section. This behavior occurs because $\dot{Q}_{ny}$ is highest at the first wall and because the direction of heat flow is from the first wall into the fluid. An important constraint on the design is that T_M be less than some specified value so that corrosion effects at the PbLi/HT-9 interface will be within acceptable limits^{8,4} (Sec. 4.4.1.); this limit generally is taken as $T_M = 475^\circ\text{C}$.

The model described above is used to examine the blanket coolant channel and peak PbLi/structural interface temperature. The thermal analysis of the first wall and outboard steel shield (Fig. 4.4.-1), both of which are also cooled by the blanket PbLi stream, requires a separate auxiliary calculation. The temperature distributions within both the first wall and the outboard shield can be computed by solving the one-dimensional, steady-state conduction equation using constant properties, the respective nuclear power densities, and appropriate boundary conditions. For the outboard shield, the boundary conditions hold the inside surface temperature at $T_M = 475^\circ\text{C}$ and require the heat flux to be zero at the outside surface. The solution of the heat conduction equation for the case of a shield slab of thickness l is

$$T = T_M + \dot{Q}_{n\gamma} l^2 / 2k, \quad (4.4.-13)$$

where k is the thermal conductivity of the shield material. If the 0.2-m-thick shield is divided into two equally thick slabs and an additional channel is added between the two slabs, the maximum temperature for $\dot{Q}_{n\gamma} = 0.26 \text{ MW/m}^3$ is 534°C for $T_M = 475^\circ\text{C}$. This maximum temperature is below the irradiation embrittlement limit of 550°C for 316 stainless steel. A single channel of PbLi should be adequate for cooling the outboard shield.

The maximum temperature, T_O , of a first wall with thickness t_w occurs on the side facing the plasma. This temperature is computed as a function of the first-wall/PbLi surface temperature, T_M , and the heat flux and power generation rate. The following relationship between T_M and T_O is derived from the one-dimensional conduction equation

$$T_O - T_M = \dot{Q}_{n\gamma} t_w^2 / 2k + q_w t_w / k, \quad (4.4.-14)$$

where in this case k is the thermal conductivity of the HT-9 wall material.

Thermalhydraulics Systems Model. A systems model was developed to optimize the ATR/ST blanket thermalhydraulics design. The design variables are the number of channels, the dimensions of the channels, the inlet PbLi temperature, and the PbLi mass flow rate. The parameter to be maximized is the overall efficiency of the power plant. At the level of the thermalhydraulics optimization, the plant efficiency is taken as 75% of η_I [Eq. (4.4.-1)] reduced by $\eta_{\text{GEN}} = 0.98$ and $1 - \epsilon_{\text{pump}}$, where ϵ_{pump} is the fraction of $P_{\text{ET}} = \eta_{\text{TH}} P_{\text{TH}}$ required to pump the PbLi coolant. The design constraints are $T_M \leq T_{\text{MAX}}$ and $P \leq P_{\text{MAX}}$, where T_{MAX} is the maximum allowable PbLi/HT-9 interface temperature and P_{MAX} is the allowable pressure in the blanket, as listed in Sec. 4.4.1.

Quantities that must be input to the thermalhydraulics systems model include the half thickness of the poloidal and radial sections of the first (inside) channel, a_{p1} and a_{r1} , the maximum allowable PbLi temperature, and the increase in coolant bulk temperature from inlet to exit, $\Delta T_p = T_{po} - T_{pi}$. The following operations are then performed in the course of search for blanket parameters that meet the main design goals listed in Sec. 4.4.1:

- (i) The inlet and exit coolant bulk temperatures, T_{pi} and T_{po} , are computed. This calculation requires an iterative method to determine the mass flow rate that gives the specified bulk temperature difference along with the solution of the thermal equations and subsequent adjustment of fluid temperatures to satisfy the condition, $T_M \leq T_{MAX}$.
- (ii) The dynamic pressure drop for the first coolant channel, ΔP , is calculated by numerical integration of Eqs. (4.4.-6) and (4.4.-7).
- (iii) The widths of the poloidal sections of the remaining channels are determined. This computation requires an iterative procedure in which the thermal equations are solved for each successive channel to determine the maximum allowable thickness such that all coolant/structure interface temperatures are below T_{MAX} and the coolant temperature rise is identical to that of the first channel. The maximum first-wall temperature, T_o , is also computed [Eq. (4.4.-13)].
- (iv) The widths of the radial sections of the remaining channels are determined by adjusting the channel thickness at inlet and exit to produce an overall pressure drop equal to that of the first channel. The minimum allowable channel thickness is chosen such that the exit coolant velocity is not larger than that of the first channel. In some cases this condition may necessitate the use of additional flow restrictions to produce the required pressure drop.
- (v) A net thermal efficiency is estimated. The gross thermal efficiency, $\eta_{TH} = 0.75\eta_{I,GEN}$ [Eq. (4.4.-1)] is defined as the ratio of electrical (turbine) power to heat input and is approximated as a function of temperatures and flow rates of both the PbLi blanket coolant and the pressurized water used to cool the inboard shield and the divertor.

The net efficiency is defined as follows for the purpose of the thermalhydraulics optimization.

$$\eta_p^* = \eta_{TH}(1 - \epsilon_{pump}) , \quad (4.4.-15)$$

where ϵ_{pump} is the ratio of pump power (efficiency $\eta_{pump} = 0.75$) to gross electrical power, P_{ET} . The overall plant efficiency, $\eta_p = \eta_{TH}(1 - \epsilon)$, given in Table 4.3.-I, includes all recirculating power, whereas η_p^* accounts only for pump power in order to emphasis thermalhydraul sensitivities. The results of the blanket optimization calculations include the dimensions of all channels, PbLi temperatures, system efficiency, pumping power, and pressure drop in the blanket. This systems model has been evaluated over a range of parameters to illustrate sensitivities. An interim thermalhydraulics design point is also suggested.

4.4.2.2. Results. The most important constraints affecting the blanket design are the maximum allowable temperature at the interface of the coolant and channel wall, T_{MAX} , the maximum pressure in the blanket, P_{MAX} , the PbLi velocity, V , and the number of channels N_c . The corrosion limit for PbLi and HT-9 or 316 stainless steel is taken as approximately 475°C .

The hydrostatic or gravity pressure head is $\Delta P_G = 0.9 \text{ MPa}$ (134 psi). If the coolant flows downward through the blanket, the dynamic pressure drop, $\Delta P = \Delta P_H + \Delta P_M$ [Eq. (4.4.-5)] causes a decrease in maximum blanket pressure, and the startup value of $P_{MAX} = \Delta P_G$ may be used in the structural design. Upward flow gives a maximum blanket pressure of $P_{MAX} = \Delta P_G + \Delta P$. Although the blanket design has not been of sufficient detail to warrant a structural analysis, it appears that a pressure of $\sim 1 \text{ MPa}$ (150 psi) is feasible if the outside walls of the blanket are stiffened by ribs spaced at appropriate intervals. The PbLi velocity should be sufficiently low to minimize erosion of the channel surfaces; an upper limit of $V_{MAX} = 2 \text{ m/s}$ was selected. The number of channels is directly related to design complexity and cost; a value of $N_c = 3$ was judged as a satisfactory compromise, although considerably more structural design work is required.

A series of calculations were made for $T_{MAX} = 475^\circ\text{C}$ and for a range of first-channel (poloidal) half-thicknesses, a_{p1} , and coolant temperature differences $\Delta T_p = T_{po} - T_{pi}$. Results are shown in Figs. 4.4.-2 and 4.4.-3. The efficiency η_p^* is seen to increase in Fig. 4.4.-2 with decreasing values of ΔT_p , but the fluid velocity is above acceptable limits for ΔT_p less than $\sim 60 \text{ K}$, as is shown in Fig. 4.4.-3. For $\Delta T_p = 60 \text{ K}$, r_p^* maximizes for $a_{p1} \approx 0.4 \text{ m}$, but four channels would be required for these conditions, and the coolant velocity would be greater than $V_{MAX} = 2 \text{ m/s}$. The pumping power fraction, ϵ_{pump} , increases rapidly with decreasing values of ΔT_p and a_{p1} , as is shown in Fig. 4.4.-4; high values of ϵ_{pump} result in decreased overall efficiency and larger, more expensive pumps. A value of $a_{p1} = 0.08 \text{ m}$ and $\Delta T_p = 60 \text{ K}$, therefore, was selected as typical. Details for this design are given in Table 4.4.-I.

Significant increases in efficiency are possible if T_{MAX} can be increased, as is shown in Fig. 4.4.-5. For example, use of vanadium alloy as a structural material would permit temperatures as high as 700°C . The increased efficiency would have to be balanced against higher cost and the difficulty of fabricating this refractory material.

TABLE 4.4.-I
TYPICAL BLANKET THERMAL-MECHANICAL PARAMETERS FOR THE DESIGN
INDICATED ON FIGS. 4.4.-2/4

<u>Parameter</u>	<u>Value</u>
Number of segments, N_c	12
Material	HT-9/PbLi
Geometry	
♦ Channel thicknesses with increasing radius, $a_{p1}(m)$	0.160/0.532/0.388
♦ Channel thicknesses entering manifold, a_{Ri}	0.320/0.196/0.021
♦ Channel wall thickness (mm)	5.0
Blanket thermal rating	
♦ Total cooling rate ^(a) (MWt)	1247.7
♦ Peak/average power density (MW/m ³)	49.2/3.6
♦ Maximum PbLi/channel wall interface temperature, $T_{MAX}(^{\circ}C)$	475.
♦ Maximum first wall material temperature, $T_o(^{\circ}C)$	543.
Coolant properties	
♦ Inlet/outlet temperature, $T_{pi}/T_{po}(^{\circ}C)$	341.3/401.3
♦ Coolant velocity in channels, $V_i(m/s)$	1.685/0.311/0.045
♦ Coolant velocity in radial header exits (m/s)	1.685
♦ Total mass flow rate, $\dot{m}_p(kg/s)$	124,000.
♦ Hydrodynamic pressure drop in blanket, $\Delta P(MPa/psi)$	0.3/42.1
♦ Pumping power ^(b) , $P_{pump}(MW)$	5.10
♦ Pump power fraction, ϵ_{pump}	0.007
♦ Effective plant thermal efficiency, $\eta_p^* \equiv \eta_{TH}(1 - \epsilon_{pump})^{(c)}$	0.360
♦ Maximum blanket pressure (hydrostatic), $\Delta P_G(MPa/psi)$	0.9/134.0
♦ Reynolds/Hartmann/Nusselt numbers, $Re/H/Nu$	4.35(10) ⁵ /29.36/120.7

(a)Includes energy from first wall and outboard shield

(b)Based on $\eta_{pump} = 0.75$

(c) $\eta_{TH} = 0.75\eta_{GEN}\eta_I$, where $\eta_{GEN} = 0.98$ and η_I is from Eq. (4.4.-1)

The PbLi temperature is also constrained by the operating pressure of the steam power cycle used to produce electrical energy. Estimates of cycle efficiency are based on a 13.8-MPa (2,000 psi) steam pressure, and the inlet PbLi temperature, therefore, must be greater than the saturation temperature (335^oC) of steam at that pressure. This limitation is illustrated in Fig. 4.4.-6. The 13.8 MPa "pinch-point" line assumes an inlet PbLi temperature

Fig. 4.4.-4. Dependence of recirculating power fraction required for PbLi pumps on first-channel half width and overall coolant temperature change, $\Delta T_p = T_{po} - T_{pi}$, for the maximum PbLi/structural interface temperature of $T_{\text{MAX}} = 475^\circ\text{C}$.

Fig. 4.4.-5. Dependence of effective plant efficiency, $\eta_p^* = \eta_{\text{TH}} (1 - \epsilon_{\text{pump}})$, on first-channel half-width thickness, a_{p1} , maximum PbLi/structural interface temperature, and overall coolant temperature change, $\Delta T_p = T_{po} - T_{pi}$.

Fig. 4.4.-6. "Pinch point" plot showing constraints imposed by steam-generator pressure, effective plant efficiency, PbLi coolant velocity, and possible corrosion limits.

that is 10 K above the saturation temperature of steam. Selecting $T_{MAX} = 475^{\circ}\text{C}$ and $V = 2 \text{ m/s}$ defines the design window shown. The design point is also shown on Fig. 4.4.-6.

The basecase generated in Sec. 3. is for a thermal power of $P_{TH} = 2049.4 \text{ MW}$, $\eta_{TH} = 0.36$, and a plant net electric power of $P_E = 500 \text{ MWe(net)}$. A series of parametric studies was done to determine the effects of varying the net electric power, P_E , while maintaining T_{MAX} , ΔT_p , and blanket geometry constant. In effect, the ATR/ST baseline geometry was operated with a higher neutron wall loading. Table 4.4.-II gives the key parameter for these higher- P_E , but off-optimum-COE, baseline cases. Using the identical power splits ($f_w = 0.391$) and blanket geometry, these higher-power baseline cases (originally determined for $\eta_{TH} = 0.36$) were subject to a thermalhydraulic reoptimization. Results are shown for a range of P_E values in Figs. 4.4.-7. The most significant effect of increasing P_E is the large increase in blanket pressure drop caused by higher PbLi flow rates. The thermal efficiency, η_{TH} , is nearly constant, but the effective plant efficiency, η_p^* , decreases more rapidly because of increasing pump power fraction.

TABLE 4.4.-II

RESULTS FROM ATR/ST SYSTEMS CODE SHOWING IMPACT OF GENERATING MORE POWER FROM AN FPC THAT RETAINS THE $P_E = 500\text{-MWe}(\text{net})$ BASELINE GEOMETRY^(a)(Table 1.5.-I)

P_F	P_E	P_{TH}	I_w	Q_E	COE ^(b)	P_E/M_{FPC}	$B_{\phi V(0)}$
(MW)	(MWe)	(MWt)	(MW/m ²)		(mills/kWeh)	(kWe/tonne)	
3,570.7	1,200	4,362.4	6.9	4.23	33.7	183.6	2.8
3,035.4	1,000	3,710.1	5.9	4.0	37.8	154.0	2.7
2,359.5	750	2,886.0	4.6	3.6	45.7	116.7	2.5
1,671.9	500 ^(a)	2,047.4	3.2	3.1	61.4	78.8	2.3
1,391.7	400	1,705.5	2.7	2.9	73.0	63.5	2.2

(a)Baseline: $a = 1.5$, $A = 1.8$, $\kappa = 3.0$, $\eta_{TH} = 0.36$

(b)Constant 1980 dollars

The results shown on Fig. 4.4.-7 are probably optimistic in that key constraints related to P_{MAX} , V_{MAX} , and T_{pi} (i.e., fusion point of PbLi) were not enforced. Furthermore, although the basic $P_E = 500\text{-MWe}(\text{net})$ baseline configuration was maintained intact as P_E (and P_{TH}) was increased, reoptimization of the blanket coolant channels and coolant temperature drop is possible, while maintaining constraints like $T_{MAX} \leq T_M = 475^\circ\text{C}$. The thermalhydraulics model was used to examine these tradeoffs, and a range of design windows for $P_E = 500, 750, 1000$, and $1,200\text{ MWe}(\text{net})$ is shown on Figs. 4.4.-8. The conditions for simultaneous solution to $T_{pi} > 250^\circ\text{C}$ (PbLi solidification), $V < V_{MAX} = 2\text{ m/s}$, and $\Delta P < \Delta P_{MAX} = 0.35\text{ MPa}$ (50 psi) are shown. The point of maximum effective plant efficiency, η_p^* , is also shown, with the $P_E = 500\text{-MWe}$ as in Fig. 4.4.-8 also illustrating the variation of η_p^* . It should be noted that for $P_E = 1,200\text{ MWe}(\text{net})$, the design window as defined above has almost disappeared. It should also be noted that the assumption of one-dimensional MHD flow between two semi-infinite parallel plates also comes into question for $a_{p1} \geq 0.2\text{ m}$. The dependence of both η_{TH} and $\eta_p^* = \eta_{TH}(1 - \epsilon_{\text{pump}})$ on P_E is shown in Fig. 4.4.-8. In principle, this information should be fed back to the parametric systems code (Sec. 3.), and the design reoptimized, but the consistency of these models is considered acceptable. Nevertheless, the baseline FPC is capable of increases in neutron wall loading by a factor of

Fig. 4.4.-7. Dependence of PbLi coolant temperature difference, ΔT_p , on half thickness of first poloidal channel, a_{p1} , for $T_{MAX} = 475^\circ\text{C}$ and $P_E = 500-1,200 \text{ MWe}$, showing $T_{pi} > 250^\circ\text{C}$, $\Delta P < 50 \text{ psi}$, and $V \leq 2 \text{ m/s}$ constraints, and associated design window. The point of nearly maximum effective plant efficiency, η_p^* , is indicated.

Fig. 4.4.-8. Dependence of η_{TH} , $\eta_P^* = \eta_{TH} (1 - \epsilon_{pump})$ on net electric power, P_E , for fixed values of T_{MAX} , a_{p1} , and ΔT_p . The increase in P_E over the 500-MWe(net) design point reflects an increase in neutron wall loading above the nominal design value of $I_W = 3.3 \text{ MW/m}^2$.

~ 2.1 while holding fairly stringent constraints on P_{MAX} , T_{MAX} , and V_{MAX} , and maintaining the simplicity of an integrally cooled first-wall/blanket system.

4.4.3. Pressurized-Water Cooling Circuits. Figure 4.4.-9 illustrates the essential features of the water cooling circuits envisaged for the 500-MWe(net) ATR/ST design. Water under PWR conditions (~ 300°C, ~ 2000 psi) is used to recover efficiently fusion power from the 0.20-m-thick shields that surround the TFC center post and the DFC, as well as surface heat deposited in the divertor channel. Low-pressure, high-velocity water is also used to remove nuclear and ohmic heating from the TFCs and DFCs. The design resolution of shields, divertor chambers, and coils is not sufficient to warrant detailed thermal designs of these systems, and only estimates of the thermalhydraulic requirements are made. As seen from the power balance given in Fig. 4.4.-1, the thermal power delivered to the PbLi, pressurized-water, and chilled-water (coil) cooling circuits is 1,247.7(54.2%), 802.0(34.9%), and 250.8(10.9%) MWt,

respectively, with 34.9% of the recoverable thermal power ($P_{TH} = 2049.7$ MWt) being delivered to the pressurized-water coolant.

Equation (4.4.-1) shows that the cycle efficiency increases with increasing fluid temperature. Both the inlet and outlet water temperatures, therefore, should be as high as possible. In addition, the following criteria from those listed in Sec. 4.4.1. are invoked to define satisfactory thermalhydraulic design for the inboard shield and the divertor chamber:

- ♦ The bulk fluid velocity must not be so large as to cause excessive erosion. A value of $V_{MAX} = 10$ m/s is selected as a nominal upper limit.
- ♦ The maximum temperature at the inside surface of the coolant tubes must be less than the saturation temperature of water by a specified amount. A difference of $\Delta T_{SAT} = 10$ K is selected for design purposes.
- ♦ The maximum material temperature must be less than a specified value. One-half the melting temperature for HT-9 ($T_{MAX} = 580$ C) is selected as the upper limit.

In performing the structural analysis and thermal-hydraulic analysis for the pressurized-water systems used for the inboard shield and divertor chambers, the approach used is identical to that given in Ref. 8. In particular, a steady-state one-dimensional stress calculation for thin-walled tubes was used. Dee-shaped tubes were used for cooling the divertor-chamber and inboard-shield wall surfaces. The inside diameter of the D-tubes, d_i , where d_i is constrained by stress calculations, the inlet water temperature, T_{wi} , and the inlet water pressure P_{wi} , are treated as design variables. The stress calculations are used to determine the appropriate tube-wall thickness, δ . The stress is constrained to be less than a design value, σ_D , which is taken as the yield strength reduced by a factor of safety of two, and all calculations are based on HT-9 as a shield and wall materials. While HT-9 does not have the good thermal-stress characteristics of the copper alloy used in the Ref. 8 design, lower activation and a higher melting temperature are advantages. Furthermore, the average heat fluxes onto the divertor-chamber walls amount only to 1.35 MW/m² and should present no serious problem for the ferritic alloy. Following the stress calculations, the number of surface tubes, N_T , the mass flow, $\dot{m}_w$, the average fluid velocity, V , the inside wall temperature, T_{si} , (depends on the inner surface heat flux, q_w , and the local film coefficient, h), the outside surface temperature of the tubes, T_{so} (constrained by maximum material temperature), and the hydraulic pressure drop, ΔP_H , are calculated.

4.4.3.1. TFC Central Column. The inboard shield is cooled by separate single-pass pressurized-water circuits with surface D-tubes as well as interior coolant channels. A bottom-to-top coolant-flow direction was chosen with the inboard-shield coolant manifolds being located adjacent to the inboard-shield coolant inlets and outlets. A detailed view of this arrangement is shown in Fig. 4.4.-9. These manifolds are connected to the divertor shield manifolds to reduce the number of inlet/outlet water headers required. The thermal ratings, coolant configurations, and coolant properties for the inboard shield are given in Table 4.4-III. An inner blanket of PbLi was considered early in the study. Although this blanket increases the thermal-conversion efficiency and tritium breeding ratio, the large MHD pressure drop in this high-field region (Fig. 2.5.-7), the maintenance problems associated with an inboard blanket, and

Fig. 4.4.-9. Pressurized-water coolant headers and manifolds for the 500-MWe(net) ATR/ST baseline case. The PbLi blanket coolant flow pattern is also shown.

TABLE 4.4.-III
INBOARD SHIELD THERMAL-MECHANICAL DESIGN PARAMETERS

Thermal Rating

Average surface heat flux, q_w (MW/m ²)	0.25
Volumetric heat source, $\dot{Q}_{nV}$ (MW/m ³)	13.7
Power absorbed by inboard coolant (MW)	323.4
Percent of total thermal output	15.8

Coolant Configuration

Number of surface channels, N_T	310
Number of interior channels, N_C	1,100
Channel length, l (m)	12.0
D-tube inside diameter, d_i (mm)	12.0
Wall thickness to outside surface, δ (mm)	3.0
Maximum temperature in shield wall, K	670

Coolant Properties

Flow rate in channel, $\dot{m}_w/N_T$ (kg/s)	0.36
Total flow rate, $\dot{m}_w$ (kg/s)	635
Pressure drop, ΔP_H (MPa/psi)	0.72/104
Nominal coolant velocity, V (m/s)	9.8
Water inlet temperature, T_{wi} (K)	443
Water exit temperature, T_{wo} (K)	568

Manifold/Header Properties

Inlet manifold, ID/OD (mm)	280/325
Outlet manifold, ID/OD (mm)	300/360
Inlet header (radial run), ID/OD (mm)	250/300
Outlet header (radial run), ID/OD (mm)	270/320
Total inboard circuit pressure drop (MPa)	0.73
Inboard shield pumping power (MW)	0.75

the already adequate tritium breeding ratio from the outboard blanket alone made an inboard blanket unnecessary and/or unattractive.

4.4.3.2. DFC Shield and Divertor Chamber. The DFC shield and divertor chamber, because of its high heat load, requires three separate (parallel) coolant passes in order to meet design constraints. Furthermore, each parallel loop first cools the surface of the divertor chamber and then returns through the DFC shield, removing the less demanding bulk nuclear heating. The details

of the divertor coolant geometry are also indicated in Fig. 4.4.-9, as well as in Sec. 4.5.2. One coolant pass is shown on the outer side of the inboard divertor chamber, and two separate passes are shown cooling the outboard divertor chamber. Most of the coolant temperature rise occurs at the surface leg of each pass in order to keep the velocity below 10 m/s. The manifold arrangements associated with the divertor shield are shown in Fig. 4.4.-9. The thermal ratings, coolant configurations, and coolant properties are given for the divertor shield in Table 4.4.-IV.

4.4.3.3. TFC and DFC Coolant. Both the TFC central column and the DFCs are shielded from the plasma by a minimum of 0.2-m-thick water-cooled steel. The thermal ratings, coolant configurations, and coolant properties for the TFC central column and TFC outboard (horizontal and vertical legs) are given in Table 4.4.-V. A 56°C temperature drop with an exit temperature of 70°C and a 2.5 m/s water velocity are selected as reasonable design parameters and result in manifolds of acceptable size. The TFC coolant channels pass through the conductor centers (100 turns) and for the TFC central column these channels are 5 mm in diameter. The outboard TFC conductors would require 3-mm-diameter coolant channels.

4.5. Mechanical/Electrical Integration

The main goal of this scoping study is to identify an economically optimum ST reactor that is based upon the firmest physics possible. An important part of this goal is the assurance that key engineering constraints are met at a level where this system can be integrated at thermal (Sec. 4.3.), thermalhydraulic (Sec. 4.4.), mechanical, and electrical levels. The present section presents a preliminary mechanical and electrical integration of the FPC, with an emphasis placed on the latter. In this context, a focus has been given to FPC maintenance (Sec. 4.5.2.). Little or no design resolution of key FPC systems is available at this point, but Sec. 4.5.3. presents brief descriptions of magnet, vacuum, RF, fueling, tritium, and current-drive systems based largely on experience reflected in other, more-detailed studies and the scoping results presented herein.

4.5.1. FPC Mechanical/Electrical Integration. The essential elements of the FPC mechanical layout have been expanded from the systems code results given in Fig. 4.1.-1 into a preliminary engineering layout using the results of the thermalhydraulic integration (Sec. 4.4.). Figure 4.5.-1 is compatible with the

TABLE 4.4.-IV
DIVERTOR SHIELD THERMAL-MECHANICAL DESIGN PARAMETERS

<u>Divertor Chamber/Chamber Surface^(a)</u>	<u>Inboard/ Outer</u>	<u>Outboard/ Inner</u>	<u>Outboard/ Outer</u>
<u>Thermal rating</u>			
Average surface heat flux, q_w (MW/m ²)	1.35	1.34	1.34
Volumetric heat generation, Q_{nY} (MW/m ³)	6.7	.0	2.4
Power absorbed by coolant ^(b) (MW)	72.9	78.9	84.5
Fraction of total thermal output (%)	3.6	3.8	4.1
<u>Coolant configuration</u>			
Number of channels, N_c	2,040	3,090	3,510
Channel length, l (m)	3.9	3.7	3.8
D-tube ID, d_i (mm)	4.0	4.0	4.0
Wall thickness to outside surface, δ (mm)	1.0	1.0	1.0
Maximum temperature in shield wall (K)	590	551	571
<u>Coolant properties</u>			
Flow rate in channel, $\dot{m}_w/N_c$ (kg/s)	.034	.030	.037
Total flow rate, $\dot{m}_w$ (kg/s)	70.	93.	129.
Pressure drop, ΔP_H (MPa/psi)	0.80/116	0.80/116	0.80/116
Nominal coolant velocity, V (m/s) ^(c)	9.5	9.7	9.6
Water inlet temperature, T_{wi} (K)	443	443	443
Water surface exit temperature, T_{so} (K)	537	505	525
Water exit temperature, T_{wo} (K)	568	568	568
<u>Manifold/Header Properties</u>			
Inlet manifold, ID/OD (mm) ^(c)	140/164	220/235	
Outlet manifold, ID/OD (mm) ^(c)	154/180	310/360	
Inlet header (radial run), ID/OD (mm) ^(c)	250/300	250/300	
Outlet header (radial run), ID/OD (mm) ^(c)	270/320	270/320	
Total pressure drop (MPa)	0.81		
Divertor shield pumping power (MW)	0.41		
Total shield pumping power (MW)	0.82		

(a)The inboard surface of the inboard divertor chamber is considered part of the inboard shield for purposes of the thermal-hydraulic calculations (Table 4.4.-III).

(b)Two divertors, power is given for only one.

(c)Inboard/outboard values.

TABLE 4.4.-V
TOROIDAL FIELD COIL THERMAL-MECHANICAL DESIGN PARAMETERS

	<u>Central Column</u>	<u>Outboard</u>
<u>Thermal rating</u>		
Volumetric heat generation, $\dot{Q}_{n\gamma}$ (MW/m ³)	0.91	0.012
Power absorbed by coolant (MW)	163.5	39.5
Percent of total thermal output	8.0	1.9
<u>Coolant configuration</u>		
Number of channels, N_c	100.	100.
Channel length, l (m)	14.0	22.
Channel diameter, d_i (mm)	5.0	1.0
Maximum temperature in TFC (K)	344.	340.
<u>Coolant properties</u>		
Flow rate in channel, $\dot{m}_w/N_c$ (kg/s)	1.56	0.20
Total flow rate, $\dot{m}_w$ (kg/s)	155.5	20.2
Pressure drop, ΔP_H (MPa/psi)	0.15/21.6	0.21/31.0
Nominal coolant velocity, V (m/s)	2.5	2.5
Water inlet temperature, T_{wi} (K)	283.	283.
Water exit temperature, T_{wo} (K)	339.	339.
<u>Manifold/Header properties</u>		
Inlet manifold, ID/OD (mm)	30/38	20/25
Outlet manifold, ID/OD (mm)	30/38	20/25
Inlet header (radial run), ID/OD (mm)	20/25	15/19
Outlet header (radial run), ID/OD (mm)	20/25	15/19
Total circuit pressure drop (MPa)	0.16	0.22
Total TFC pumping power (MW)		0.002

physics parameters summarized in Table 1.5.-I and the thermalhydraulics summarized in Tables 4.4.-II through 4.4.-V. The coolant manifolds and headers, magnetic coils, and the FPC support structure shown in Fig. 4.5.-1 provide for the preliminary evaluation of the FPC maintenance scheme given in the following Sec. 4.5.2. An isometric view derived from the plan and elevation drawings (Fig. 4.5.-1) is given in Fig. 4.5.-2, with Fig. 4.4.-9 giving a detailed view of the pressurized-water and PbLi coolant manifolds and headers located at the top (and bottom) of the FPC.

Fig. 4.5.-1A. Elevation view of the 500-MWe(net) ATR/ST baseline case showing the key design features including magnet coils, divertor chambers, blanket coolant system, water coolant system, and bottom support structure.

Fig. 4.5.-1B. Plan view of the 500-MWe(net) ATR/ST baseline case showing three levels: I) upper TFCs; II) water manifolds and headers; and III) blanket plan view.

Fig. 4.5.-2. Isometric drawing of the FPC for the 500-MW_e(net) ATR/ST baseline case. Details of the coolant manifolding are given in Fig. 4.4.-9.

The relatively tight array of TFCs results in minimum coil mass and stored energy for any resistive-coil tokamak design that attempts to minimize costs. The horizontal legs of the TFCs must be disconnected in order to maintain the divertor chambers and the outboard first wall/blanket/shield. The TFCs are secured at the bottom bends by restraining supports and are secured against overturning forces by the restraining wedges, both of which are described in Sec. 4.5.2.

The choice of the impurity-control system has a strong influence on the FPC layout. The goal of minimum COE (Sec. 3.2.) and related reductions in FPC size and mass also influences the FPC layout. The number, size, location, and kind of penetrations through the vacuum vessel as well as the positions of the PFCs (particularly the DFCs) are determined by these choices. Table 4.5.-I summarizes the FPC component masses that are estimated on the basis of the preliminary layouts given in Fig. 4.5.-1. The upper divertor chamber unit (252 tonnes), the blanket assembly/outboard shield (793 tonnes), and the TFC central column/inboard shield (739 tonnes) are each removed as separate units after the

TABLE 4.5.-I
SUMMARY OF KEY FPC MASSES AND SIZES

<u>Region</u>	<u>Thickness (m)</u>	<u>Volume (m³)</u>	<u>Composition</u>	<u>Mass (tonne)</u>
Inboard shield	0.2	24.5	90%HT-9, 10%H ₂ O	171.2
Divertor shield	0.2-0.5	44.7	90%HT-9, 10%H ₂ O	313.1
Outboard shield	0.2	89.5	100% PCASS	626.2
Subtotal (shield)		158.7		1110.5
Blanket (drained)	1.05	403.6		166.7 ^(a)
Inboard TFC	0.9-1.3	77.7	70%Cu, 10%PCA, 10%MgO, 10%H ₂ O	567.7
Outboard TFC	1.1	368.0	70%Cu, 10%PCA, 10%MgO, 10%H ₂ O	2,686.3
Subtotal (TFC)		445.7		3,254.0
DFC (2 coils)	0.5-1.0	26.0	70%Cu, 10%PCA, 10%MgO, 10%H ₂ O	189.9
EFC (2 coils)		75.3	80%Cu, 20%He	549.5
Subtotal PFC		101.3		739.4
Subtotal (TFC/PFC)		547.0		3,993.4
Subtotal divertor plugs		35.4		251.5
Total (blanket/shield/coils)		1,109.3		5,270.6

^(a)Weight of PbLi in blanket equals 1,716 tonnes

spoke assembly of horizontal TFC legs (280 tonnes) is removed. Removal of the bottom divertor chamber unit also requires removal of the bottom TFC spoke assembly while the TFC central column is supported from above by a crane. The coolant-water lines and PbLi horizontal headers are positioned to facilitate TFC disconnects and to provide space for the vacuum pumps.

For purposes of off-site fabrication and assembly, the reactor blanket is divided into 12 toroidal sectors, each being centered between neighboring TFCs. The 12 sectors would be welded into a single-piece blanket prior to insertion into the reactor cavity, with the PbLi-cooled outboard shield providing primary internal structural support. The 2,509-tonne PbLi-filled blanket/outboard shield and upper divertor chamber (252 tonnes) would be supported from below by a strongback arrangement extending through gaps in the TFCs.

4.5.2. FPC Maintenance Approach. The level of this mechanical integration is sufficient for a preliminary development of the FPC maintenance approach, although considerably more mechanical design is required. The ATR/ST maintenance approach envisages removal of the TFC central column and inboard shield, two divertors and associated manifold units, and the blanket/outboard shield, each as separate units. This maintenance scheme is dictated largely by the divertor coils being located inboard of the TFCs, which means that the latter have to be separable. The TFC central column is an integral unit, as is the blanket/outboard shield, since inboard blanket sectors are not required. It is necessary for this approach that the TFCs have separable joints. The TFC central column and upper divertor unit are accessed by removing the upper TFC spoke assembly of horizontal legs. Efficient maintenance requires that the maintenance areas be properly arranged, including having the coolant manifolds associated with the TFC central column and the divertor units connected inside respective separation planes, as is shown on Fig. 4.5.-1 and 4.5.-2. The use of divertors generally increases the complexity of the maintenance scheme. Besides the requirement of separable TFC joints, the bottom divertor requires a bottom vacuum vessel lid, added space for removal, and a means to support the TFC column during removal of the bottom divertor unit. The following subsections briefly discuss the maintenance procedures anticipated for the TFC central column, divertor unit, and outboard blanket/outboard shield assembly.

4.5.2.1. TFC Central Column. The three outboard connections of the TFC along with the TFC end column connections permit removal of the upper and lower divertor units, the TFC central column, and the blanket/outboard shield

Fig. 4.5.-3. Plan view of the TFC assembly showing horizontal legs, central column, and joints.

Fig. 4.5.-4. Plan and elevation views of TFC assembly, showing demountable joints.

assembly. Figure 4.5.-3 shows a plan view of the TFC assembly, and Fig. 4.5.-4 gives an elevation view with three types of joints shown. A sliding pinned lap joint connects with the TFC central column, with shear pins oriented vertically downward. Located at each upper TFC leg bend is a sliding pinned lap joint, with shear pins directed normally to the TFC plane. Finally, a sliding finger joint is located at the outer part of TFC bend, with shear pins positioned in the radial direction. The midline running through the TFC on Fig. 4.5.-3 depicts the separation of the 50 inboard copper turns or windings from the 30 outboard turns. Each turn has a cross-sectional area of 20 mm by 500 mm, is insulated from neighboring conductors, and requires a separate coolant line. The coolant lines at the joint pass to the outside and are connected to manifolds. The coolant manifolds are connected together so that the coolant flow is continuous, passing near but not through the joint.

Multiple shear pins able to withstand shear forces up to ~ 100 MPa (15,000 psi) and insulated by powdered MgO sleeves and washers provide structural support and when tightened give the desired contact pressure and uniform electrical conductivity. A number of shear pins are required at each TFC joint in order to withstand the normal magnetic shear forces. The various directional forces per unit length ($dF/dl = \vec{i} \times \vec{B}$) are calculated from the coil current and magnetic field. The radial, poloidal, and toroidal components of the magnetic field are calculated by the NEO code, and the results are illustrated in Fig. 2.5.-7. The appropriate shear stress length differs for each joint and can be determined from Figs. 4.5.-3 and 4.5.-4. Taking the shear stress for copper as ~ 100 MPa, the joint at the TFC column for a 20-mm-diameter shear pin requires ten such pins, the lap joint at the upper bend requires four shear pins, and the finger joints at the TFC outboard side require six shear pins. The joint surfaces would be plated with silver or indium to assure that the joints are separable after use. In order to disconnect a TFC joint, the manifolds are disconnected and the shear pins loosened. Such an arrangement has been used on the PDX tokamak⁸⁵ although only four turns per coil are required, the PDX insulators are organic, and generally the connections are simpler. Sliding high-current joints have been proposed for the Ignition Studies Project (ISP).⁸⁶

Figure 4.5.-3 shows the wedge-type units which restrain the turning forces between TFCs. The maximum turning forces occur at the top and bottom of the coils. Similar wedge-type units are positioned between the bottoms of the TFCs.

In addition, the outer fixed portion of the TFCs rest in curved support structures extending from the vacuum vessel floor. These support structures also provide restraint against turning forces.

The TFC central-column end connections are shown in Fig. 4.5.-2. These connections are offset wedge-shaped plates that are bolted to the TFC central column by steel bolts running the length of the TFC column, as well as bolts that form an upward extension of the inboard shield. Because of the higher coolant requirement of the TFC column ($P_{TFC}^{NI} = 64.7$ MW, $P_{TFC}^{OI} = 96.3$ MW, $P_{TFC}^{NO} = 3.5$ MW, $P_{TFC}^{SO} = 36.0$ MW in Table 4.3.-I), the central column is cooled separately from the outboard leg.

The coolant manifolds for the TFC column lie below the upper TFC horizontal-conductor unit and inboard of the upper divertor unit. This arrangement is shown in Figs. 4.5-1 and 4.5-2. In principle, it would be convenient to locate both the TFC central column inlet and outlet manifolds and both the inboard shield inlet and outlet manifolds at the top of the TFC assembly so that only the TFC upper spoke assembly would have to be removed in retracting the TFC central column. This approach would waste manifold space, however, and require longer TFCs. The TFC central column and divertor units, furthermore, may all have to be maintained during the same repair period.

Locating the principal water-coolant manifolds inside the TFC and inside the respective separation planes of the TFC central column and those of the divertor units reduces the number of disconnects by a factor of 2 to 3 and provides more space for the disconnects. This situation follows since the TFC upper and lower horizontal-conductor units must be removed whether the TFC central column or a divertor unit is removed, under the assumption that both divertor units are removed during the same repair period. Locating the manifolds inside the TFC also avoids the space problems associated with penetrations through the TFC. This arrangement also eases the intermediate cooling problem associated with afterheat removal during maintenance. Locating the water coolant manifolds inside the TFC requires an increase in the inside height of the D-shaped coil from 6.85 m to 7.35 m and a decrease in the inboard divertor chamber height by 0.2 m.

The coolant manifolds for the inboard shield are shown in Figs. 4.5.-1 and 4.5.-2. These inlet and outlet manifolds are connected, respectively, to corresponding inlet and outlet manifolds of divertor units and are larger in order to accommodate the increased flow. In replacing the TFC central column,

the upper and lower TFC horizontal-conductor units are removed after first disconnecting the appropriate coolant lines and electrical leads. The TFC column manifold flanges and inboard shield flanges are then disconnected. Since these flanges are located on the divertor separation plane, it is then possible to lift the TFC column/inboard shield ($567.7 + 171.2 = 738.8$ tonnes) as a single unit.

4.5.2.2. Divertor. The inlet and outlet divertor coolant manifolds are shown in Figs. 4.5.-1, 4.5.-2, and 4.5.-4. The flanges associated with these manifolds are located along the two separation planes of the divertor unit. Once the upper (lower) TFC horizontal-conductor unit is removed, these flanges can be disconnected, and the upper (lower) divertor unit removed. In the case for the upper unit, the divertor with coil and manifolds would be lifted out (255 tonne) of the reactor cavity with a crane.

The removal of the lower divertor unit involves the structural support for the FPC. The main structural support for the fixed portion of the TFCs and the blanket assembly is a ring-shaped strongback structure located in the bottom of the outer vacuum vessel with structural supports extending from the bottom of the vessel. The blanket/outboard shield assembly has 12 pads extending upward from the vacuum vessel floor and extending between the TFCs.

The 12 outer fixed portions of the TFCs rest in curved supports shown in Fig. 4.5.-1. The TFC central column is supported by a massive central hydraulic jack cylinder (Figs. 4.5.-1). Twelve outer (smaller) hydraulic jack cylinders (Figs. 4.5.-1) support the bottom divertor unit outer chamber ring. These 12 cylinders extend upward through the TFCs with wings extending for outward bottom support for the TFC spokes. The central cylinder, the outer cylinders, and the bottom vacuum vessel lid would be removed as a single unit.

In order to remove the bottom divertor unit, the central TFC column would be supported from the top by a crane if the column was not already removed. The water coolant separation flanges in the divertor separation planes are disconnected. These flanges are tapered with an outer clamped ring that can be disconnected at one point by a remote mechanical arm positioned in a radial direction. The shear pins in the TFC horizontal-conductor unit would be disconnected, and the bottom divertor unit, bottom TFC spoke, and bottom vacuum vessel lid would be lowered together as the hydraulic cylinders are lowered. The bottom divertor unit would be raised off the support structure by

transhauler units rolled into place and then transported horizontally from the reactor cavity through a tunnel.

4.5.2.3. Blanket/Outboard-Shield Maintenance. The principal blanket structural support is provided by the outboard shield and 12 blanket-section divider ribs. Twelve rectangular PbLi headers are used, necking down to exit between the TFCs at a major radius of about 5 m (Fig. 4.5.-2). These headers are connected to the main coolant manifolds using fusible PbLi flanges, which can be slid into position at any angle and are self-sealing.⁸

Replacement of the blanket/outboard shield assembly is expected every four years for the 500-MWe(net) baseline. This replacement involves removing the upper TFC horizontal-conductor unit, disconnecting divertor manifold flanges, removing water headers running to flanges located at the vacuum vessel, and removal of the 12 upper outboard quadrant TFC sections (280 tonne). The upper TFC horizontal-conductor unit with the upper quadrant TFC sections attached could be removed as a single piece (901 tonne). The PbLi is transferred by gravity to the drain tanks below the vacuum vessel through drain lines leading from the lower PbLi manifold. After draining the PbLi from the blanket assembly, the PbLi flanges are disconnected, and the drained blanket/inboard shield assembly (812 tonnes) would be lifted vertically with a crane.

4.5.2.4. Alternative FPC Maintenance Schemes. The complexity of the maintenance approach previously described derives in large part from the requirement to remove the two divertor units individually. Other simpler maintenance approaches can be envisioned and are summarized below.

Single-Null Divertor. From the viewpoint of equilibrium, stability, or heat loading, a single null divertor may also be possible. For the divertor heat fluxes expected for the 500-MWe(net) baseline design, a single null divertor could be used by either doubling the divertor chamber volume or using the present volume with a doubling of the number of coolant passes. A single divertor would allow removal of all the intricate support structure on the bottom and the associated building volume. In addition, two TFC joints would be eliminated, and the blanket could be expanded into the lower section, thereby reducing somewhat the required lithium enrichment for the same TBR and increasing the thermal efficiency. The upper divertor and the blanket assembly would remain as separate units, as described for the baseline case.

Upward Removal of Double-Null Divertors. Moderate redesign of the outboard parts of the divertors and the blanket, which includes offsetting the upper part of the blanket more radially outward than the lower part, would allow a crane to lift the upper divertor followed by a crane lift of the lower divertor. Since both divertors would probably have to be removed at the same time, this maintenance scheme offers the previously described advantages of single-piece divertor maintenance. Specifically, the large support structure and volume at the bottom are not required, and the two bottom TFC joints would be eliminated. Both divertor units in this case would have to be removed before the blanket assembly can be removed, however.

Horizontal Removal of Double-Null Divertor and Blanket Sectors. In this maintenance scheme, the divertor coils would be displaced in a vertical direction toward the TFCs. The arrangement would permit the 12 inboard shield/divertor units/blanket sectors to be separately moved outward through the TFCs. This maintenance scheme would involve increasing the length and radius of the TFCs, perhaps with some thinning on the outside. The PbLi and water headers would have exits between the EFCs, which would also have to be displaced vertically. This scheme would unfortunately also require TFC joints, since the DFCs have to be replaced occasionally. Again, the large support structure and volume at the bottom of the FPC are not required.

Horizontal Torus. The arrangement of the ST with a horizontal major axis is a possibility because of the low-aspect-ratio, spherical nature. The unconventional tilting of the ST, so that the major toroidal axis is horizontal, however, is expected not to aid the maintenance schemes because of the large unit weights and the need for horizontal removal of massive components.

4.5.3. Other Systems. Only a cursory examination of other key systems that comprise a fusion power plant was possible within the scope of this preliminary study. Brief mention is made of magnet, vacuum, fueling, tritium handling and current drive requirements in this section.

4.5.3.1. Magnets. The ATR/ST magnet requirements have largely been described in Sec. 4.5.2 as part of the maintenance approach. Table 4.5.-II gives key design parameters for the resistive TFCs, resistive DFC, and superconducting EFC.

4.5.3.2. Vacuum. Vacuum is provided to the reactor torus by enclosing the entire FPC within a vacuum vessel. The cylindrical vacuum vessel mean diameter is 14 m and a wall thickness of 0.2 m. The 10-m-long cylindrical vacuum vessel

TABLE 4.5.-II
NOMINAL COIL PARAMETERS FOR ATR/ST DESIGN
ESTIMATED FROM PARAMETRIC SYSTEMS CODE

<u>Parameter</u>	<u>TFC</u>	<u>DFC</u>	<u>EFC</u>
Conductor	Copper	Copper	NbTi/Cu
Coil thickness, $\delta(\text{m})$	1.1	0.9	0.6
Current center coordinates, $r(\text{m})/z(\text{m})$		2.40/5.75	7.50/1.50
Total coil-set mass (tonne)	3,254.0	189.9	549.5
Magnetic field at conductor, $B_{c\theta, \phi}(\text{T})$	7.04		6.86
Stored energy, $W(\text{MJ})$	6.1		

has two end caps (full diameter on top and divertor unit removal diameter on bottom) with a total volume of 2,460 m³. Vacuum cryopumps are attached to the side of the vacuum vessel near the divertor chambers, giving more space in the otherwise congested region inside the vacuum vessel; pump maintenance is also eased by this arrangement. The relative smallness of the vacuum vessel, primarily as a result of the small aspect ratio of the ST, promises a reduced reactor building relative to the STARFIRE tokamak⁷ or MARS tandem-mirror²⁰ reactors.

4.5.3.3. RF Startup. Because of the small aspect ratio, the ATR/ST has little or no room for OHCs and fast wave current drive (FWCD) will be needed to initiate and ultimately achieve the $I_{\phi} \approx 40\text{-MA}$ plasma current. Furthermore, if OFCD proves infeasible, FWCD will also be required to sustain this current against ohmic losses, albeit the optimum plasma parameter would be different from those for the baseline. Startup powers for LH-drive that peak at 100-150 MW and decrease to 60-MW for sustainment would be required. The period of time during which this startup power would be applied would exceed ~ 400 s in order to hold these peak-power limits. For an effective antenna or duct power density of 10 MW/m², approximately 4.8% of the 261-m² outboard first wall would be dedicated to the startup function. The cost of this startup power is a crucial issue.

4.5.3.4. Fueling. The primary function of the ATR/ST vacuum and fueling systems is the removal of excess impurities and helium ash generated by fusion reactions. Purging of the helium ash is accompanied by the removal of large quantities of unburned DT fuel for subsequent reprocessing and reinjection into the plasma chamber as fresh fuel (pellets). The external fueling rate must

balance the combined DT exhaust and burnup in steady state. The rate of generation, and, hence, removal of helium ash by the fusion reactor is given by

$$\phi_{\text{He}}(\text{He/s}) = \frac{P_F}{17.6k_B/10^3} , \quad (4.5.-1)$$

which for $P_F = 1,672 \text{ MW}$ and $k_B = 1.602(10)^{-16} \text{ J/keV}$ equals $5.94(10)^{20} \text{ He/s}$ or $2.23 \text{ Pa m}^3/\text{s}$ (17.0 l torr/s).

The molecular DT gas load being pumped must be calculated consistently with an appropriate edge-plasma/divertor model. A detailed analysis is beyond the scope of this study, but the following simplified expression relates the DT gas flow to the average plasma density, $n = 1.21(10)^{20} \text{ m}^{-3}$, particle confinement time, $\tau_p = 4\tau_E$, where the global energy confinement time is taken as $\tau_E \approx 0.8 \text{ s}$ (Table 1.5-I), and the plasma volume $V_p = 358 \text{ m}^3$

$$\phi_{\text{DT}}(\text{Pa m}^3/\text{s}) = 1.88(10)^{-21} \frac{nV_p(1 - R_{\text{DT}})}{\tau_p(1 - R_{\text{Le}}^*)(1 - R_{\text{LS}}^*)} . \quad (4.5.-2)$$

In this expression, R_{DT} is an effective divertor-slot reflection coefficient ($R_{\text{DT}} = 0$ implies the divertor slot is "black" to incident particles), R_{Le}^* is an edge-plasma recycling coefficient, and R_{LS}^* is a scrapeoff recycling coefficient. For $R_{\text{DT}} \approx 0$, $R_{\text{Le}}^* \approx R_{\text{LS}}^* \approx 0.5$, the DT flow rate to the divertor pumps amounts to $101.8 \text{ Pa m}^3/\text{s}$ (773.7 l torr/s) or 19.6 kg DT/d . This gas flow along with the 0.36 kg He/d must be handled by the divertor pumps.

The $\sim 19.6 \text{ kg/d}$ of DT must be supplied across the separatrix by the periodic injection of cryogenic ($\rho_0 \approx 250 \text{ kg/m}^3$) DT pellets.⁸⁷ As the pellets penetrate the plasma, ablation or evaporation contributes to the particle inventory. Larger initial pellet radius or higher injection speed allows deeper penetration into the plasma. It is desirable to limit the individual pellet particle inventory to a small fraction, g , of the nominal plasma particle inventory to reduce fluctuations in the density and fusion power during refueling. The time interval between pellet injection is $\tau_I(\text{s})$.

The pellet ablation process has been considered by several authors.⁸⁷⁻⁹⁰ The basic model applied here is the so-called "neutral ablation cloud" model.⁸⁸ Corrections for the perturbation of the local magnetic field by the pellet lead to somewhat (up to ~ 50%) reduced pellet ablation rates and longer lifetimes.⁸⁹ The surface ablation, not including alpha-particle effects, of a spherical pellet with initial radius $a(m)$ is represented by the following expression for the time-rate-of-change of the pellet radius:⁸⁸

$$\dot{a}(m/s) = \frac{-1.0(10)^{-8} n_e^{1/3} T_e^{5/3}}{a^{2/3} m^{1/3}}, \quad (4.5.-3)$$

where $m(\text{amu})$ is the molecular weight of the fuel (e.g., 5 amu for DT), $n_e(m^{-3})$ is the target plasma electron density, and $T_e(\text{keV})$ is the target plasma electron temperature. Integration of Eq. (4.5.-3) gives the pellet lifetime, $t_p(s)$, or the time after initial injection ($t = 0$) at which the pellet completely evaporates ($a \rightarrow 0$). Assuming a constant pellet injection speed, $u(m/s)$, the maximum pellet penetration distance, $d(m)$, is given by $d = ut_p$. For radial plasma density and temperature profiles of the form $[1 - (r/r_p)^v]$, where $r_p(m)$ is the plasma radius and v is a parameter, the integration of Eq. (4.5.-3) can be rewritten in the following form

$$(3/5)a^{5/3} = \int_s^{r_p} \frac{1.0(10)^{-8} n_{e0}^{1/3} T_{e0}^{5/3}}{m^{1/3} u} [1 - (r/r_p)^v]^2 ds, \quad (4.5.-4)$$

where $s = r_p - d$ is the penetration distance along a perpendicular injection trajectory and n_{e0} and T_{e0} are the central ($r = 0$) values of plasma density and temperature, respectively. For the profile approximation used here, $n_{e0} = n(v + 2)/v$ and $T_{e0} = T(v + 2)/v$. Injection beyond the center of the plasma column is not of interest, so that solutions of Eq. (4.5.-4) can be limited to the range $0 < d < r_p$. Hence,

$$u(m) = 1.67(10)^{-8} \frac{n_{eo}^{1/3} T_{eo}^{5/3} r_p}{m^{1/3} a^{5/3}} \left(\frac{d}{r_p} - \frac{2}{(\nu + 1)} [d/r_p]^{(\nu + 1)} + \frac{1}{(2\nu + 1)} [d/r_p]^{(2\nu + 1)} \right), \quad (4.5.-5)$$

which gives the required pellet injection speed as a function of normalized penetration distance, d/r_p , for fixed plasma and pellet properties. Parabolic profiles ($\nu = 2$) for both plasma temperature and density profiles are assumed for the ATR/ST. Furthermore, injection along a path in the equatorial plane from the outboard side (between the EFCs) is assumed for convenience.

The time interval between pellet injections is given by $\tau_I(s) = M_p/\dot{M}_p$, where M_p is the pellet mass ($\rho_0 = 250 \text{ kg/m}^3$, $n_0 = 3.18(10)^{28} \text{ m}^{-3}$), and $\dot{M}_p = 2.27(10)^{-4} \text{ kg/s}$ is the required mass flow rate. Pellet-fueling parameters for several combinations of assumed ratios of pellet to plasma inventory, g , and normalized penetration distance, d/r_p , are summarized in Table 4.5.-III. Assuming advanced laser-ablation technology, the pellet injection speed is

TABLE 4.5.-III

PELLET-FUELING PARAMETER VARIATIONS(a)

Pellet Inventory/Plasma Inventory, g :	<u>0.01</u>	<u>0.05</u>	<u>0.10</u>
Pellet radius, $a(\text{mm})$	1.18	2.02	2.55
Pellet injection time, $\tau_I(s)$	$7.62(10)^{-3}$	$3.81(10)^{-2}$	$7.62(10)^{-2}$
Pellet mass, $M_p(\text{kg/pellet})$	$1.73(10)^{-6}$	$8.65(10)^{-6}$	$1.73(10)^{-5}$
Pellet velocity, $u(\text{km/s})$, for penetration depth, d/r_p :			
0.25	236	96	66
0.333	304	124	85(b)
0.50	416	170	116
0.75	508	208	141
1.0	525	215	146

(a) Fixed parameters, $\nu = 2$, $n = 1.21(10)^{20} \text{ m}^{-3}$, $T = 15 \text{ keV}$, $r_p = a = 1.5 \text{ m}$, $\tau_p = 3.2 \text{ s}$, DT injection rate, $\dot{M}_p = 2.27(10)^{-4} \text{ kg/s}$.
(b) Interim reference case.

limited to $u \leq 100$ km/s, requiring $g \geq 0.2$ and $d/r_p \leq 0.33$. Near-term injector technology based on pneumatic or centrifugal acceleration can attain $u \approx 2$ km/s, which is inadequate for this application. A model including magnetic-shielding effects would give somewhat lower estimates of the required pellet speed. Direct penetration of the pellet to the central plasma may not be necessary if the fuel can diffuse inward. This diffusion process was assumed for the STARFIRE design,⁷ which is based only on edge-plasma refueling. Calculations of pellet refueling of the central cell of the MARS design²⁰ using a similar model have projected $u \approx 43$ km/s for $d/r_p = 1.0$ and $u \approx 1.2$ km/s for $d/r_p = 0.2$. In both cases, $g = 0.1$ and $a \approx 4$ mm. A similar calculation for the WITAMIR design⁹¹ yields $u \approx 114$ km/s for $g = 0.1$ and $a \approx 3$ mm. Clearly, deep refueling by pellet injection represents a development area for magnetic fusion in general.

4.5.3.5. Tritium. Two inlet and outlet headers exist in the primary PbLi coolant circuit leading from the respective PbLi manifolds and passing through the vacuum vessel wall, and each inlet/outlet set connects with a separate heat transfer loop consisting of a pump, steam-generator-module (superheater, steam generator, and economizer), and a PbLi-cleanup and tritium removal system. The PbLi superheater, steam generator, and economizer are based on a double-wall-tube design⁹² to prevent tritium from penetrating into the steam system. The primary water coolant circuit is similar to the PbLi coolant circuit except the water circuit has no superheaters because the reactor water exit temperature ($\sim 300^\circ\text{C}$) is much lower than that of PbLi (401°C). The reactor, steam turbine, and tritium-recovery buildings would be closely coupled to minimize piping runs and total building volume and related costs.

4.5.3.6. Oscillating-Field Current Drive (OFCD). Although the fundamental equation being solved for OFCD is identical for both the ATR/ST [Eq. (2.4.-8)] and the RFP,² the significant differences in magnetic-field and current-density profiles are expected to yield different results. Within the context of the electrical-circuit analogue being modeled, these differences in profiles are reflected as differences in the plasma inductance, L_p , and the dependence of L_p on plasma current through the pinch parameter, Θ . These differences have been discussed in Sec. 2.4.2.1. and summarized on Fig. 2.4.-8. As seen from Eq. (2.4.-8), OFCD operates on the basis of these non-linearities in L_p and $dL_p/d\Theta$, under the assumption that plasma processes exist to allow the required profile adjustments in response to sinusoidal oscillations applied to V_ϕ and V_Θ .

at the plasma surface. The variation in F , Θ , L_p , and $dL_p/d\Theta$ over each cycle must allow a net voltage to sustain the plasma current against resistive decay [i.e., the $I_\phi R_p$ term in Eq. (2.4.-8)]. In the case of the RFP ($\Theta \approx 1.5$ in Fig. 2.4.-9), both the L_p and $dL_p/d\Theta$ terms in the I_ϕ and V_Θ coefficients can be comparable, and the strong second-order non-linearity that is so manifested should be capable of efficiently sustaining plasma currents against large dissipative losses. The picture of the ST developed by NEQ (Fig. 2.4.-9), however, indicated that a) L_p is nearly constant in the Θ range of interest, and b) $(\Theta/2)(dL_p/d\Theta)$ in magnitude is much less than both the L_p and $L_0(1-F)/(\epsilon\Theta)^2$ terms in the appropriate coefficient of Eq. (2.4.-8). Hence, the non-linearities that might drive current in the ST (given a non-disruptive relaxation mechanism) are much weaker for the equilibrium model used, and the nature of OFCD in the ST geometry is expected to be different from that of the KFP² and possibly technologically more difficult.

These differences can best be illuminated by recasting Eq. (2.4.-8) in terms of the parameter $\xi = d \ln L_p / d \ln \Theta^2$ as follows:

$$V_\phi = I_\phi R_p + L_p(1 + \xi) \dot{I}_\phi + \epsilon\Theta(L_p/L_0) \left[\frac{1-F}{(\epsilon\Theta)^2(L_p/L_0)} - \xi \right] V_\Theta, \quad (4.5.-6)$$

where again $V_\phi = \delta V_\phi \cos \omega t$ and $V_\Theta = \delta V_\Theta \sin \omega t$ are the toroidal and poloidal voltages impressed on the plasma, and ideal circuit coupling is assumed for the present level of analysis. As seen from Fig. 2.4.-9, $L_p \approx 1.0 \mu H$ for the NEQ model of the ST, and the vacuum toroidal inductance is $L_0 = \mu_0 R_T^2 \epsilon^2 / 2 \approx 0.52 \mu H$. The NEQ results on Fig. 2.4.-8 can be accurately fitted by $(1-F)/\Theta^2 \approx 1.11$. Figure 4.5.-5 gives the dependence of ξ and $(1-F)/[(\epsilon\Theta)^2(L_p/L_0)]$ for both the circularized, one-dimensional (Sec. 2.4.2.1.) ST model as well as for NEQ; a comparison with the CRFPR is also given. The NEQ model of the ATR/ST allows ξ to be obtained in Eq. (4.5.-6), and, along with the fit $\Theta \approx 7(10)^{-9} I_\phi + 0.18$ from the NEQ calculations, gives the following approximation to the OFCD equation:

$$V_\phi = I_\phi [R_p + 7.7(10)^{-9} V_\Theta / \epsilon] + 0.2 V_\Theta / \epsilon + L_p \dot{I}_\phi. \quad (4.5.-7)$$

Integrating this expression over one period and enforcing a constant average toroidal current gives

$$\frac{R_p \epsilon}{7.7(10)^{-9}} = \left(\frac{\delta I_\phi}{I_{\phi 0}} \right) \delta V_\Theta \cos \phi, \quad (4.5.-8)$$

where I_ϕ was assumed to lag the poloidal voltage δV_Θ by the phase angle ϕ . This result gives a clear indication of the consequences of lost non-linearity in the ATR/ST, as modeled by NEQ: the bulk of the OFCD must be provided by the toroidal-field circuit, and the associated reactive power can be high since $\phi \leq 90^\circ$ is required to maximize the OFCD effect.² The associated reactive power must be handled directly by the TF circuit rather than the PF circuit, which can store much of the field energy associated with this reactive power in the intrinsic toroidal inductance. Taking typical ATR/ST values [$R_p = 9.4(10)^{-9}$ ohm, $\epsilon = 1/1.8$, $B_{\phi V}(0) \approx 2.3$ T, $R_T = 2.7$ m], the desire for small fluctuations in the plasma current (e.g., $\delta I_\phi / I_\phi \leq 0.01$) requires that $\delta V_\Theta \geq 68 / \cos \phi$. Using the design values for $B_{\phi V}(0)$ and R_T gives $I_\Theta \approx 62$ MA for the closely coupled case, which then gives $(\delta V_\Theta) I_\Theta \approx 4.2 / \cos \phi$ gigawatts for the reactive power that must be handled by the TF circuit; the large phase shifts expected will lead to enormous reactive powers that cannot be easily isolated from conductors and other TF circuit elements. As illustrated for a number of profile modes in Fig. 4.5.-5 and referring to the OFCD equation [Eq. (4.5.-6)], it is the loss of non-linear coupling of the poloidal- and toroidal-current flows within the plasma that requires overdriving the surrounding circuitry in order to observe a net current-drive effect. Equilibrium profiles other than those predicted by NEQ will be required in order to increase the dependence of L_p on I_ϕ as Θ (i.e., increase ξ). The tuning of the ATR/ST equilibrium configuration to optimize OFCD performance, however, was beyond the scope of this study. These problems for the application of OFCD to the tokamak appear to be related to the concerns discussed in Sec. 2.4.2.1. and reflected in Fig. 2.4.-10. The search for and quantification of relaxation mechanisms and local profile tailoring, however, must await future efforts. These more-detailed studies must relate the lumped-parameter current-drive model to local profile changes, such as those suggested in Fig. 2.4.-11.

Fig. 4.5.-5. Dependence of main elements of the coefficients in the OFCD equation [Eq. (2.4.-8) or (4.5.-6)] that causes profile-related non-linearities responsible for the current-drive effect. Both the NEQ and the circularized one-dimensional-model (Sec. 2.4.2.1.) results are shown. A comparison with typical RFP profiles is also given.

In view of these estimates, application of OFCD to the ATR/ST with a minimization of reactive power flowing to and from the plasma will require a minimum swing in the toroidal flux, since the largest peak powers are expected to occur in the TF circuit. Gauging from the RFP results,² however, small relative toroidal flux swings, $\delta\phi/\phi_0$ are expected to require large reactive

powers flowing to and from the PF circuit, and an optimum (i.e., minimum reactive power) is expected. In order to estimate these effects, Eq. (2.4.-8) was solved for 60-Hz oscillations under the constraints that the cycle averages of the time derivatives of both toroidal plasma current, $\langle I_\phi \rangle$ and magnetic helicity, $\langle K \rangle$ (Sec. 2.4.2.1.), be zero. As discussed in Ref. 2., the simultaneous requirement that both $\langle I_\phi \rangle$ and $\langle K \rangle$ equal 0 requires that the difference between energy and helicity resistances be determined iteratively; the ratio $R_p(\text{helicity})/R_p(\text{energy}) \equiv f_A \approx 2.0$ results for the particular ATR/ST conditions being studied.

Figure 4.5.-6 gives as a function of $\delta\phi/\phi_0$ the peak powers (TF, PF, and total), the normalized plasma current swing, $\delta I_\phi/\langle I_\phi \rangle$, and amplitude of the

Fig. 4.5.-6. Steady-state OFCD results as a function of toroidal flux variation for the ATR/ST baseline parameters. Table 4.5.-IV summarizes typical values for the point of minimum reactive power.

Fig. 4.5.-7. Steady-state OFCD results as a function of toroidal flux variation for the CRFPR baseline parameter.²

toroidal voltage oscillation, δV_ϕ . All results are for a drive frequency of 60 Hz. Figure 4.5.-7 gives a similar result for the CRFPR(20) design.² Table 4.5.-IV gives typical parameters for the point of minimum reactive power, which is in excess of four times greater than for the CRFPR case. As indicated on Fig. 4.5.-7, achieving the minimum reactive power for the RFP case is inhibited by a loss of field reversal (i.e., $F > 0$ on Fig. 4.5.-7). It should be noted that this model assumes ideal coupling of the OFCD coils and the plasma, and, therefore, the reactive powers listed are minimum values for the plasma profiles assumed.

Given that a) a non-disruptive relaxation mechanism exists for tokamak profiles and b) reactive powers at the level suggested in Table 4.5.-IV can be efficiently managed, the issue of OFCD cost presents an issue. For example, the cost of the more-or-less conventional circuitry needed to handle this reactive

TABLE 4.5.-IV
SAMPLE OFCD PARAMETERS FOR THE ATR/ST AT THE POINT OF MINIMUM
TOTAL REACTIVE POWER (Fig. 4.5.-6)(a)

Toroidal flux variation, $\delta\phi/\phi_0$	0.0020
Toroidal flux, ϕ_0 (Wb)	69.4
Toroidal current variation, $\delta I_\phi/\langle I_\phi \rangle$	0.0146
Toroidal current, I_ϕ (MA)	40.0
Drive frequency (Hz)	60.0
Peak reactive power in TF circuit, P_ϕ (GW)	7.06
Peak reactive power in PF circuit, P_θ (CW)	7.48
Peak total reactive power, $P_{\theta,\phi}$ (GW)	10.24
Peak toroidal voltage variation, $\delta V_\phi/\langle f_A I_\phi R_p \rangle$	493.63
Toroidal loop voltage, $\langle f_A I_\phi R_p \rangle$ (V)	0.38
Helicity/energy resistance ratio, f_A	2.03
Average power delivered by PF circuit, $\langle P_\phi \rangle$ (MW)	-16.65 ^(b)
Average power delivered by TFC circuit, $\langle P_\theta \rangle$ (MW)	31.75
Power dissipated in plasma, P_Q (MW)	15.09
PF circuit Q-value, $Q_\phi = P_\phi/\langle P_\phi \rangle$	-450. ^(b)
TF circuit Q-value, $Q_\theta = P_\theta/\langle P_\theta \rangle$	222.
OFCD Q-value, $Q = P_{\theta,\phi}/P_Q$	682.

(a) Pinch parameter, $\Theta = 0.47$ and reversal parameter, $F = 0.76$.

(b) Slight wave-form asymmetries actually result in a net power flow out of the PF circuit.

power is expected to be in the range 10-20 \$/kVAR; unless the OFCD Q-value can be decreased by 5-10, the cost per unit of power delivered to the plasma will be comparable to the cost of fast-wave current drive (2-3 \$/W). Such decreases in OFCD Q-values may be possible by local profile tailoring (i.e., Sec. 2.4.2.1.) and this approach deserves further investigation as a means to reduce the reactive power and cost of an otherwise low-technology (i.e., low-frequency, low amplitude) current-drive scheme.

5. SUMMARY AND CONCLUSIONS

The basecase ATR/ST design selected a resistive TFC system that operates at moderate to low power levels (500 MWe); the cost of power recirculated to the TFCs was balanced by the reduced coil and overall FPC mass, although the nuclear economy of scale penalized the lower-power 500-MWe(net) design. Figure 5.-1 shows the dependence of COE and UDC on net electrical power; also shown is the dependence of engineering Q-value, Q_E (inverse of recirculating power fraction), which is generally low and is the main cause of driving upwards both COE and UDC. The economy-of-scale curves given in Fig. 5.-1 are for the minimum-COE cases as well as for a system that is constrained to a larger plasma radius ($a = 1.5$ m) and lower neutron wall loadings ($I_w \leq 5-6$ MW/m²). Figure 5.-2 repeats the minimum-COE component of Fig. 5.-1 in order to give a comparison with present projections for fossil (coal) power at two fuel costs and fission power (PWR) based on both medium and best experience.⁹³ Generally, the ATR/ST falls within a range predicted for "best-experience" and "medium-experience" PWRs, but becomes competitive with fossil power plants only for unit sizes above ~ 1000 MWe(net). These positive predictions, however, are based on a) the existence of an efficient and cost-effective current-drive mechanism, and b) the attainment of a stable, high-beta configuration with acceptable transport properties by invoking low-aspect-ratio, highly-elongated plasmas and a considerable extrapolation of the present-day tokamak database.

A number of influences other than net electric power and the choice of resistive copper coils drive the ATR/ST design. Firstly, the choice of a double-null divertor exaggerates the vertical plasma elongation, which in turn contributes to the size, mass, and cost of the FPC; a single-null divertor, possibly operating at higher heat flux, should be examined as a means to increase the FPC mass power density and possibly reduce cost. Secondly, higher plasma triangularity may be required by high-beta stability considerations (Fig. 2.2.-3); higher plasma aspect ratio will be required, which in turn will adversely affect the MPD and the divertor design. Thirdly, the choice of an integrally-cooled blanket and first wall has limited the neutron wall loading (plasma radius) to values below (above) the point of minimum COE; although small (10-15%), operating near to the $I_w \approx 10-15$ MW/m² minimum-COE would reduce FPC size and cost. Fourthly, if oscillating-field current drive (OFCD) proves infeasible for either physics or cost reasons, the use of fast-wave current drive (FWCD) will lead to higher-cost systems that reoptimize at lower plasma

Fig. 5.-1. Economy-of-scale curves for both minimum-COE and I_w -constrained ATR/ST designs.

Fig. 5.-2. Comparison of minimum-COE ATR/ST costs with costs projected for fossil (coal) power and fission power (PWR) based on medium experience (PWR/ME) and best experience (PWR/BE).⁹³

density and higher temperature; the current-drive issue represents the greatest uncertainty for the ATR/ST design in that the use of either Alfvén or lower-hybrid FWCD will decrease Q_E from the ~ 3.1 design value predicted for OFCD to below 2.5 (Fig. 2.4.-12). Lastly, even with an integrally cooled blanket and first wall, a significant fraction ($\sim 39\%$) of the total fusion power is delivered to the water-cooled divertor and central columns shield, thereby lowering the overall plant thermal efficiency from a potential maximum of $\geq 42\%$ (i.e., entirely cooled with PbLi) to the design value of 36%.

The ATR/ST based on a resistive-coil FPC remains large, although the FPC for $P_E = 1,200$ MWe(net) can be nearly four times less massive than the STARFIRE design. As shown by the trade study summarized in Fig. 3.2.-8, however, selecting a superconducting design results in a system of the STARFIRE size, but with costs that do not differ significantly from those predicted for the resistive coil systems, in that the cost of large superconducting magnets

tradeoff evenly with reduced recirculating power for the tokamak scaling used. Figure 5.-3 gives a comparison of COEs for STARFIRE, MARS, the ATR/ST, and the recently completed compact RFP reactor design, CRFPR,⁸ which like the ATR/ST, is a resistive-coil reactor concept. Table 5.-I compares normalized costs and unit costs for these various designs. Figure 5.-4 gives a comparison of FPC cross-sections between the ATR/ST and other recent fusion reactor designs. Generally, the early STARFIRE and MARS designs project smaller COEs despite low values of MPD because of the lower unit costs assumed for the basic FPC components; as seen from TABLE 5.-I, the STARFIRE FPC costs 19 \$/kg compared to 45 \$/kg for the ATR/ST with the higher (more realistic) unit costs also characterizing other recent designs (Table 5.-I).

A number of positive indications have emerged from the ATR/ST study, some of these having potentially strong developmental and operational cost advantages not necessarily reflected by the cost models presently being used. Firstly,

Fig. 5.-3. Comparison of COE versus MPD for STARFIRE,⁷ MARS,²⁰ the ATR/ST, and the CRFPR.⁸

TABLE 5.-I

COMPARISON OF CONSTANT-DOLLAR COSTS NORMALIZED AS PERCENTAGE OF
TOTAL DIRECT COST FOR A RANGE OF CONCEPTUAL FUSION REACTORS
(1980 DOLLARS, a factor 1.348 takes these costs to 1986)

<u>ACCOUNT</u>	<u>UWMAK-I⁹⁴</u>	<u>STARFIRE⁷</u>	<u>MARS²⁰</u>	<u>CRFPR⁸</u>	<u>ATR/ST</u>	<u>CSR⁹⁵</u>	<u>PRW^{28,93}</u>
20. Land and Land Rights	0.11	0.19	0.21	0.30	0.22	0.34	--
21. Structure and Site Facilities	13.11	20.09	10.56	24.41	18.58	27.00	22.34
22. Reactor Plant Equipment (RPE)	53.82	56.00	64.15	37.31	51.29	50.43	34.01
22.1.1. First Wall/Blanket	6.95	4.77	3.01	0.95	2.98	1.90	--
22.1.2. Shield	3.88	10.78	3.17	0.19	1.12	--	--
22.1.3. Coils	17.82	9.90	20.84	3.09	11.03	2.78	--
FPC	28.65	25.48	27.02	4.23	15.13	4.68	~5.-6. (a)
23. Turbine Plant Equipment	16.01	14.47	11.63	20.17	16.16	22.81	24.99
24. Electric Plant Equipment	13.40	6.77	6.76	10.17	8.01	12.41	8.56
25. Misc. Plant Equipment	0.88	2.37	1.40	3.76	2.82	4.28	4.67
26. Special Materials	2.65	0.014	5.28	3.89	2.91	2.72	5.33
90. Total Direct Costs (TDC)	100.	100.	100.	100.	100.	100.	100.
99. Total Costs	154.25	185.23	138.10	136.28	138.87	136.28	158.93
Unit Direct Cost, UDC(\$/kWe)	1,150. (b)	1,439.	1,633. (c)	1,112.	1,485.	977.8	562. (a)
Cost of Elec., COE(mills/kWeh)	36.1 (b)	35.1	38.3 (c)	27.9	37.8	24.6	--
Unit FPC costs, c _{FPC} (\$/kg)	7.	19.	25.	42.	45.	53.	40-50 (d)
Net Electric Power, P _E (MWe)	1,437.	1,200.	1,202.	1,000.	1,000.	1,000.	1,139.

(a)Originally reported in 1984 dollars, a factor of 1.25 converts to common-base 1980 costs.⁷⁴

(b)Originally reported as 742 \$/kWe and 23.3 mills/kWeh in 1974 dollars; a factor of 1.55 converts to common-base 1980 costs.⁷⁴

(c)Based on 1966 \$/kWe and 46 mills/kWeh in 1983 dollars; factor of 1.21 converts back to common-base 1980 costs.⁷⁴

(d)Not explicitly reported in Ref. 28, but the ~ 1000-tonne pressure vessel (including heads) costed at ~ 50 \$/kg would give the listed value. Reference 23 reports the Nuclear Steam Supply System (NSSS) cost is 20.49% of the total direct cost.

Fig. 5.-4. Cross-section of FPCs for a range of fusion reactors, including the RFP, spheromak, and ST concepts summarized on Table 5.-I. The PWR shown is the British Sizewell 'B',⁹⁶ which is somewhat larger than the US PWR.²⁸ Both the Ref. 8 compact RFP reactor as well as more recent designs with lower neutron wall loading.⁹⁷ The RFPR is an early superconducting RFP design.⁹⁸ The other designs are as follows: Modular Stellarator Reactor, MSR-IIB⁹⁹; the ELMO Bumpy Torus Reactor, EBTR¹⁰⁰; the STARFIRE tokamak⁷; the Torsatron Reactor, UWTOR-M¹⁰¹; the Tandem Mirror Reactor, MARS²⁰; and the Compact Spheromak Reactor.⁹⁵

although physically large, the ATR/ST design allows few-piece FPC maintenance, with the TFC central column (including inboard shield), divertor assemblies, and the outboard blanket/shield assembly each being maintainable as a single unit. These advantages are permitted by the low-field resistive TFCs that can be designed with breakable joints. Secondly, all coils, including the superconducting EFCs, operate at relatively low field (7.2, 4.9, and 6.9 T, respectively, for the TFC, DFC, and EFC), although the TFC and EFCs are large. Thirdly, the ATR/ST configurations are naturally suited for magnetic-divertor impurity control, in that both first-wall and breeding-blanket perturbations are small (13% and 14%, respectively), the heat fluxes are moderate (1.3-1.5 MW/m², average), and the system appears to be maintainable; this conclusion is probably a first for tokamak commercial reactor design and gives promise for a major reconciliation between reactor physics (i.e., maintenance of favorable H-mode transport) and engineering (accessibility, low fields). Fourthly, although the large currents that are a natural consequence of the ATR/ST geometry present serious problems for current drive, particularly if OFCD proves infeasible, when combined with strong neoclassical resistivity enhancement, the ATR/ST can operate with a significant amount of ohmic heating. These large plasma currents should also be favorable from a plasma confinement viewpoint; the cost-optimized ATR/ST design requires a level transport ($\tau_E \approx 0.7$ s, $\chi_E \approx 0.56$ m²/s) that is a third of that predicted by Kaye-Goldston⁴⁷ scaling and one-sixth that predicted by neo-Alcator²⁵ scaling. Along with current drive, however, the stability and unique transport issues in this high-beta, low-aspect-ratio, highly elongated plasma remain as the main unknown issues at this time.

In conclusion, the resistive-coil ATR/ST projects a reactor that in the 1000-1200 MWe(net) class can operate with mass power densities above 100 kWe/tonne and thereby portend competitive economics¹⁵; further improvements are desirable, however, to reduce the projected COE and technological complexity. The possibility of a cryogenic TFC column (a major recirculating power consumer) and further FPC shrinkage by the use of pumped limiter impurity control are options worthy of further exploration. Along with the physics of OFCD, the beta limits imposed by stability and transport remain as issues that can be resolved only by experiment, although further reactor optimization towards somewhat high plasma aspect ratios and triangularity are warranted on the basis of present theoretical understanding. The adverse impact of this reoptimization on the double-null divertor may be sufficient for re-

consideration of single-null divertors or pumped-limiter impurity control. The reduced size (increased MPD relative to STARFIRE) and technology (low-field resistive coils, few-piece maintenance, workable divertors) remain as major attractions for the ATR/ST approach.

REFERENCES

1. R. A. Krakowski, "Fusion Concepts With Improved Prospects for Reactors," SAE P-164, 3, 3.6, 20th Intersoc. Energy Conversion Eng. Conf. (IECEC), Miami Beach, FL (August 18-23, 1985).
2. R. L. Hagenson, R. A. Krakowski, C. G. Bathke, R. L. Miller, M. J. Embrechts, N. M. Schnurr, et al., "Compact Reversed-Field Pinch Reactors (CRFPR): Preliminary Engineering Considerations," Los Alamos National Laboratory report LA-10200-MS (August 1984).
3. A. Opdenaker and P. Anbro (eds.), "Tokamak Power Systems Studies Report for Fiscal Year 1985," USDOE report (to be published, 1986).
4. Y-K. M. Peng, "Spherical Torus, Compact Fusion at Low Field," Oak Ridge National Laboratory report ORNL/FEDC-84/7 (January 1985).
5. Y-K. M. Peng, D. J. Stricker, S. K. Borowski, W. R. Hamilton, R. L. Reid, J. R. Haines, et al., "Spherical Torus: An Approach to Compact Fusion at Low Field - Initial Ignition Assessment," 6th Top. Mtg. on the Technol. of Fus. En., San Francisco (March 3-7, 1985), Fus. Tech. 8(1) Part 2(a), 338 (July 1985).
6. Y-K. M. Peng and D. J. Strickler, "Features of Spherical Torus Plasmas," Oak Ridge National Laboratory report ORNL/FEDC-85/6 (December 1985).
7. C. C. Baker, M. A. Abdou, R. M. Arons, A. E. Bolon, C. D. Boley, J. N. Brooks, et al., "STARFIRE - A Commercial Tokamak Fusion Power Plant Study," Argonne National Laboratory report ANL/FPP-80-1 (September 1980).
8. C. Copenhaver, R. A. Krakowski, N. M. Schnurr, R. L. Miller, C. G. Bathke, R. L. Hagenson, et al., "Compact Reversed-Field Pinch Reactors (CRFPR): Fusion-Power-Core Integration Study," Los Alamos National Laboratory report LA-10500-MS (August 1985).
9. F. Troyon and R. Gruber, "A Semi-Empirical Scaling Law for the β -Limit in Tokamaks," Phys. Letters 110A (1), 29 (1985) [also Ecole Polytechnique Fédérale de Lausanne report LRP-239/84 (January 1984)].
10. J. D. Callen and R. A. Dory, "Magnetohydrodynamic Equilibria in Sharply Curved Axisymmetric Devices," Phys. Fluids 15, 1523 (August 1972).
11. D. J. Strickler, J. B. Miller, K. E. Rothe, and Y-K. M. Peng, "Equilibrium Modeling of the TFCX Poloidal Field Coil System," Oak Ridge National Laboratory report ORNL/FEDC-83/10 (April 1984).
12. H. M. Attaya and M. E. Sawan, "NEWLIT-A General Code for Neutron Wall Loading Distribution in Toroidal Reactor," Fus. Tech. 8(1) Part 2(A), 609 (July 1985).
13. Los Alamos Monte Carlo Group, "MCNP - a General Monte Carlo Code for Neutron and Photon Transport, Version 3A," Los Alamos National Laboratory report LA-7396-M, Revised (April 1981).

14. P. J. Harbour and J. G. Morgan, "Models and Codes for the Plasma Edge Region," UKAEA Culham Laboratory report CLM-R234 (December 1982).
15. J. Sheffield, R. A. Dory, S. M. Cohn, J. G. Delene, L. Parsly, D. E. T. F. Ashby, and W. T. Reiersen, "Cost Assessment of a Generic Magnetic Fusion Reactor," *Fus. Tech.* 9, 199 (1986) [also Oak Ridge National Laboratory report ORNL/TM-9311 (March 1986)].
16. R. W. Conn (Chairman and editor), "Panel X Report to the Magnetic Fusion Advisory Committee," University of California at Los Angeles (May 8, 1985).
17. R. K. Linford, "New Directions in Fusion Machines: Report on the MFAC Panel X on High-Power-Density Options," IEEE 11th Symp. on Fus. Engr., Austin, TX (November 18-22, 1985) (to be published, 1986).
18. K. Yamazaki, T. Amano, H. Naitou, Y. Hamada, and M. Azumi, "Ballooning Beta Limits of Dee- and Bean-Shaped Tokamaks," *Nucl. Fus.* 25, 1543 (1985) [also Institute of Plasma Physics, Nagoya University report IPPJ-713 (February 1985)].
19. K. F. Schoenberg, C. J. Buchenauer, R. S. Massey, J. G. Melton, R. W. Moses, Jr., R. A. Nebel, and J. A. Phillips, "F- Θ Pumping and Field Modulation Experiments on a Reversed Field Pinch Discharge," *Phys. Fluids* 27 (3), 548 (1984).
20. B. G. Logan, C. D. Henning, G. A. Carlson, R. W. Werner, D. E. Baldwin, W. L. Barr, *et al.*, "MARS Mirror Advanced Reactor Study Final Report," Lawrence Livermore National Laboratory report UCRL-53480 (July 1984).
21. L. J. Perkins, B. G. Logan, J. M. Doggett, W. D. Nelson, D. C. Lousteau, G. L. Kulcinski, J. D. Gordon, *et al.*, "MINIMARS: An Attractive Small Tandem Mirror Fusion Reactor," IEEE 11th Symp. on Fus. Engr., Austin, TX (November 18-22, 1985) (to be published, 1986).
22. R. A. Krakowski, R. L. Miller, and J. G. Delene, "Directions for Improved Fusion Reactors," Proc. 4th IAEA Technical Comm. and Workshop on Fusion Reactor Design and Technology, Yalta, USSR (May 26 - June 6, 1986) [also Los Alamos National Laboratory document LA-UR-86-13C6].
23. J. B. Taylor, "Relaxation of Toroidal Plasma and Generation of Reversed Magnetic Fields," *Phys. Rev. Lett.* 33, 1139 (1979).
24. R. A. Scardovelli, R. A. Nebel, K. A. Werley, and G. H. Miley, "F- Θ Pumping and Applications to ZT-40M," *Bull. Amer. Phys. Soc.* 30, 9, paper 2R18, p. 1401 (October 1985).
25. P. C. Efthimion, M. Bell, W. R. Blanchard, N. Bretz, J. L. Cecchi, J. Coonrod, *et al.*, "Initial Confinement Studies of Ohmically Heated Plasmas in the Tokamak Fusion Test Reactor," Princeton Plasma Physics Laboratory report PPPL-2078 (June 1984).
26. F. Wagner and K. Lackner, "Divertor Tokamak Experiments," Proc. NATO Advanced Study Institutes Program Course on the Physics of Plasma-Wall Interactions in Controlled Fusion, Val-Morin, Quebec, Canada (1985).

27. R. G. Mills, (ed.), "A Fusion Power Plant," Princeton Plasma Physics Laboratory report MATT-1050 (August 1974).
28. United Engineers and Constructors, Inc., "1000-MWe Central Station Power Plants Investment Cost Study," U.S.A.E.C. report WASH-1230 (June 1972).
29. S. M. Kaye and R. J. Goldston, "Global Energy Confinement Scaling for Neutral-Beam-Heated Tokamaks," Nucl. Fus. 25, 65 (1985).
30. C. Mercier and H. Luc, "The MHD Approach to the Problem of Plasma Confinement in Closed Magnetic Configurations," EUR 5127e, Commission of the European Communities, Luxembourg (1974).
31. J. W. Connor, R. C. Grimm, R. J. Hastie, and P. M. Keeping, "The Conductivity of a Toroidal Plasma," Nucl. Fus. 13, 211 (1973).
32. M. K. Bevir and J. W. Gray, "Relaxation, Flux Consumption and Quasi Steady State Pinches," Proc. RFP Theory Workshop (April 28 - May 2, 1980), Los Alamos National Laboratory report LA-8944-C, p. 186 (January 1982).
33. K. F. Schoenberg, R. F. Gribble, and J. A. Phillips, "Zero-Dimensional Simulations of Reversed-Field Pinch Experiments," Nucl. Fus. 22, 1433 (1982).
34. F. Wagner, M. Keilhacker, and the ASDEX and NI Teams, "Importance of the Divertor Configuration for Attaining the H-Regime in ASDEX," J. Nucl. Mater. 121, 103 (1984).
35. S. M. Kaye, M. G. Bell, K. Bol, D. Boyd, K. Bran, D. Buchenauer, et al., "Attainment of High Confinement in Neutral Beam Heated Divertor Discharges in the PDX Tokamak," J. Nucl. Mater. 121, 115 (1984).
36. K. Yamazaki and W. T. Reiersen, "'Crescent' - Shaped Tokamak for Compact Ignition," Nagoya University report IPPJ-754 (December 1985).
37. R. Gruber, F. Troyon, D. Berger, L. C. Bernard, S. Rousett, R. Schreiber, et al., "ERATO Stability Code," Comp. Phys. Comm. 21, 323 (1985).
38. F. Troyon, R. Gruber, H. Saurenmann, S. Semenzato, S. Succi, "MHD-Limits to Plasma Confinement," Plasma Phys. and Contr. Fus. 26, 209 (1984).
39. J. A. Wesson and A. Sykes, "Tokamak Beta Limit," Nucl. Fus. 25, 85 (1985).
40. L. C. Bernard, F. J. Helton, R. W. Moore, T. N. Todd, "MHD Beta Limits: Scaling Laws and Comparison with Doublet III Data," Nucl. Fus. 23, 1475 (1983).
41. T. Tuda, M. Azumi, K. Itoh, G. Kurita, T. Takeda, T. Takizuka, et al., "Accessible Beta Value of Tokamaks," IAEA 10th Int. Conf., on Plasma Phys. and Contr. Nucl. Fus. Res., London 2, 173 (1984).
42. C. M. Bishop, P. Kirby, J. W. Connor, R. J. Hastie, and J. B. Taylor, "Ideal MHD Ballooning Stability in the Vicinity of a Separatrix," Nucl. Fus. 24, 1579 (1984).

43. M. Okabayashi, P. Beiersdorfer, K. Bol, D. Buchenauer, M. S. Chance, P. Couture, et al., "Studies of Bean-Shaped Tokamaks and Beta Limits for Reactor Design," IAEA 10th Int. Conf. on Plasma Phys. and Contr. Nucl. Fus. Res., London, 1, 229 (1984).
44. F. L. Hinton, M. S. Chu, R. R. Dominguez, R. W. Harvey, L. L. Lao, J. K. Lee, et al., "Physics of the H-Mode," IAEA 10th Int. Conf. on Plasma Phys. and Contr. Nucl. Fus. Res., London, 2, 3 (1984).
45. C. Copenhaver, "Ideal Magnetohydrodynamic Stability of Tokamak Plasmas," Phys. Fluids 23, 624 (1980).
46. B. Blackwell, C. L. Fiore, R. Gandy, A. Gondhaleker, R. S. Granetz, M. Greenwald, "Energy and Impurity Transport in the Alcator C Tokamak," IAEA 9th Int. Conf. on Plasma Phys. and Contr. Nucl. Fus. Res., Baltimore, II, 27 (1983).
47. S. M. Kaye and R. J. Goldston, "Global Energy Confinement Scaling for Neutral-Beam-Heated Tokamaks," Nucl. Fus. 25, 65 (1985).
48. M. Murakami, J. D. Callen, L. A. Berry, "Some Observations on Maximum Densities in Tokamak Experiments," Nucl. Fus. 25, 347 (1985).
49. D. A. Ehst, J. N. Brooks, Y. Cha, K. Evans, Jr., A. Hassaein, S. Kim, et al., "Tokamak Burn Cycle Study," Argonne National Laboratory report FPP/TM-178, pp. 2-7 (November 1983).
50. J. B. Taylor, "Relaxation and Magnetic Reconnection in Plasma," UKAEA Culham Laboratory report CLM-P765 (December 1985) (submitted to Revs. Mod. Phys.).
51. D. D. Schnack, E. J. Caramana, and R. A. Nebel, "Three-Dimensional Magnetohydrodynamic Studies of the Reversed-Field Pinch," Phys. Fluids 28, 321 (1985).
52. E. J. Caramana, R. A. Nebel, and D. D. Schnack, "Nonlinear, Single-Helicity Magnetic Reconnection in the Reversed-Field Pinch," Phys. Fluids 26, 1305 (1983).
53. H. P. Furth, "Nonideal Magnetohydrodynamic Instabilities and Toroidal Magnetic Confinement," Phys. Fluids 28, 1595 (1985).
54. D. Robinson, "High- β Diffuse Pinch Configurations," Plas. Phys. 13, 439 (1971).
55. R. Gross, Fusion Energy, p. 118, Wiley and Sons, NY, 1984.
56. E. A. Lazarus, S. E. Attenberger, L. R. Baylor, S. K. Burowski, R. L. Brown, B. A. Carreras, et al., "Proposal for the Spherical Torus Experiment," Oak Ridge National Laboratory (August 1985).
57. N. J. Fisch and C. F. F. Karney, "Current Generation with Low Frequency Waves," Phys. Fluids 24, 27 (1981).

58. Yan Ming Li, "Alfvén Waves in the Presence of Alpha Particles - Stability, Heating, and Current Drive," Doctoral Dissertation, University of Texas at Austin, 1984.
59. R. A. Demirkhanov, A. G. Kirov, L. F. Ruchko, and A. V. Sukachev, "Generation of Steady-State Currents and Transfer Control in a Toroidal Magnetic Trap due to Alfvén Waves," JETP Letters 33, 28 (1981).
60. T. Okazaki, K. Maki, T. Kobayashi, M. Sugihara, and N. Fujisawa, "A Steady-State Tokamak Reactor Using the Compressional Alfvén Wave," Nucl. Fus. 24, 1451 (1984).
61. S. M. Mahajan and D. W. Ross, "Spectrum of Compressional Alfvén Waves," Phys. Fluids 26, 2561 (1983).
62. F. C. Jones, S. Bernabei, T. K. Chu, W. M. Hooke, E. B. Meservey, R. W. Motley, et al., "Current Ramp-up by Lower Hybrid Waves in the PLT Tokamak," Princeton Plasma Physics Laboratory report PPPL-2200 (1985).
63. D. A. Ehst, "Relativistic Electron Beam Current Drive," in "FED-A, An Advanced Performance FED Based on Low Safety Factor and Current Drive," Y.-K. M. Peng, P. H. Rutherford, et al., Oak Ridge National Laboratory report ORNL/FEDC-83/1, Sec. 4.5. (August 1983).
64. A. Mohri, K. Narihara, Y. Tomita, T. Tsuzuki, M. Hasegawa, and K. Ikuta, "Experiment on Plasma Confinement by Long-Lived Intense Relativistic-Electron-Beam Ring," IAEA 8th Int. Conf. on Plasma Phys. and Contr. Nucl. Fus. Res., Brussels, I, 511 (1980).
65. H. C. Howe, "Physics Considerations FED Limiter," Oak Ridge National Laboratory report ORNL/TM-703 (1982).
66. R. L. Hagenson and R. A. Krakowski, "Compact Reversed-Field Pinch Reactors (CRFPR): Sensitivity Study and Design-Point Determination," Los Alamos National Laboratory report LA-9389-MS (July 1982).
67. D. A. Ehst, K. Evans, Jr., W. M. Stacey, Jr., "The Influence of Physics Parameters on Tokamak Reactor Design," Nucl. Tech. 43, 28 (April 1979).
68. N. Jarmie, R. E. Brown, and R. A. Hardenkopf, "Fusion-energy Reaction $^2\text{H}(t, \alpha)n$ from $E_t = 12.5$ to 117 keV," Phys. Rev. C 29, 2031 (June 1984) and relevant Erratum in Phys. Rev. C 33, 385 (January 1986).
69. C. A. Flanagan, D. Steiner, G. E. Smith, et al., "Initial Trade and Design Studies for the Fusion Engineering Device," Oak Ridge National Laboratory report ORNL/TM-7777 (1981).
70. J. Kessner and R. W. Conn, "Space-Dependent Effects on the Lawson and Ignition Conditions and Thermal Equilibria in Tokamaks," Nucl. Fus. 16, 397 (1976).
71. D. Steiner, R. C. Block, and B. K. Malaviya, "The Integrated-Blanket-Coil Concept Applied to the Poloidal Field and Blanket Systems of a Tokamak Reactor," Fus. Tech. 7, 66 (January 1985).

72. Chemical Rubber Co., Handbook of Chemistry and Physics, 55th Edition, 1974-1975.
73. V. A. Maroni, E. J. Cairns, and F. A. Cafasso, "A Review of the Chemical, Physical, and Thermal Properties of Lithium That are Related to its Use in Fusion Reactors," Argonne National Laboratory report ANL-8001 (March 1973).
74. U.S. Energy Information Administration, "Monthly Energy Review," (August 1985).
75. South Central Region, The Handy - Whitman Index of Public Utility Construction Costs, Whitman, Requardt and Associates, 1304 Saint Paul Street, Baltimore, MD, 1984.
76. R. H. Whitley, G. R. Lutz, S. A. Freije, D. H. Berwald, J. D. Gordon, "An Assessment of Multiplexed Deployment of Advanced Commercial Tokamaks," ANS 7th Top. Mtg. on Tech. of Fus. En., Reno, Nevada (June 15-19, 1986) (to be published in Fus. Tech., 1986).
77. D. Garber (ed.), "ENDF/B-IV Summary Documentation," Brookhaven National Laboratory report BNL-17541 (ENDF-201) (October 1975).
78. J. W. Davidson and M. E. Battat, "Blanket Activation and Afterheat for the Compact Reversed-Field Pinch Reactor," IEEE 11th Symp. on Fus. Engr., Austin, TX (November 18-22, 1985) (to be published, 1986) [also Los Alamos National Laboratory document LA-UR-85-4017].
79. R. E. MacFarlane, "TRANSX-CTR: A Code for Interfacing MATXS Cross-Section Libraries to Nuclear Transport Codes for Fusion Systems Analysis," Los Alamos National Laboratory report LA-9863-MS (1984).
80. M. A. Hoffman and G. A. Carlson, "Calculating Techniques for Estimating Pressure Losses for Conducting Fluid Flows in Magnetic Fields," Lawrence Livermore National Laboratory report UCRL-51010 (1971).
81. P. A. Roth and H. Chow, "Analysis of a Liquid-Metal Cooled Transient Using ATHENA," 6th ANS Top. Mtg. on the Technol. of Fus. En., San Francisco, CA (March 3-7, 1985), Fus. Tech. 8(1) Part (2A), 581 (July 1985).
82. A. H. Hadid and M. S. Tillack, "Entry Length Effects in Liquid-Metal Fusion Blankets," ANS 7th Top. Mtg. on Tech. of Fus. En., Reno, Nevada (June 15-19, 1986) (to be published in Fus. Tech., 1986).
83. S. V. Patankar and D. B. Spalding, Heat and Mass Transfer in Boundary Layers, Intertext Books, London, 1970.
84. M. A. Abdou and G. D. Morgan, (eds.), "Blanket Comparison and Selection Study, Interim Report," Argonne National Laboratory report ANL/FPP/83-1 (October 1983).
85. E. T. Puzio, PDX TF Coil Assembly Drawings, Princeton Plasma Physics Laboratory (1975).

86. H. A. Murray, "High Current Electrical Joint Program for ISP," IEEE 11th Symp. on Fus. Engr., Austin, TX (November 18-22, 1985).
87. S. L. Milora, "Review of Pellet Fueling," J. Fusion Energy 1, 15 (1981).
88. P. B. Parks and R. J. Turnbull, "Effect of Transonic Flow in the Ablation Cloud on the Lifetime of a Solid Hydrogen Pellet in a Plasma," Phys. Fluids 21, 1735-1741 (1978).
89. R. P. Gilliard and K. Kim, "Significance of Magnetic Shielding in the Ablation of a Solid Hydrogen Pellet in a Plasma," IEEE Transaction on Plasma Science PS-8, 477-484 (December 1980).
90. W. A. Houlberg, M. A. Iskra, H. C. Howe, and S. E. Attenberger, "Pellet-A Computer Routine for Modeling Pellet Fueling in Tokamak Plasma," Oak Ridge National Laboratory report ORNL/TM-6549 (January 1979).
91. B. Badger, K. Audenaerde, J. B. Beyer, D. Braun, J. D. Callen, G. A. Emmert, et al., "WITAMIR-I: A University of Wisconsin Tandem Mirror Reactor Design," University of Wisconsin report UWFDM-400 (September 1980).
92. "Equipment Specification - Intermediate A Prototype Steam Generator," Westinghouse Electric Corporation Breeder Reactor Component Project (June 1980).
93. J. H. Crowley, and R. E. Allan, "Phase VII Update (1984) Report for the Energy Economic Data Base Program: EEDB-VII," Department of Energy report DOE/NE-0051/2 (August 1985).
94. B. Badger, M. A. Abdou, R. W. Boom, R. G. Brown, T. E. Chang, R. W. Conn, et al., "UWMAK-I, A Wisconsin Toroidal Fusion Reactor Design," University of Wisconsin report UWFDM-68 (March 1974).
95. R. L. Hagenson and R. A. Krakowski, "Steady-State Spheromak Reactor Studies," Fus. Technol. 8(1), 1606 (1985).
96. A. Walker, "Design of the PWR for Sizewell 'B'," The Nuclear Engineer 24, 6, 176 (1984) [also, UK Central Electricity Generating Board Brochure, "A Technical Outline of Sizewell 'B': The British Pressurized Water Reactor," (1983)].
97. R. L. Miller, R. A. Krakowski, and R. L. Hagenson, "Fusion Reactor Options and Alternatives for the Reversed-Field Pinch," ANS 7th Top. Mtg. on Tech. of Fus. En., Reno, Nevada (June 15-19, 1986) (to be published in Fus. Tech., 1986).
98. R. Hancox, R. A. Krakowski, and W. R. Spears, "The Reversed-Field Pinch Reactor," Nucl. Eng. and Design 63, 251 (1986).
99. R. L. Miller, C. G. Bathke, R. A. Krakowski, F. M. Heck, L. Green, J. S. Karbowski, et al., "The Modular Stellarator Reactor: A Fusion Power Plant," Los Alamos National Laboratory report LA-9737-MS (July 1983).

100. C. G. Bathke, D. J. Dudziak, R. A. Krakowski, W. b. Ard, D. A. Bowers, J. W. Davis, et al., "ELMO Bumpy Torus Reactor and Power Plant Conceptual Design Study," Los Alamos National Laboratory report LA-8882-MS (August 1981).
101. B. Badger, I. N. Sviatoslavsky, S. W. VanSciver, G. L. Kulcinski, G. A. Emmert, D. T. Anderson, et al., "UWTOR-M: A Conceptual Modular Stellarator Power Reactor," University of Wisconsin report UWFD-550 (October 1982).

LIST OF NOMENCLATURE

a	Plasma radius (half-width) in equatorial plane (m)
$a_{pi,Ri}$	PbLi poloidal, radial coolant-channel half-width ($i = 1,2,3 \dots$) (m)
a_w	Wall or shell effective radius (m)
A	Area (m^2), aspect ratio (R_T/a), or vector potential ($\nabla \times \vec{A} = \vec{B}$)
A^+	Empirical constant in Eq. (4.4.-11)
ATR	Advanced tokamak reactor
A_{DIV}	Area of divertor-chamber wall surfaces (m^2)
A_p	Area for PbLi flow or plasma cross-sectional area (m^2)
A_{FW}	First-wall area (m^2)
A_{FW}^{IN}	Inboard first-wall area (m^2)
AFDC	Allowance for Funds During Construction
A_S	Surface area of plasma (m^2)
b	Plasma minor vertical radius (m)
b_1	Constant in Nagoya beta scaling, Eq. (2.2.-7)
B	Magnetic-field strength (T) or "Blanket"
B_c	Field at coil (T)
B_{CM}	Maximum field at coil (T)
B_R	Radial component of magnetic field (T)
B_Z	Axial field (T)
B_ϕ	Toroidal magnetic field (T)
B_θ	Poloidal magnetic field (T)
$B_{\phi,\theta}$	Flux surface averaged field (T)
$\langle B_\phi \rangle$	Average toroidal magnetic field in plasma (T)
$B_{\phi V}(0)$	On-axis vacuum toroidal field
BOP	Balance of Plant
c_p	Specific heat (J/kg K)
C	"Coil" or $1.5nk_B V_p$
C_{AC}	Annual Capital-Cost Charge (\$/yr)
C_F	Annual fuel charge (\$/yr)
C_{IJ}	Account IJ Cost
$C_{O\&M}$	Annual Operation-Cost Charge (\$/yr)
C_{SCR}	Annual scheduled component replacement charge (\$/yr)
COE	Cost of Electricity (mills/kWeh)

CRFPR	Compact Reversed-Field Pinch Reactor
CSR	Compact Spheromak Reactor
d	Pellet penetration distance into plasma (m)
d _i	Inside tube diameter (m)
dpa	Displacement per atom
D	Scrapeoff particle diffusivity (m ² /s)
DFC	Divertor-field coil
E	Escalation rate (yr ⁻¹)
EBTR	ELMO Bumpy Torus Reactor
ECRH	Electron cyclotron resonance heating
EDC	Escalation During Construction
EFC	Equilibrium-field coil
ET	Elongated torus (tokamak)
f _c	Fraction of alpha-particle energy
f _{αi,ae}	Partition of trapped alpha-particle energy between ions, electrons
f _{AUX}	Auxiliary power fraction, P _{AUX} /P _{ET}
f _{CR}	TFC outboard-leg radial fattening parameter
f _{Cf}	TFC outboard-leg toroidal fattening parameter
f _D	Fraction of plasma transport power delivered to divertor
f _F	Fanning friction factor
f _{IO}	Split of energy flow between inboard and outboard plasma surface
f _{RAD}	Fraction of plasma energy lost by radiation
f _w	Fraction of thermal power deposited in water coolant
f _β	Factor by which beta limit is reduced
F	Reversal parameter, B _{φR} / $\langle B_\phi \rangle$ or $\bar{B}_\phi(a)/\langle B_\phi \rangle$
FPC	Fusion Power Core
FPY	Full Power Year
FW	First wall
FWCD	Fast-wave current drive
G&A	General and Administrative Costs
g	Fraction an injected DT pellet is of total DT plasma inventory
g _i	Plasma profile factor (i = BR, DT, OHM)
H	Hartmann number, aB(σ _p /η) ^{1/2}
H _T	Hartmann number at laminar/turbulent transition
IBC	Integrated Blanket/Coil
I _c	Coil current (MA)

$I_{c\theta}$	Current in TFC (MA)
IDP	Interim Design Point
I_N	Troyon parameter, $\mu_0 I_\phi / a B_\phi V(0)$
I_ϕ	Plasma toroidal current (MA)
I_{PFC}	Current in poloidal-field coils (DFC + EFC) (MA)
I_{Qw}	Heat flux at first wall (MW/m ²)
I_{RF}	RF-driver current (MA)
I_w	Average neutron first-wall loading (MW/m ²)
$\hat{I}_w$	Peak neutron first-wall loading (MW/m ²)
$I_{w\tau}$	First-wall/blanket radiation fluence lifetime (MWyr/m ²)
I&C	Instrumentation and Control
ID	Inside diameter (m)
IDC	Interest During Construction
j	Current density (MA/m ²)
j_c	Coil current density (MA/m ²)
j_{BS}	Bootstrap current density (MA/m ²)
j_θ	Plasma poloidal current density (MA/m ²)
j_ϕ	Plasma toroidal current density (MA/m ²)
k_B	Boltzmann constant [$1.602(10)^{-16}$ J/keV]
k_{eff}	Effective thermal conductivity of fluid including turbulent contribution (W/m K)
$k_{w,p}$	Thermal conductivity of water, PbLi (W/m K)
$k_{\perp, \parallel}$	Perpendicular, parallel wave number (1/m)
K	Magnetic helicity, $\int \vec{A} \cdot \vec{B} dV_p$
KG	Kaye-Goldston (scaling)
l	Poloidal mode number or coolant-tube length (m)
L	Tube connection length (m) or "limiter"
L_0	Toroidal vacuum inductance, $\mu_0 R_T / 2$ (H)
L_p	Plasma inductance (H)
LH	Lower hybrid (wave)
m	Poloidal mode number
$m_{i,e}$	Ion, electron mass (kg)
$\dot{m}_i$	Mass flow rate of water, PbLi for the i^{th} channel (kg/s)
$\dot{m}_w / N_T$	Mass flow rate of water for a single tube (kg/s)
$\dot{m}_{w,p}$	Total mass flow rate of water, PbLi (coolant kg/s)
M_p	Mass of DT pellet (kg)

M_N	FW/B/S neutron energy multiplication
M_N^{PN}	Recoverable nuclear heating (MW)
M_{FPC}	FPC mass (tonne)
M_{PbLi}	Mass of PbLi (tonne)
MHD	Magnetohydrodynamic
MPD	Mass power density (kWe/tonne)
MSR	Modular Stellarator Reactor
n	Toroidal mode number, scrapeoff or average plasma density (m^{-3}) exponent in velocity profile
n_i	Average plasma ion density (m^{-3})
n_o	Separatrix density or initial or final density (m^{-3})
N	Number of divertors or toroidal sectors, or plasma line density (m^{-1})
N_c	Number of coolant channels
N_{TF}	Number of toroidal-field coils
NA	Not applicable or Neo-Alcator (scaling)
NC	Neoclassical
N_T	Number of coolant tubes in first wall or second wall
N_{TF}	Number of toroidal field coils
OFCD	Oscillating-field current drive
OHC	Ohmic-Heating Coil
OD	Outside diameter (m)
p	Plasma pressure (Pa)
P_f	Plant availability factor
P	Power (MW), or pressure (MPa), or plant construction period (yr)
P_α	Alpha-particle power (MW)
P_Q	Plasma ohmic power (MW)
P_{AUX}	Auxiliary plant power (MW)
$P_{AW, LH}^{IND}$	Inductive power to plasma (MW)
P_{BLK}	Total blanket nuclear heating (MW)
P_c	Recirculating power (MW)
P_c^Q	Ohmic power recirculated to FPC (MW)
P_{CUR}	Power delivered to plasma by current drive (MW)
P_{DFC}	Divertor coil heating (MW)

P_{DFC}^N	Divertor coil nuclear heating (MW)
P_{DFC}^Q	Divertor coil ohmic heating (MW)
P_{DIV}	Power to divertor (MW)
P_{DIV}^N	Divertor nuclear heating (MW)
$P_{e \rightarrow i}$	Electron/ion equipartition power (MW)
P_E	Net electrical power output (MWe)
P_{ECRH}	Electron cyclotron resonance heating power for startup (MW)
P_{EFC}	Equilibrium field coil heating (MW)
P_{EFC}^N	Equilibrium field coil nuclear heating (MW)
P_{EFC}^Q	Equilibrium field ohmic heating (MW)
P_{ET}	Gross electrical power (MWe)
P_F	Fusion power (MW)
P_{FW}	Total power to first wall (MW)
P_{FW}^N	Total first-wall nuclear heating (MW)
P_{max}	Maximum coolant pressure (MPa)
P_N	Fusion-neutron power (MW)
Pr	Prandtl number
Pr_{eff}	Effective Prandtl number
P_{RAD}	Plasma radiated power (MW)
P_{RF}	RF power to plasma by current drive (MW)
$\tilde{P}_{RF}$	Peak RF power (MW)
P'_{RF}	RF power in Alfvén (MW) or lower-hybrid waves (MW)
P_{SLD}^{NI}	Inboard shield nuclear heating (MW)
P_{SLD}^{NO}	Outboard shield nuclear heating (MW)
P_{TFC}	Total toroidal-field-coil heating (MW)
P_{TFC}^{NI}	Inboard toroidal-field-coil nuclear heating (MW)

P_{TFC}^{QH}	Inboard toroidal-field-coil ohmic heating (MW)
P_{TH}	Total recoverable thermal power (MWt)
P_{THp}	Total thermal power to PbLi coolant, excluding pump power (MW)
P_{THw}	Total power to water-cooled first-wall/second-wall/limiter systems (MW)
P_{TR}	Plasma transport power (MW)
PbLi	Eutectic mixture of lead and lithium ($Pb_{83}Li_{17}$)
PCASS	Primary Candidate Alloy, Stainless Steel(316)
EFC	Poloidal-Field Coil
PWR	Pressurized-Water Reactor
q	Plasma safety factor, $(B_\phi/B_\theta)/(r/R_T)$
q_a	Plasma safety factor at plasma edge
q_D	Design heat flux (MW/m^2)
q_{DIV}	Divertor channel heat flux (MW/m^2)
q_w	First wall heat flux (MW/m^2)
Q_E	Engineering Q-value, $P_{ET}/P_c = 1/\epsilon$
Q_p	Plasma Q-value, P_F/P_Q
Q_{NY}	Energy generation per unit volume (MW/m^3)
r	Minor radius (m)
r_p	Circularized plasma minor radius (m)
r_s	FPC system minor radius, $r_w + \Delta b + \delta_{c\phi} + \delta_{c\theta}$ (m)
r_w	First-wall minor radius (m)
R	Major radius
R_{DT}	Effective divertor-slot reflection coefficient
R_p	Plasma resistance (ohm)
R_T	Major toroidal radius of plasma (m)
$R_{1,2}$	Major radial position of EFC or DFC (m)
RCTR	Resistive-coil tokamak reactor
Re	Reynolds number, $\rho Va/\eta$
Re_T	Transition Reynolds number
RF	Radio frequency
RFP	Reversed-Field Pinch
R_{Le}^*	Edge-plasma particle recycle coefficient
R_{Ls}^*	Scrapeoff particle recycle coefficient
R&D	Research and Development

RPE	Reactor Plant Equipment
S	Shield
$S(\epsilon)$	Neoclassical resistivity enhancement
SI	International System of Units (mks)
SSR	Second stability region
ST	Spherical torus (tokamak)
t	Time (s)
t_p	Pellet lifetime in plasma(s)
t_w	Thickness of channel walls in the blanket (m)
T	Temperature of coolant or average plasma ($^{\circ}\text{C}$, K, keV)
T_E	Environment or heat-rejection temperature for thermal cycle (K)
$T_{i,e}$	Ion, electron temperature (keV)
$T_{\max}$	Maximum structural temperature
T_o	Separatrix temperature (keV)
T_{pi}	Inlet PbLi bulk temperature (K)
T_{po}	Outlet PbLi bulk temperature (K)
T_s	Surface temperature (K)
$T_{si,o}$	Inside, outside tube-wall temperature or maximum local water temperature (K)
T_w	Plasma temperature at first wall (keV)
T_{wi}	Inlet water bulk temperature (K)
T_{wo}	Outlet water bulk temperature (K)
TBC	Total base cost
TBD	To be determined
TBR	Tritium breeding ratio
TDC	Total direct cost
TFC	Toroidal-field coil
TPSS	Tokamak Power Systems Studies
u	Pellet injection velocity (m/s)
UBC	Unit base cost (\$/kWe)
UDC	Unit direct cost (\$/kWe)
UTC	Unit total cost (\$/kWe)
v_A	Alfvén velocity (m/s)
$v_{THi,e}$	Ion, electron thermal velocity (m/s)
$v_{w,p}$	Local water or PbLi coolant velocity (m/s)
$V_{w,p}$	Bulk water or PbLi coolant velocity (m/s)

V	Bulk fluid velocity (m/s)
V_c	Volume of coils (m ³)
V_p	Plasma volume (m ³)
V_{BL}	Volume of blanket (m ³)
V_{FPC}	FPC volume (m ³)
V_{max}	Maximum coolant flow (m/s)
V_{RB}	Volume of reactor building (m ³)
V_{SHD}	Volume of shield (m ³)
V_{STR}	Volume of FPC structure (m ³)
V_{VAC}	Volume of vacuum system (m ³)
$V(\psi)$	Volume enclosed by flux surface ψ
v_α	Alpha-particle velocity (m/s)
$V_{\theta, \phi}$	Poloidal, toroidal (loop) voltage at plasma (V)
w	Coolant-channel width = $2a$ (m)
$w_{\theta i}$	Width of i^{th} poloidal PbLi-coolant channel (m)
W_M	Magnetic energy (J)
w_{Ri}	Width of i^{th} radial PbLi-coolant channel (m)
x	Radial scrapeoff distance, axial distance along tube or channel (m), ratio of plasma to first-wall radius
$x(\psi)$	Flux function $(\psi - \psi_0)/(\psi_L - \psi_0)$
y	Distance from blanket coolant-channel wall (m)
z	Toroidal distance for limiter (m)
$z_{1,2}$	Distance from equatorial plane of EFC or DFC (m)
Z_{eff}	Average atomic number of plasma
Z_i	Charge of i^{th} species
α	Thermal expansion coefficient (1/K) or ratio of heat and particle diffusivities in edge-plasma scrapeoff, χ/D
β	Volumetric thermal expansion coefficient (1/K) or normalized plasma pressure (toroidal beta)
β_c	Critical plasma beta above which confinement is degraded
β_θ	Poloidal plasma beta
γ	Sheath potential factor
δ	Coolant-tube wall thickness (m) or plasma triangularity
δ_{cp}	TFC central column radius in the equatorial plane
δ_{CR}	TFC outboard-leg radial thickness

δ_{CT}	TFC outboard-let toroidal thickness
$\delta_{c\phi}$	Toroidal-field-coil thickness (m)
$\delta_{c\theta}$	Poloidal-field-coil thickness (m)
δ_c	$= \delta_{c\phi} + \delta_{c\theta}$ nominal coil thickness (m)
Δb	Blanket thickness (m)
ΔP	Coolant pressure drop (Pa)
ΔP_G	Gravitational pressure head (MPa)
ΔP_H	Hydrodynamic pressure drop (Pa)
ΔP_M	Magnetic pressure drop (Pa)
Δs	Shield thickness (m)
ΔT_p	PbLi temperature difference, $T_{po} - T_{pi}$ (K)
ΔT_{SAT}	Saturation temperature of water coolant (K)
ϵ	Emissivity of first wall or limiter, total recirculating power fraction (P_c/P_{ET}), or inverse aspect ratio (a/R_T)
ϵ_{pump}	Pump power fraction
η	Viscosity (kg/m s), efficiency, ratio of density and temperature e-folding lengths in scrapeoff (λ_n/λ_T), coil resistivity
η_{eff}	Effective viscosity including turbulence contribution (kg/m s)
η_D	Divertor efficiency
η_{GEN}	Electrical generator efficiency
η_I	Ideal thermal-cycle efficiency
η_p	Plant efficiency, $\eta_{TH}(1 - \epsilon) = P_E/P_{TH}$
η_{pump}	Efficiency of pumps
η_{TH}	Actual cycle efficiency $= 0.75 \eta_I$
η_{TH}^*	Thermal-conversion efficiency (P_{ET}/P_{TH}) $= \eta_{GEN}\eta_{TH}$
η_P^*	$\eta_{TH} (1 - \epsilon_{pump})$
θ	Poloidal direction
Θ	Pinch parameter, $B_\theta/\langle B_\phi \rangle$ or $\bar{B}_\theta(a)/\langle B_\phi \rangle$
κ	Plasma elongation, b/a
λ_c	Coil conductor filling fraction
$\lambda_{T,n}$	Temperature or density e-folding distances in scrapeoff (m)
μ	Ratio of parallel current density to magnetic field (m^{-1}) or viscosity (kg/m s)
μ_0	$4\pi(10)^{-7}$ H/m
ν	Poisson's ratio or exponent on current dependence of τ_E (PHYS)
ρ	Mass density (kg/m ³)
σ	Stress (Pa) or Stefan-Boltzmann constant ($5.6703(10)^{-8}$ W/m ² K ⁴)

σ_D	Design stress (Pa)
σ_p	Electrical conductivity of PbLi (1/ohm m)
σ_Y	Yield stress (MPa)
σ_{VM}	von Mises stress (MPa)
σ_w	Electrical conductivity of wall (1/ohm m)
τ	Shear stress (Pa)
τ_{pi}	Plasma particle confinement time (s)
$\tau_{ }$	Parallel confinement time in scrapeoff (s)
τ_{ce}	Electron conduction/convection confinement time (s)
τ_{eq}	Ion-electron equipartition time (s)
τ_E	Global energy confinement time (s)
$\tau_{Ei,e}$	Ion, electron confinement time (s)
$\tau_E(OPT)$	Minimum-COE confinement time (s)
$\tau_E(PHYS)$	Physics-based confinement-time (s)
τ_I	Time interval between pellet injections (s)
τ_o	Plasma startup time (0)
τ_R	Dynamo relaxation time (s)
τ_u	Unscheduled down time (d)
τ_s	Scheduled down time (d)
ϕ	Toroidal direction, toroidal flux (Wb), or OFCD phase angle
$\phi_{He,DT}$	Particle flow rate from plasma (Pa m ³ /s)
χ_E	Heat conduction coefficient (m ² /s) or thermal diffusivity
ψ	Poloidal flux (Wb)
$\psi_{o,L}$	Poloidal flux at plasma edge
ω_{ce}	Electron cyclotron frequency (s ⁻¹)
ω_{pe}	Plasma frequency (s ⁻¹)

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APPENDIX A. TABLE OF ATR/ST DESIGN PARAMETERS

This table contains the comprehensive and uniform design data for ATR/ST design points at 500 MWe and 1000 MWe. The format of this table follows the DOE/OFE guidelines.¹ Superscripted numbers in parenthesis refer to notes found at the end of this table.

TABLE OF REACTOR DESIGN PARAMETERS

Parameter	Unit	500 MWe	1000 MWe
1. Characteristic Machine Dimensions			
1.1. Reactor envelope ^(1a)			
1.1.1. Height (TFC central column)	m	17.0	17.0
1.1.2. Width (EFC outside diameter)	m	15.8	15.8
1.1.3. Length (major circumference)	m	17.0	17.0
1.1.4. Volume of reactor envelope	m ³	13,330.	13,330.
1.2. Plasma chamber			
1.2.1. Major toroidal radius, R_T	m	2.70	2.70
1.2.2. Minor radius, a	m	1.50	1.50
1.2.3. Plasma volume, V_p	m ³	358.	358.
1.2.4. Plasma chamber volume	m ³	425.	425.
1.2.5. Wall surface area	m ²	416.	416.
1.2.6. Number of sectors, $N^{(1b)}$	-	12	12
2. Plasma Parameters (Steady State)			
2.1. Plasma dimensions			
2.1.1. Toroidal major radius, R_T	m	2.70	2.70
2.1.2. Minor radius, a	m	1.50	1.50
2.2. Average ion density, n_i	$10^{20}/m^3$	1.21	1.63
2.3. Average alpha-particle density, n_α	$10^{20}/m^3$	0.06	0.08
2.4. Energy confinement time, τ_E	s	0.79	0.59
2.5. Electron confinement time, τ_{pe}	s	TBD	TBD
2.6. Ion confinement time, τ_{pi}	s	3.2	2.4
2.7. Average Lawson parameter, $n_i \tau_E$	$10^{20} \text{ s}/m^3$	0.96	0.96
2.8. Poloidal/toroidal beta, β_θ/β	-	0.23/0.29	
2.9. Plasma toroidal current, I_ϕ	MA	39.8	46.2
2.10. Average ion temperature, $T_i^{(2a)}$	keV	15.	15.
2.11. Average electron temperature, $T_e^{(2a)}$	keV	15.	15.
2.12. Average alpha-particle energy, T_α	keV	770.	770.

	<u>Unit</u>	<u>500 MWe</u>	<u>1000 MWe</u>
2.13. Effective plasma ion charge ^(2b) , Z_{eff}	-	1.1	1.1
2.14. Reactor cycle			
2.14.1. Burn pulse length	s	Steady state	
2.14.2. Total pulse length	s	Steady state	
2.15. Fuel cycle	-	DT/Li	
2.16. Plasma-heating method	-	Ohmic (to ignition)	
2.16.1. Plasma-heating power, P_Q	MW	15.0	20.0
2.16.2. Plasma-heating frequency	GHz	NA	NA
2.17. Plasma energy gain ^(2c) , Q_p	-	112.	152.
3. <u>Power-Output</u>			
3.1. Plasma fusion power, $P_F = P_N + P_\alpha$	MWt	1671.8	3035.4
3.1.1. 14.1-MeV neutron power, P_N	MWt	1337.4	2428.3
3.1.2. 3.5-MeV alpha-particle power, P_α	MWt	334.4	607.1
3.2. Reactor thermal power	MWt	2047.4	3710.1
3.2.1. Power to first wall	MWt	102.1	TBD
3.2.2. Power to divertor	MWt	478.6	TBD
3.2.3. Power from blanket ($M_N = 1.27$)	MWt	1247.7	TBD
3.2.4. Power to inboard shield	MWt	323.4	TBD
3.3. Blanket power amplification factor, M_N	-	1.27	1.27
3.4. Plasma chamber power density ^(3a)	MWt/m ³	3.9	7.1
3.5. Plasma power density ^(3b)	MWt/m ³	4.7	8.5
3.6. Engineering power density ^(3c)	MWt/m ³	0.98	1.75
3.7. Blanket power density	MWt/m ³	5.1	9.2
3.8. Total thermal power to conversion cycle ^(3d) , P_{TH}	MWt	2047.4	3710.1
3.9. Plant gross electrical output, P_{ET}	MWe	737.9	1335.6
3.10. Plant net electrical output, P_E	MWe	500.	1000.
3.11. Thermal cycle efficiency, η_{TH}	-	0.36	0.36
3.12. Net plant efficiency, $\eta_p = \eta_{TH}(1 - \epsilon)$	-	0.24	0.27
3.13. Recirculating power fraction, ϵ	-	0.32	0.25
4. <u>Reactor Coolant System</u>			
4.1. Blanket coolant	-	$Pb_{83}Li_{17}$	

	Unit	500 MWe	1000 MWe
4.2. Blanket outlet temperature (hot leg)	K	674	TBD
4.3. Blanket inlet temperature (cold leg)	K	614	TBD
4.4. Blanket outlet pressure	MPa	0.73	TBD
4.5. Blanket inlet pressure	MPa	0.10	TBD
4.6. Blanket coolant flow rate (total)	kg/s	124,000.	TBD
4.7. Blanket structural material ^(4a)	-	HT-9	HT-9
4.8. First-wall coolant type	-	central-column integral with outboard blanket and TFC shield	
4.9. First-wall outlet temperature	K	TBD	TBD
4.10. First-wall inlet temperature	K	TBD	TBD
4.11. First-wall outlet pressure	MPa	TBD	TBD
4.12. First-wall inlet pressure	MPa	TBD	TBD
4.13. First-wall coolant flow rate	kg/s	TBD	TBD
4.14. Total number of blanket coolant loops ^(4b)	-	2	4
4.15. Type of blanket coolant circulator	-	PbLi Pump	
4.16. Power input to each circulator	MWe	TBD	TBD
4.17. Peak temperature in case of loss-of-coolant-flow accident			
4.17.1. First wall	K	TBD	TBD
4.17.2. Blanket multiplier	K	Pb	
4.17.3. Breeder	K	90% ⁶ Li	
4.18. Thermal-energy storage	J	not required	
5. <u>Intermediate Coolant System</u>	-	TBD	NA
6. <u>Steam Generation (SG) System</u> (PbLi/H ₂ O circuits)		TBD	
6.1. Steam outlet temperature	K	TBD	TBD
6.2. Steam outlet pressure	MPa	TBD	TBD
6.3. Steam flow rate (total)	kg/s	TBD	TBD
6.4. Feedwater temperature	K	TBD	TBD
6.5. Number of steam generators (SG) per loop	-	TBD	TBD
6.6. Number of reactor sectors per SG	-	TBD	TBD
6.7. SG materials, shell/tube ^(6a)	-	HT-9	HT-9
7. <u>Shield Coolant System</u> (inboard/outboard)			
7.1. Total power deposited in the shield	MWt	323.4/17.1	TBD
7.2. Shield coolant type	-	H ₂ O/PbLi	
7.3. Shield outlet temperature	K	568.	TBD

	Unit	500 MWe	1000 MWe
7.4. Shield inlet temperature	K	443.	TBD
7.5. Coolant outlet pressure	MPa	TBD	TBD
7.6. Coolant inlet pressure	MPa	TBD	TBD
7.7. Coolant flow rate (total)	kg/s	635.	TBD
8. Reactor Auxiliary System			
8.1. Vacuum pumping system			
8.1.1. Vacuum and plasma chamber pressure			
8.1.1.1. Plasma chamber pressure	Pa	TBD	TBD
8.1.1.2. Vacuum chamber pressure	Pa	TBD	TBD
8.1.2. Plasma chamber volume	m ³	425.	425.
8.1.3. Vacuum chamber volume	m ³	TBD	TBD
8.1.4. Number of vacuum pumps	-	TBD	TBD
8.1.5. Regeneration interval	h	TBD	TBD
8.1.6. Helium-pumping speed (per pump)	m ³ /s	TBD	TBD
8.2. Magnet cooling system (chilled H ₂ O)			
8.2.1. TFC cooling load ^(8a)	MWt	203.	302.
8.2.2. EFC cooling load ^(8b)	MWt	0.4	0.7
8.2.3. DFC cooling load ^(8c)	MWt	47.8	62.1
8.3. Plasma-heating-system cooling load	MWt	TBD	TBD
8.4. Plasma-fueling system			
8.4.1. Type ^(8d)	-	DT pellet	
8.4.2. Fuel composition	-	50% D, 50% T	
8.4.3. Fueling rates	g/s(ⁿ)	0.091	TBD
	g/s(T)	0.136	TBD
8.4.4. Pellet diameter	mm	5.1	TBD
8.4.5. Pellet injection frequency	s ⁻¹	13.1	TBD
8.5. Tritium-processing and recovery system			
8.5.1. Total tritium inventory	kg	TBD	TBD
8.5.2. Vulnerable tritium inventory	kg	TBD	TBD
8.6. Impurity control system			
Magnetic divertor			
8.6.1. Divertor coolant type	-	H ₂ O	H ₂ O
8.6.2. Divertor outlet temperature	K	NA	NA
8.6.3. Divertor inlet temperature	K	NA	NA
8.6.4. Divertor outlet pressure	MPa	NA	NA
8.6.5. Divertor inlet pressure	MPa	NA	NA
8.6.6. Divertor coolant flow rate	kg/s	NA	NA

	Unit	500 MWe	1000 MWe
9. Reactor Components			
9.1. First wall/blanket			
9.1.1. Structural material (first-wall/blanket)-		HT-9	HT-9
9.1.2. Breeding material	-	Pb ₈₃ Li ₁₇ (90% ⁶ Li)	Pb ₈₃ Li ₁₇ (90% ⁶ Li)
9.1.3. Neutron multiplier material	-	Pb	Pb
9.1.4. Breeding ratio ^(9a)	-	1.12	1.12
9.1.5. Number of modules ^(9b)	-	12	12
9.1.6. Weight of modules	tonnes	14	14
9.1.7. Weight of largest single component ^(9b)	tonnes	167.	167.
9.1.8. Dimensions of largest component (R,h) m		5.6/10.	
9.1.9. First-wall power loading			
9.1.9.1. 14.1-MeV neutron current	MW/m ²	3.24	5.87
9.1.9.2. Alpha-particle flux	MW/m ²	0.0	0.0
9.1.9.3. Radiation, charge-exchange, and conduction power to first wall	MW/m ²	1.35	2.38
9.1.9.4. Radiation, charge-exchange, and conduction/convection power to limiter	MW/m ²	NA	NA
9.1.9.5. First-wall life	MWyr/m ²	15	15
9.2. Shielding			
9.2.1. Material (inboard)/(outboard)	-	(H ₂ O/PCASS)/(PbLi/PCASS)	
9.2.2. Number of modules		12	12
9.2.3. Weight of each module	tonnes	52	52
		(included in 9.1.6.)	
9.2.4. Weight of largest single component ^(9c)	tonnes	626.	626.
		(included in 9.1.6.)	
9.2.5. Dimensions of largest component		torus,	
	m	TBD	TBD
9.3. Magnets ^(9d)			
9.3.1. Superconducting	-	EFC	EFC
9.3.2. Conductor material (insulator)	-	Cu-alloy(MgO) 70%/10%	
9.3.3. Structural material	-	PCASS(10%)	

	<u>Unit</u>	<u>500 MWe</u>	<u>1000 MWe</u>
9.3.4. Operating temperature	K	300-350	
9.3.5. Coolant	-	H ₂ O	H ₂ O
9.3.6. Mean stress in coil	MPa	TBD	TBD
9.3.7. Maximum force transmitted to building	MN	TBD	TBD
9.3.8. Maximum field	T	6.9	8.0
9.3.9. Field on plasma axis (paramagnetic/vacuum)	T	(4.1/2.3)(4.8/2.7)	
9.3.10. Field at plasma surface	T	TBD	TBD
9.3.11. Number of magnets			
9.3.11.1. Toroidal-field coils	-	12	12
9.3.11.2. Equilibrium-field coils	-	2	2
9.3.11.3. Ohmic-heating coils	-	NA	NA
9.3.11.4. Divertor-field coils	-	2	2
9.3.12. Mean coil radius			
9.3.12.1. Toroidal-field coil	m	NA	NA
9.3.12.2. Equilibrium-field coil	m	7.51	7.51
9.3.2.40.3. Divertor-field coil	m	2.40	2.40
9.3.13. Total stored energy (full torus)	GJ	12	16
9.3.13.1. Toroidal-field coil	GJ	TBD	TBD
9.3.13.2. Poloidal-field coil	GJ	TBD	TBD
9.3.14. Weight of largest single component ^(9e)	tonnes	568.	568.
9.3.15. Dimension of largest single torus component ^(9e)	m	17.0	17.0
9.4. Energy transfer and storage		Not required	
9.5. Plasma heating			
9.5.1. Type	-	ohmic dissipation	
9.5.2. Frequency	GHz	NA	NA
9.5.3. Power to the plasma, P _Q	MW	15.0	20.0
9.5.4. Transmission method	-		
9.5.5. Power reflected from plasma	MW	NA	NA
9.5.6. Power loss in transmission	MW	TBD	TBD
9.5.7. Power loss in amplifiers	MW	NA	NA
9.5.8. Power loss in power supplies	MW	TBD	TBD

	<u>Unit</u>	<u>500 MWe</u>	<u>1000 MWe</u>
9.5.9. System input power	MW	TBD	TBD
9.5.10. Heating time	s	TBD	TBD
9.5.11. Number of waveguide grills	-	TBD	TBD
9.5.12. Number of amplifiers	-	TBD	TBD
10. <u>Electrical Power Requirements</u>			
10.1. Cold-plasma start-up power from grid	MWe/s	300/8 (300-400)/8	
10.2. Auxiliary power requirements ^(10a)			
(normal operation)	MWe	51.6	93.5
10.2.1. Electrical energy storage	MWe	none required	
10.2.2. Magnet power supply (other than ohmic losses)	MWe	0.0	0.0
10.2.3. First-wall circulators ^(10b)	MWe	TBD	TBD
10.2.4. Divertor-coolant circulators	MWe	TBD	TBD
10.2.5. Blanket circulators ^(10b)	MWe	5.1	TBD
10.2.6. Shield-coolant circulators	MWe	TBD	TBD
10.2.7. Refrigeration system	MWe	TBD	TBD
10.2.8. Vacuum svstem (roughing)	MWe	TBD	TBD
10.2.9. Miscellaneous reactor plant auxiliaries	MWe	TBD	TBD
10.2.10. Feedwater pump system	MWe	TBD	TBD
10.2.11. Condensing system	MWe	TBD	TBD
10.2.12. Heat rejection system	MWe	TBD	TBD
10.2.13. Miscellaneous BOP auxiliaries	MWe	TBD	TBD
11. <u>Buildings</u>			
11.1. Reactor Building			
11.1.1. Characteristic dimensions	m	TBD	TBD
(includes steam-generators and accumulators)			
11.1.2. Enclosed volume			
11.1.2.1. Free volume	m ³	TBD	TBD
11.1.2.2. Total volume	m ³	1.9(10) ⁵	
11.1.3. Minimum wall thickness for shielding	m	2.5	2.5
11.1.4. Internal pressure			
normal/accident	MPa	TBD	TBD
11.1.5. Containment atmosphere	-	CO ₂	CO ₂

	<u>Unit</u>	<u>500 MWe</u>	<u>1000 MWe</u>
11.2. Turbine Building			
11.2.1. Characteristic dimensions	m	110 × 50 × 44	
11.2.2. Enclosed volume	m ³	2.42(10) ⁵	
11.3. Reactor Service Building			
11.3.1. Characteristic dimensions	m	TBD	TBD
11.3.2. Special functions (i.e., tritium handling, radwaste, maintenance and storage, hot cells, blanket processing equipment, etc.)	m	TBD	TBD
12. <u>Reactor Maintenance</u>			
12.1. First-wall/blanket			
12.1.1. Annual percentage	% area/yr	TBD	TBD
12.1.2. Area	m ² /yr	416.	416.
12.1.3. Weight ^(12a)	tonnes/yr	42.	84.
12.2. Radioactive material storage for life of plant (30 yr)			
12.2.1. Total long-term storage	m ³ /tonnes	TBD	TBD
12.2.2. Remaining recycled blanket storage	m ³ /tonnes	TBD	TBD
12.2.3. Total recycled reactor equipment (after plant decommissioning)	m ³ /tonnes	TBD	TBD
12.3. Reactor availability	-	TBD	TBD
12.4. Overall plant availability ^(12b)	-	0.76	0.76

Footnotes for Reactor Design Table

1. Characteristic Machine Dimensions

- (a) The reactor envelope encloses the EFCs and has the dimensions given with a circumference of 17.0 m (at the major toroidal radius of 2.7 m).
- (b) Sectors are used only for assembly of the reactor torus. The outboard FW/B/S assembly is removed as a unit. The TFCs are assembled from demountable segments.

2. Plasma Parameters

- (a) Anticipated to give a thermally stable burn.
- (b) Based only on steady-state alpha-particle density,
 $Z_{\text{eff}} = (1 + 4f_{\alpha}) / (1 + 2f_{\alpha})$, $f_{\alpha} = n_{\alpha} / n_i = 0.05$.
- (c) Based on ratio of fusion power to ohmic dissipation in plasma; inclusion of ohmic power dissipated in TFCs and PFCs gives Q-values of 9.1 and 12.6, respectively.

3. Power Output

- (a) Based on fusion power divided by plasma chamber volume (425 m³).
- (b) Based on fusion power divided by plasma volume (358 m³).
- (c) Based on total thermal power and FPC volume (2100 m³) enclosed by and including magnets.
- (d) Does not include primary-coolant pumping power.

4. Reactor Coolant System

- (a) HT-9 is a ferritic steel (85.0 Fe/11.5 Cr/1.0 Mo/0.55 Mn/0.5 Ni/0.5 W/0.4 Si/0.3 V/0.2 C).
- (b) Each PbLi loop contains a superheater, steam generator, and an economizer.

6. Steam Generation (SG) System

- (a) HT-9 double-tube heat exchangers, PbLi/water on shell side and water/system on tube side, with stagnant helium gas separating double tubes.

8. Reactor Auxiliary System

- (a) Of this total, 70.7 MW is nuclear heating and 132.3 MW is ohmic heating.
- (b) Superconducting with 0.4 MW nuclear heating.
- (c) Of this total [500 MWe(net) case], 11.0 MW is nuclear heating and 36.8 MW is ohmic heating.

- (d) Pellet speed $u = 85$ km/s for penetration $d/r_p = 0.333$ with ratio of pellet inventory to plasma inventory equal to 0.10. Significant edge-plasma fueling would be highly desirable to reduce required pellet velocity and/or injection frequency.

9. Reactor Components

- (a) MCNP three-dimensional tritium breeding ratio (TBR) result.
- (b) The first-wall/blanket/outboard shield forms a single, integral unit; weight does not include the (drained) PbLi coolant.
- (c) Outboard shield.
- (d) Total masses of TFC set, EFC set, and DFC set are 3,254 tonnes, 538 tonnes, and 186 tonnes, respectively.
- (e) TFC central column.

10. Electrical Power Requirements

- (a) Taken nominally as $f_{AUX} = 0.07$ of the gross electrical power, $P_{ET} = 737$ and 1336 MWe, respectively.
- (b) Based on 75% pump efficiency.

12. Reactor Maintenance

- (a) Based on replacing entire first wall and blanket after four (two) years of operation.
- (b) Based on 60 day/year unscheduled maintenance and 28 day/FPC replacement.

REFERENCE FOR APPENDIX A

1. C. R. Head, "Standard Fusion Reactor Design Study Contents," Department of Energy, Office of Fusion Energy letter RS&A:CRH:#478 (March 7, 1979).

APPENDIX B. COST DATABASE

B.1. Cost Basis

Capital and operating costs leading to an estimate of the cost of electricity (COE) for the ATR/ST are calculated using a costing code based on widely used DOE guidelines^{1,2} and a unit-cost database (or a set of scaling rules) developed over a number of years and implementing information from a number of studies.³⁻²¹ A new system of accounts will eventually supersede the system used here. The present database follows closely that of the CRFPR.²¹

The ATR/ST cost estimate is specific to this system and reflects the ground rules applied to this study. It is expected, however, that many of the results can be used to assess economic trends for many other magnetic fusion concepts, particularly since the database has been calibrated by other recent studies. When comparing results with an estimated cost of an existing power plant, it should be emphasized that the ATR/ST design is preconceptual, with most subsystems not fully defined or developed. Cost estimates for poorly defined subsystems were determined with implicit design allowances to account for uncertainties. Fortunately, many of the balance-of-plant (BOP) and heat-transport systems are similar to existing Pressurized-Water Reactor (PWR) fission systems, and this similarity should enhance the credibility of the capital-cost estimates. The direct-capital cost estimates associated with the reactor plant equipment (RPE), the BOP equipment, land, and all the related structures and site facilities, are based upon supplier quotes, historical data, and costs of analogous systems, as compiled, assimilated, and modified by the Refs. 3-21 studies. The indirect-cost estimates related to construction are based upon DOE recommendations,^{1,2} as modified by recent studies. Time-related costs account for both interest and escalation during construction. The annual costs include annualized capital cost, operations and maintenance costs, fuel costs, and any scheduled component replacement costs. Given these costs along with the plant capacity (i.e., net power) and the plant availability, the bus-bar energy cost estimate is determined. These costs are presented in both constant (1980) dollars and then-current (1986) dollars, reflecting the assumed 6-yr construction time.

This Appendix defines the economic guidelines and assumptions used in the study and analyses. The key design, performance, and operational features and their impact on the economics of the overall system are discussed. The capital cost accounts summarized in Table B-I generally follow the DOE guidelines^{1,2} and provide a uniform evaluation/accounting tool by which possible comparisons can be made with other studies. The estimated costs for the ATR/ST are discussed, and any significant influencing factors or components are highlighted. Table B-II shows the total bus-bar energy cost components for fusion-generated energy.

B.1.1. Economic Guidelines and Assumptions

To assure a consistent economic evaluation of the ATR/ST, the DOE guidelines^{1,2} for costing fusion systems were adopted to the maximum extent for this study. This procedure assists in an evaluation of the ATR/ST that is consistent with procedures used for alternate energy systems. These guidelines apply both to design and economic analyses. All costs quoted in this report are referenced to 1980 dollars in order to facilitate comparison with past studies. It is assumed that the user of the power plant will be an investor-owned utility.

B.1.2. Level of Technology

The ATR/ST design philosophy adopts the state-of-the-art technology for all BOP systems except where incorporation of specific advances in technology will enhance the performance, schedule, and/or cost. The ATR/ST turbine cycle efficiency is determined by calculation (Sec. 4.4.) or by default is assumed to be $\eta_{TH} = 0.35$. With the exception of the PbLi steam generator, most of the BOP systems selected for the ATR/ST represent current PWR technology (i.e., the pressurized-water circuit steam generators, superheaters, turbine and electric plant equipment, Condensing and Heat Rejection Equipment, and most of the Miscellaneous Plant Equipment). All buildings are also considered to be of conventional design.

Three levels of technology compose the RPE. The first level represents technologies that have been demonstrated for a commercial power plant. Typical of this level are the Primary and Closed Coolant Systems, power supplies, portions of the Radioactive Waste Treatment and Disposal System, and most of the Other Reactor Plant Equipment. The second level represents technologies that have been demonstrated in existing power plants but that have new or unique

TABLE B-I
SUMMARY OF STANDARD CAPITAL COST ACCOUNTS¹

Cost Account Title

Direct Costs:

- 20. Land and Land Rights
- 21. Structures and Site Facilities
- 22. Reactor Plant Equipment (RPE)
- 23. Turbine Plant Equipment
- 24. Electric Plant Equipment
- 25. Miscellaneous Plant Equipment
- 26. Special Materials

Total Direct Cost (TDC)

Unit Direct Cost (UDC, \$/kWe)

Indirect Costs:

- 91. Construction Facilities, Equipment, and Services
- 92. Engineering and Construction Management Services
- 93. Other Costs

Total Base Cost

Unit Base Cost (UBC, \$/kWe)

- 94. Interest During Construction (IDC)
- 95. Escalation During Construction (EDC)
- 99. Total Cost

Unit Total Cost (UTC, \$/kWe)

Annual Costs:

- 40. Salaries of Facility Personnel
- 41. Miscellaneous Supplies and Equipment
- 42. Outside Support Services
- 43. General and Administrative Costs
- 44. Coolant Makeup
- 45. Process Materials
- 46. Fuel Handling Costs
- 47. Miscellaneous Costs
- 50. First Wall/Blanket/Limiter Replacement
- 51. Replacement of Other Reactor Components
- 02. Fuel Cost
- 03. Fuel Cycle Materials Cost

TABLE B-II
TOTAL BUS-BAR ENERGY COST COMPONENTS

- ♦ Annualized Capital Cost (Account 99)
- ♦ Operations and Maintenance (Accounts 40-47)
- ♦ Scheduled Component Replacement (Accounts 50-51)
- ♦ Fuel (Accounts 02-03)
- Total Annual Cost
- Cost of Electricity (COE, mills/kWh)

application, design, and/or configuration. Systems at this level represent only a modest extrapolation of the existing state of the art; examples are elements of the shielding, the Atmospheric Tritium Cleanup System, elements of the Maintenance Equipment, Special Heating Systems, Inert Atmosphere System, and Reactor I&C System. The third level of technology remains to be demonstrated in a commercial power plant, but the technology may have been commercially demonstrated by other industries. Typical of this third level are the FFC, PbLi steam generator, PbLi pumps, and associated heat-transport equipment. All these systems and related technologies are assumed to have been commercially demonstrated in power plants by the time the ATR/ST is constructed (i.e., first-wall/blanket/limiter systems, the radiation-hardened copper-alloy coils, large vacuum cryopumps, tritium processing and storage, and special remote maintenance equipment).

Another aspect of the level of technology involves the design and operational philosophy of remote maintenance. This philosophy will require a re-evaluation of present power-plant design criteria, procurement procedures, and operational practices. Designs will have to be modified or redesigned for modular or single-piece replacement, and firmer control will have to be enforced on specifications and interchangeability of parts. Present trends in the nuclear industry indicate advantages to more off-site, factory construction. This philosophy represents an evolutionary change in the power industry, may be necessary in the future, but is not reflected directly in the costing procedure.

It is implied in all of the foregoing discussions that the ATR/ST, like STARTRE¹³, EBTR,¹⁴ and CRFPR^{19,21} is not the first-of-a-kind fusion power plant. These systems are assumed to be based on a specific design technology wherein all systems have been proved thoroughly. Equipment R&D costs,

therefore, are not included, and the equipment is costed with appropriate learning curves applied. Engineering and Construction Management Services (Account 92) are reduced to a degree that reflects the design standardization of the reactor and BOP. No tooling costs are included in the cost estimate as all initial tooling costs are amortized over previously constructed ATR/ST fusion power plants. Learning-curve design allowances and site assumptions are consistent with those assumed for STARFIRE.¹³

A contingency allowance is added to account for the difference between the sum of individual estimated costs and the total amount that is reasonably expected to be spent, considering the degrees of uncertainty in the estimated quantities, prices, and labor productivity. This contingency allowance is intended to reduce the risk of an overrun. The ATR/ST estimate uses the recommended value² of 15% for Accounts 21, 22, 23, 24, and 25.

Spare parts required by the power plant must be held in inventory in order that the plant can quickly recover operation in event of a breakdown. Spare parts do not include equipment that is permanently connected in the systems to assure a desired level of redundancy. The spare-parts allowance is assumed to be a percentage of direct equipment cost, with the following spare-parts allowances being adopted:^{1,13}

<u>Cost Account</u>	<u>Spare-Parts Allowance</u>
21, 22, 23	2%
24	4%
25	3%
Others	0%

The cost of replacing the ATR/ST first-wall and blanket is incurred approximately every $(I_w \tau / I_w)$ th year and is included as an operating expenditure, although the relatively low cost of this item (~ 10-15 M\$) would allow inclusion of extras under the spare-part allowance.

B.1.3. Indirect-Cost Allowances

The indirect-cost allowances are expenses resulting from the support activities required to design, fabricate, assemble, and check out the entire power plant. The three major accounts are Construction Facilities, Equipment and Services (Account 91), Engineering and Construction Management Services

(Account 92), and Other Costs (Account 93). Appendix G of Ref. 14 gives additional detail for specific items included in each account.

Construction Facilities, Equipment and Services (Account 91) for the ATR/ST are not unlike those for fission plants, although some assembly and checkout of the reactor torus may be conducted offsite. The cost estimate of the Construction Facilities, Equipment and Services adopts an allowance of 10% of the total direct cost. This procedure is based on that adopted for STARFIRE,¹³ which, because of a higher total direct cost for the latter, may be underestimated for the ATR/ST. Present fission nuclear experience sets this percentage more in the range 15-20%. The FFC will require very little on-site handling and no field construction, with the possible exception of minor assembly of the PFCs.

Engineering and Construction Management Services (Account 92) consists of the expenses for reactor and plant engineering and construction management services. The design philosophy of applying the present and envisaged power-plant technology will certainly reduce the required engineering for the BOP and the Heat Transfer and Transport systems. The only engineering services being considered are those which are necessary for site development, utility requirements, new or updated regulatory guides, and design improvements. Services required of the construction management will be eased somewhat because of familiarity with the PWR systems. Based upon these considerations and the capital-intensive cost base, Account 92 costs are taken to be 8% of the TDC.

Associated with other costs (Account 93) are taxes, insurance, staff training, plant startup, and owner's General and Administrative (G&A) costs. Most of these items scale directly with the direct capital expense, and the ATR/ST estimate, therefore, adopted the recommended 5% of TDC for Other Costs.¹ A proposal⁴ for an update and re-standardization of the indirect cost accounts has not been implemented into the ATR/ST cost package at this time. The new methodology would increase indirect costs from the 23% value, used in this study and for STARFIRE¹³ to 35%.

B.1.4. Time-Related Costs

Time-related costs are incurred because the fabrication, installation, construction, checkout, and startup occur over a finite period of time. These expenses are related to the opportunity cost of money and the changes in the purchasing power of the dollar with respect to time. Account 94 represents the

allowance for funds used during construction (AFDC) or interest during construction (IDC). The IDC is the expense of the interest charges of financing the debt, the charges on the equity (common stock) portion of the financing, and any administrative charges on the financing. The interest during construction is determined by three elements: the total direct and indirect capital cost of the facility, the time distribution of the capital expenditures, and the aggregate interest rate on all financing charges. The time distribution of the capital expenditures is dependent upon the construction schedule, the construction techniques, the material and equipment purchases and progress payments, and the checkout and startup schedule. The expenditure pattern^{1,13}

Fig. B-1. Fusion-power-plant expenditure pattern.^{1,13}

shown in Fig. B-1 is adopted by this study. At a point 60% through the construction period, one-half of the direct costs have been incurred.

The aggregate interest rate is representative of a privately owned utility. The following assumptions¹ are used as a basis for determining the cost of capital:

- ♦ Utility is investor owned.
- ♦ Capital structure is 53% debt financing and 47% equity (common stock) financing.
- ♦ Nominal cost of debt financing is 8% per year.
- ♦ Nominal cost of equity financing is 14% per year.
- ♦ Power plant economic lifetime is 30 yr with no salvage value.
- ♦ Cost escalation and general inflation is 5% per year.

Given these assumptions, the nominal cost of capital is 10% per year, and the real (deflated) cost of capital is 5% per year.

Two modes of economic analysis are utilized in this study. The first mode is a "constant-dollar" mode, which assumes the purchasing value of the dollar remains constant over time, and expresses the cost in 1980 dollars. The inflation is assumed to be zero and the cost of capital is 5% per year. The second mode of analysis uses the "then-current-dollar" mode which assumes that purchasing value changes over time (inflation rate is not zero). The cost of capital for this mode is 10% per year, and the escalation is 5% per year. Figure B-2 illustrates the means by which interest and escalation are additive to the direct and indirect costs of capital. The specific values assumed for interest and escalation have been standardized¹⁻³ for comparison purposes with other fusion studies and are not intended to reflect actual interest and inflation fluctuations. The multipliers of the direct cost as a function of construction time are illustrated in Fig. B-3. All costs reported in Accounts 20-26 and 91-93 are presented in 1980 dollars, and all effects of cost of capital and escalation during construction are reported, respectively, in Accounts 94 and 95 as factors of total direct and indirect costs.¹ The then-current-dollar analysis gives essentially a nominal first-year facility cost, with escalation only computed during construction.

Fig. B-2. Comparison of constant and then-current dollar analyses for a 6-yr construction period.^{1,13}

Care should be exercised when comparing the cost of ATR/ST to that of other energy sources. Key factors involve the cost basis of the estimate (usually the start of construction), the length of construction, the basis for the cost of capital and escalation, and the presentation mode of the facility economics (e.g., constant, then-current, or levelized). Any new energy source presently starting construction will certainly cost more than an existing energy source as a result of inflation. Any comparison, therefore, should only consider new starts on alternative energy sources. Also, the preliminary nature of the present ATR/ST cost estimates, based on incomplete engineering designs, is again

Fig. B-3. Factors used for Interest During Construction (IDC) and Escalation During Construction (EDC) as a function of construction duration.^{1,3,13}

emphasized, this uncertainty being reflected both in uncertain physics performance (i.e., UDC) and plant availability (i.e., COE).

B.1.5. Key Design, Performance, and Operational Features

Several key features of a power plant directly influence the capital and operational cost. Table B-III lists the major design, performance, and operational features that can significantly affect the power plant economics. The ATR/ST design philosophy assumes a developed fusion-energy industry, as reflected by several design features: steady-state operation, enhanced maintenance access, and limiter/vacuum impurity-control system. These features remain to be thoroughly demonstrated but would be qualitatively typical of the design features needed for a fusion power plant.

TABLE B-III

KEY ATR/ST DESIGN, PERFORMANCE, AND OPERATIONAL FEATURES

- ♦ A specific design technology dictated by past construction and operating experience
- ♦ Small aspect-ratio tokamak device
- ♦ Steady-state operation
- ♦ Magnetic divertor impurity-control system
- ♦ Copper-alloy high-heat flux surfaces
- ♦ Moderate neutron first-wall loading ($I_w \sim 3\text{--}6 \text{ MW/m}^2$)
- ♦ Reactor thermal power output $\sim 2000\text{--}3700 \text{ MWt}$
- ♦ No intermediate coolant loop
- ♦ Plant availability ~ 0.76
- ♦ Low fuel cost, typically expected of fusion

Steady-state operation of the ATR/ST reactor relieves thermal-fatigue problems and increases the system reliability. Steady-state operation also eliminates the need for thermal and electrical energy storage. Commercial operation also requires adequate maintenance access. The decision to incorporate fully remote, single-piece maintenance in the reactor building and hot cell exerts another strong influence on system economics, particularly from the viewpoint of plant availability. Remote handling is presently undergoing rapid development, and it is assumed that the necessary equipment has been developed and is being utilized.

System redundancy, steady-state operation, ease of reactor torus replacement, and development of reliable components should permit the assumed overall plant availability of 76% for the ATR/ST designs. This plant availability also includes a major 120-day shutdown every ten years. Steady-state operation should considerably improve reliability for the application of economically optimum engineering safety factors. The plant availability is reduced from 100% because of outage time for scheduled, t_s , and unscheduled, t_u , maintenance periods. The plant availability equals $(365 - t_u - t_s)/365$, where t_u and t_s are expressed in days. The scheduled outage time has been estimated as 28 days per reactor-torus changeout, including 120 days every 10 yrs for turbine-generator overhaul. To achieve the target availability of 76%, the unscheduled outage is set at 60 days per year.

The periodic first-wall and blanket replacement is an important operational feature. An integral neutron first-wall loading or lifetime of $I_w \tau = 15 \text{ MWyr/m}^2$, an average neutron wall load of $\sim 3\text{--}6 \text{ MW/m}^2$, and an overall plant availability of 76% yield a first-wall life of approximately 6-3 calendar years. The remote maintenance scenario is designed to accomplish the required single-piece replacement within the allowed maintenance period.

An attractive feature of a DT fusion power plant is the low cost of fuel. Deuterium is estimated to cost 2200 \$/kg. Adequate tritium would be bred by the reactor and, therefore, is not considered as a cost item. The future tritium cost will be dependent upon the then-current supply and demand for tritium but will unlikely ever decrease below the cost of processing and handling. The startup cost for tritium has not been taken into account.

B.1.6. Cost of Electricity

All prior analyses are utilized to calculate the bus-bar cost of energy available from the ATR/ST fusion power plant. The COE is the most important evaluation tool to optimize (Sec. 3.1.) and to compare with alternative energy sources. Both constant-1980 and then-current-1986 dollar analyses are used to evaluate the ATR/ST economic parameters. The general equation for bus-bar energy cost is given by

$$\text{COE} = \frac{C_{AC} + (C_{O\&M} + C_{SCR} + C_F)(1 + E)^P}{8760 P_E P_f}, \quad (1)$$

where

COE = Cost of electricity in constant or then-current dollars (mills/kWeh),

C_{AC} = Annual capital cost charge, equals total capital cost multiplied by fixed charge rate (0.10 for constant-dollar analysis or 0.15 for then-current-dollar analysis),

$C_{O\&M}$ = Annual operations and maintenance cost, $C_{40} + C_{41} + \dots + C_{47}$,

C_{SCR} = Annual scheduled component replacement cost, $C_{50} + C_{51}$,

C_F = Annual fuel costs, C_{02} and C_{03} ,

- E = Escalation rate equals 0.0 for constant-dollar analysis and 0.05 for then-current-dollar analysis,
- P = Construction period (yr),
- P_E = Net plant capacity (MWe), and
- P_f = Plant availability factor.

The essential elements of the ATR/ST cost database are summarized in Table B-IV.

For purposes of costing in the parametric systems model, the reactor building is divided into a variable-volume reactor cell, housing the FPC and vacuum tank, and a fixed-volume region, housing the dual-media primary heat-transfer/transport loops. The volume of the latter portion, consistent with the layout depicted in Fig. 4.1.-1, is estimated to be $1.55(10)^5 \text{ m}^3$ and is similar to that of the STARFIRE design.¹³ The reactor room is modeled by a rectilinear enclosure extending horizontally 9 m beyond the FPC with a height approximately 6 times that of the FPC, such that $V_{RB} = [2(R_T + r_s + 9)]^2 [12r_s] \text{ m}^3$. The basic building structure (Account 21.2.1.) is costed at 300 \$/m³, a value intermediate between that of STARFIRE¹³ and MARS,¹⁸ to which is added 2 M\$ for building services (Account 21.2.2.), 30 M\$ for containment structures (Account 21.2.3.), and 7.5 M\$ for architectural costs (Account 21.2.4.).

The ATR/ST Main Heat Transfer System includes a PbLi loop serving the blanket and a pressurized-water loop serving the resistive coils, divertor, and shield. The fraction of thermal power delivered to the pressurized-water loop is denoted by $f_w \approx 0.37$ (Fig. 4.3.-1). The cost of the PbLi loop (Account 22.2.1.) is estimated to be $3.40(10)^4 P_{TH}(1 - f_w) \text{ M\$}$ and that of the pressurized-water loop (Account 22.2.3.) is estimated to be $3.50(10)^4 P_{TH} f_w \text{ M\$}$, these estimates being calibrated by the dual-media MARS design¹⁸ with a reduction of 80% of the dominant piping costs of that design to reflect the shorter pipe runs in the ATR/ST case. This model results in a ~ 50 M\$ increase in cost over the pressurized-water Main Heat Transfer System in STARFIRE.^{13,16} The PbLi inventory in the system consists of 95% of the blanket volume, corrected by a factor of 1.09 to account for the FPC ducts connecting the blanket through the TFC/PFC sets to the main PbLi manifolds. To this variable volume is added a fixed inventory of 4,525 tonnes for the primary-loop inventory,²¹ a value assumed to be relatively constant over the parameter range

TABLE B-IV
SUMMARY FUSION REACTOR COST DATABASE^(a)

ACC. NO	ACCOUNT TITLE	(M\$, 1980)
20.	Land and land rights	3.3
21.	Structures and site facilities	
21.1	Site improvements and facilities	11.15
21.2	Reactor building	$3(10)^{-4} V_{RB} + 39.5$
21.3	Turbine building	33.5
21.4	Cooling structures	$7.135(P_{ET}/1000)^{0.3}$
21.5	Power supply and energy storage	9.16
21.6	Miscellaneous buildings	76.5
21.7	Ventilation stack	1.81
21.98	Spare parts (2%)	
21.99	Contingency (15%)	
22.	Reactor Plant Equipment	
22.1	Reactor Equipment	
22.1.1	Blanket and first wall	$0.31 V_{BL}$
22.1.2	Shield	$0.105 V_{SHD}$
22.1.3	Magnets	$0.292 V_C$
22.1.4	Supplemental heating systems	0.0
22.1.5	Primary structure and support	$0.1125 V_{STR}$
22.1.6	Reactor vacuum system	$0.015 M_{VAC} + 0.83 (P_F/250)$
22.1.7	Power supply (switching & energy storage)	$0.04 P_{ET}$
22.1.8	Impurity control system	$0.0026 V_{VAC}$
22.1.9	Direct energy conversion	0.0
22.1.10	ECRH Breakdown system	2.82
22.2	Main heat transfer system	
22.2.1	Primary coolant (PbLi)	$0.034 P_{TH} (1 - f_w)$
22.2.2.	Intermediate coolant system	0.0
22.2.3.	FW/Limiter/Shield coolant system (H ₂ O)	$0.034 P_{TH} f_w$
22.3	Auxiliary cooling systems	$6.7(10)^{-4} P_{TH}$
22.4	Radioactive waste treatment	$1.2(10)^{-3} P_{TH}$
22.5	Fuel handling and storage	$9.65(10)^{-3} P_{TH}$
22.6	Other reactor plant equipment	$1.0938(10)^{-2} P_{TH}$
22.7	Instrumentation and control	23.41
22.98	Spare parts allowance (2%)	
22.99	Contingency allowance (15%)	
23.	Turbine plant equipment	
23.1	Turbine-generators	$59.9 (P_{ET}/1000)^{0.7}$
23.2	Main steam system	$4.80 (P_{TH}/2860)$
23.3	Heat rejection systems	$33.0 (P_{TH}/2860)^{0.8}$
23.4	Condensing system	$13.8 (P_{ET}/1000)^{0.9}$
23.5	Feed heating system	$7.55 (P_{TH}/2860)$
23.6	Other turbine plant equipment	$40.9 (P_{ET}/1000)^{0.6}$
23.7	Instrumentation and control	$7.80 (P_{ET}/1000)^{0.3}$
23.98	Spare parts allowance (2%)	
23.99	Contingency allowance (15%)	

TABLE B-IV (cont)

ACC. NO	ACCOUNT TITLE	(M\$, 1980)
24.	Electric plant equipment	
24.1	Switchgear	8.6 ($P_{ET}/1000$)
24.2	Station service equipment	14.2 ($P_{ET}/1000$)
24.3	Switchboards	5.4 ($P_{ET}/1000$)
24.4	Protective equipment	2.11
24.5	Electrical structures and wiring containers	11.12+6.28($P_{ET}/1440$)
24.6	Power and control wiring	23.0+13.0($P_{ET}/1440$)
24.7	Electrical lighting	8.2
24.98	Spare parts allowance (4%)	
24.99	Contingency allowance (15%)	
25.	Miscellaneous plant equipment	
25.1	Transportation and lifting equipment	15.68
25.2	Air and water service systems	12.35
25.3	Communications equipment	6.22
25.4	Furnishings and fixtures	1.20
25.98	Spare parts allowance (3%)	
25.99	Contingency allowance (15%)	
26.	Special Materials	$0.25 + 9.5(10)^{-3} M_{PbLi}$
90.	Total direct cost (TDC)	
91.	Construction facilities, equipment, and services (10%)	
92.	Engineering and construction management services (8%)	
93.	Other costs (5%)	
94.	Interest during construction, (IDC, 10%/yr)	
95.	Escalation during construction, (EDC, 5%/yr)	
99.	Total cost	

- (a) Gross electric, P_{ET} , net electric, P_E , and total thermal, P_{TH} , powers given in MW. Volumetric $V(m^3)$ abbreviations or corresponding mass $M(\text{tonne})$ costs for the fusion power core (FPC) and related items are given as follows:
- Reactor building, $V_{RB} = 4(R_T + r_S)^2(12r_S) + 1.55(10)^5 (m^3)$
- Blanket structure (5%), $V_{BL}(m^3)$
- Shield, $V_{SHD}(m^3)$
- Magnet, $V_C(m^3)$
- Structure, $V_{STR}(m^3)$
- Vacuum tank, $M_{VAC} = (0.07)(7.8)2\pi[(R_T + r_S + 3)^2 + (R + r_S + 3)(4r_S)] (\text{tonne})$
- PbLi coolant, $M_{PbLi} = (0.95)(1.09)V_{BL}(9.4) + 4,525 (\text{tonne})$

of interest. The PbLi density is 9.4 tonnes/m³ and its unit cost (90% enriched) is taken to be 9.5 \$/m³, a value intermediate to those of UWTOR-M¹⁵ and MARS¹⁸ estimates, both being close to each other. The cost of the primary-loop PbLi is reported under Special Materials (Account 26), insofar as it is salvageable and reuseable (once cleaned of Po²¹⁰).

The first-wall/blanket/limiter replacement cost estimate applies a factor of two to the direct cost of these components to allow for the handling/replacement of the spent reactor torus. For an assumed first-wall/limiter/blanket life of $I_w \tau = 15 \text{ MWyr/m}^2$ at a cost-optimized first-wall loading $I_w \approx 3\text{--}6 \text{ MW/m}^2$ and a plant factor ≈ 0.76 , routine replacement occurs every 6–3 years. Account 50. represents $\sim 3\%$ of the base-case ATR/ST COE and is distinct from the nominal annual O&M charge (Accounts 40–47, 51) conservatively estimated² to be 2% of the direct cost. This scheme costs the first reactor, first wall, and blanket twice, and credit for any reactor-torus component reuse (i.e., TFCs or shield) is not taken.

B.2. Sample Cost-Code Output

Output from the cost code includes a cost-database summary. Table B-V is a typical example for the ATR/ST with cited reference numbers corresponding to the list at the end of this Appendix. In addition, a detailed cost summary, as shown in Table B-VI, is given. A global recapitulation of the major cost accounts and ATR/ST major parameters is included at the end of Table B-VII along with the COE estimate based on constant (1980) and then-current (1986) dollars.

TABLE B-V
COMPUTER LISTING OF 500-MWe ATR/ST COST DATABASE

[illegible]

0000000000	weather-proof housing	0.758e+05 \$/unit	1.000e+00	2,13,14
0000000000	turbine-generators	1.678e+03 \$/MMth	1.017e+02	12
0000000000	main steam (or other fluid) system	0.	1.000e+00	
0000000000	water intake common facilities	0.	1.000e+00	
0000000000	circulating water systems	0.	1.000e+00	
0000000000	cooling towers	0.	1.000e+00	
0000000000	other systems which reject heat to the atmosphere	5.658e+04 \$/unit	4.456e+02	2,13,14
0000000000	heat rejection systems	0.	1.000e+00	
0000000000	condensers	0.	1.000e+00	
0000000000	condensate system	0.	1.000e+00	
0000000000	gas removal system	0.	1.000e+00	
0000000000	turbine by-pass systems(excl. piping)	2.753e+04 \$/unit	3.808e+02	2,13,14
0000000000	condensing systems	0.	1.000e+00	
0000000000	regenerators & recuperators	0.	1.000e+00	
0000000000	pumps	0.	1.000e+00	
0000000000	tanks	2.640e+03 \$/unit	2.047e+03	2,13,14
0000000000	feed heating system	0.	1.000e+00	
0000000000	turbine auxiliaries	0.	1.000e+00	
0000000000	auxiliaries cooling system(excl. piping)	0.	1.000e+00	
0000000000	make-up treat. & condensate purification systems	0.	1.000e+00	
0000000000	chemical treat. & condensate purification systems	0.	1.000e+00	
0000000000	central lubrication service system(excl. piping)	0.	1.000e+00	
0000000000	other turbine plant equipment	6.482e+05 \$/unit	5.254e+01	2,13,14
0000000000	instrumentation & control(i&c) equipment	9.820e+05 \$/unit	7.249e+00	2,13,14
0000000000	spare parts allowance	2.000e-02 fraction	-1.000e+00	2,13,14
0000000000	contingency allowance	1.500e-01 fraction	-1.000e+00	2,13,14
0000000000	turbine plant equipment	0.	1.000e+00	
0000000000	generator circuits	0.	1.000e+00	
0000000000	station service	0.	1.000e+00	
0000000000	switchgear	8.600e+03 \$/unit	1.000e+00	2,13,14
0000000000	station service & startup transformers	0.	1.000e+00	
0000000000	low voltage unit substation & lighting transformer	0.	1.000e+00	
0000000000	battery system	0.	1.000e+00	
0000000000	diesel engine generators	0.	1.000e+00	
0000000000	gas turbine generators	0.	1.000e+00	
0000000000	motor generator sets	0.	1.000e+00	
0000000000	station service equipment	1.420e+04 \$/unit	7.371e+02	13,16
0000000000	main control board for electric system	0.	1.000e+00	
0000000000	auxiliary power & signal boards	0.	1.000e+00	
0000000000	switchboards (incl. heat tracing)	5.400e+03 \$/unit	7.371e+02	2,13,14
0000000000	gen. station grounding sys. & cathodic protection	0.	1.000e+00	
0000000000	protective equipment	2.110e+06 \$	1.000e+00	13,14
0000000000	concrete cable tunnels, trenches & envelopes	0.	1.000e+00	
0000000000	cable trays & support	0.	1.000e+00	
0000000000	conduit	0.	1.000e+00	
0000000000	other structures	1.433e+07 \$	6.000e+00	13,14,+
0000000000	electrical structures & wiring containers	0.	1.000e+00	
0000000000	generator circuits wiring	0.	1.000e+00	
0000000000	station service power wiring	0.	1.000e+00	
0000000000	control wiring	0.	1.000e+00	
0000000000	instrument wiring	0.	1.000e+00	
0000000000	containment penetrations	0.	1.000e+00	
0000000000	power & control wiring	2.965e+07 \$	1.000e+00	13,14,+
0000000000	reactor building lighting	0.	1.000e+00	
0000000000	turbine building lighting	0.	1.000e+00	
0000000000	reactor auxiliaries building lighting	0.	1.000e+00	
0000000000	radioactive waste building lighting	0.	1.000e+00	
0000000000	fuel storage building lighting	0.	1.000e+00	
0000000000	miscellaneous buildings lighting	0.	1.000e+00	
0000000000	yard lighting	0.	1.000e+00	

24.4	7.0	0.0	0.0	electrical lighting	8.200e+06	\$	1.000e+00	13,14,14
24.4	38.0	0.0	0.0	spare parts allowance	4.000e-02	fraction	-1.000e+00	13,14,14
24.4	0.0	0.0	0.0	contingency allowance	1.500e-01	fraction	-1.000e+00	2,13,14
24.4	0.0	0.0	0.0	electric plant equipment	0.0		1.000e+00	
24.4	0.0	0.0	0.0	cranes, hoists, monorails, & conveyors	0.0		1.000e+00	
24.4	1.1	1.0	0.0	railway	0.0		1.000e+00	
24.4	1.1	3.0	0.0	roadway equipment	0.0		1.000e+00	
24.4	1.1	4.0	0.0	watercraft	0.0		1.000e+00	
24.4	1.1	5.0	0.0	vehicle maintenance equipment	1.568e+07	\$	1.000e+00	13,14
24.4	1.1	5.0	0.0	transportation & lifting equipment	0.0		1.000e+00	
24.4	1.1	0.0	0.0	air systems(excl. piping)	0.0		1.000e+00	
24.4	1.1	0.0	0.0	water systems(excl. piping)	0.0		1.000e+00	
24.4	1.1	2.0	0.0	auxiliary heating systems	0.0		1.000e+00	
24.4	1.1	0.0	0.0	air & water service systems	0.0		1.000e+00	
24.4	1.1	0.0	0.0	local communications systems	0.0		1.000e+00	
24.4	1.1	0.0	0.0	signal systems	0.0		1.000e+00	
24.4	1.1	2.0	0.0	communications equipment	6.220e+06	\$	1.000e+00	13,14
24.4	1.1	0.0	0.0	safety equipment	0.0		1.000e+00	
24.4	1.1	0.0	0.0	shop, laboratory, & test equipment	0.0		1.000e+00	
24.4	1.1	0.0	0.0	office equipment & furnishings	0.0		1.000e+00	
24.4	1.1	0.0	0.0	change room equipment	0.0		1.000e+00	
24.4	1.1	0.0	0.0	environmental monitoring equipment	0.0		1.000e+00	
24.4	1.1	0.0	0.0	dining facilities	0.0		1.000e+00	
24.4	1.1	0.0	0.0	furnishings & fixtures	1.200e+06	\$	1.000e+00	2
24.4	1.1	0.0	0.0	spare parts allowance	1.000e-02	fraction	-1.000e+00	2,13,14
24.4	1.1	0.0	0.0	contingency allowance	1.500e-01	fraction	-1.000e+00	2,13,14
24.4	1.1	0.0	0.0	miscellaneous plant equipment	0.0		1.000e+00	
24.4	1.1	0.0	0.0	reactor coolant	9.500e+90	\$/kg	4.525e+06	15,18
24.4	1.1	0.0	0.0	intermediate coolant	0.0		1.000e+00	
24.4	1.1	0.0	0.0	turbine cycle working fluids	0.0		1.000e+00	
24.4	1.1	0.0	0.0	other materials	0.0		1.000e+00	
24.4	1.1	0.0	0.0	spare parts allowance	2.500e+05	\$	1.000e+00	12,14
24.4	1.1	0.0	0.0	contingency allowance	0.0	fraction	-1.000e+00	
24.4	1.1	0.0	0.0	special materials	0.0		1.000e+00	
24.4	1.1	0.0	0.0	direct cost	0.0	fraction	-1.000e+00	
24.4	1.1	0.0	0.0	temporary facilities	0.0	fraction	-1.000e+00	
24.4	1.1	0.0	0.0	construction equipment	0.0	fraction	-1.000e+00	
24.4	1.1	0.0	0.0	construction services	0.0	fraction	-1.000e+00	
24.4	1.1	0.0	0.0	construction facilities, equipment & services (10%)	1.000e-01	fraction	-1.000e+00	13,14
24.4	1.1	0.0	0.0	engineering & construction management services (8%)	8.000e-02	fraction	-1.000e+00	
24.4	1.1	0.0	0.0	taxes & insurance	0.0	fraction	-1.000e+00	
24.4	1.1	0.0	0.0	staff training & plant startup	0.0	fraction	-1.000e+00	
24.4	1.1	0.0	0.0	owner's fee	5.000e-02	fraction	-1.000e+00	
24.4	1.1	0.0	0.0	other costs (5%)	1.200e-01	fraction	-1.000e+00	2,13,14
24.4	1.1	0.0	0.0	interest during construction (IDC)	1.000e-08	fraction	-1.000e+00	
24.4	1.1	0.0	0.0	escalation during construction (EDC)	1.000e-08	fraction	-1.000e+00	
24.4	1.1	0.0	0.0	total cost				3

TABLE B-VI
SAMPLE COST ACCOUNT LISTING FOR THE 500-MWe ATR/ST DESIGN

1		fusion reactor economic evaluation (ver. 2.1)	
ATR/ST		10/30/85	
0	acc. no.	account title	million dollars (1980)
20.	1.	land & privilege acquisition	3.000
20.	2.	relocation of buildings, utilities, highways, etc.	0.300
20.		land & land rights	3.300
21.	1. 1.	general yard improvements	
21.	1. 2.	waterfront improvements	
21.	1. 3.	transportation access (off site)	
21.	1.	site improvements & facilities	11.150
21.	2. 1.	basic building structures	56.280
21.	2. 2.	building services	2.000
21.	2. 3.	containment structures	30.000
21.	2. 4.	architectural	7.500
21.	2.	reactor building	95.780
21.	3. 1.	basic building structures	30.000
21.	3. 2.	building services	2.000
21.	3. 3.	architectural	1.500
21.	3.	turbine building	33.500
21.	4. 1.	intake structures	
21.	4. 2.	discharge structures	
21.	4. 3.	unpressurized intake & discharge conduits	
21.	4. 4.	recirculating structures	
21.	4. 5.	cooling tower systems	
21.	4.	cooling system structures	6.511
21.	5. 1.	basic building structures	8.030
21.	5. 2.	building services	0.530
21.	5. 3.	architectural	0.600
21.	5.	power supply & energy storage building	9.160
21.	6. 1.	reactor auxiliaries building(incl. switchgear bay)	3.260
21.	6. 2.	hot cell building	53.960
21.	6. 3.	fuel storage building	8.630
21.	6. 4.	control room building	3.100
21.	6. 5.	diesel generator building	2.050
21.	6. 6.	administration building	0.870
21.	6. 7.	service building	1.880
21.	6. 8.	cryogenics building	0.910
21.	6. 9.	miscellaneous structures & building work	1.840
21.	6.	miscellaneous buildings	76.500
21.	7.	ventilation stack	1.810
21.	98.	spare parts allowance	4.688
21.	99.	contingency allowance	35.162
21.		structures & site facilities	274.261

22.	1.	1.	1.	breeding material(incl. tritium breeding)	37.316	
22.	1.	1.	2.	first wall & structural material	6.901	
22.	1.	1.	3.	attenuators, reflectors, & multipliers		
22.	1.	1.	4.	wall modifiers(coatings, liners, limiters, etc.)		
22.	1.	1.	5.	others		
22.	1.	1.		blanket & first wall		44.217
22.	1.	2.	1.	primary		
22.	1.	2.	2.	secondary		
22.	1.	2.		shield		16.653
22.	1.	3.	1.	principal field magnet	130.144	
22.	1.	3.	2.	secondary field magnet	28.966	
22.	1.	3.		magnets		159.111
22.	1.	4.	1.	beam heating(neutral, ion or electron)		
22.	1.	4.	2.	rf heating		
22.	1.	4.	3.	laser heating		
22.	1.	4.	4.	other heating systems		
22.	1.	4.		supplemental heating systems		
22.	1.	5.	1.	reactor structure		
22.	1.	5.	2.	equipment support structure		
22.	1.	5.		primary structure & support		20.419
22.	1.	6.	1.	plasma chamber vacuum(incl. pumps/comp./pipe)	11.032	
22.	1.	6.	2.	magnet dewar vacuum(incl. pumps/comp./pipe)		
22.	1.	6.	3.	suppl. heating vacuum(incl. pumps/comp./pipe)		
22.	1.	6.	4.	direct converter vacuum(incl. pumps/comp./pipe)		
22.	1.	6.	5.	reactor vacuum system(low grade)		
22.	1.	6.	6.	reactor vacuum wall	5.517	
22.	1.	6.		reactor vacuum systems(unless integral elsewhere)		16.599
22.	1.	7.	1.	heating		
22.	1.	7.	2.	confinement		
22.	1.	7.	3.	control system		
22.	1.	7.	4.	central energy storage		
22.	1.	7.	5.	other		
22.	1.	7.		power supply, switching & energy storage		29.480
22.	1.	8.		impurity control		48.040
22.	1.	9.	1.	vacuum tank		
22.	1.	9.	2.	direct converter modules		
22.	1.	9.	3.	thermal panels		
22.	1.	9.	4.	power conditioning equipment		
22.	1.	9.		direct energy conversion system		
22.	1.	10.		ecrh breakdown system	38.430	
22.	1.			reactor equipment		372.949
22.	2.	1.	1.	pumps & motor drives(modular & nonmodular)		
22.	2.	1.	2.	piping		
22.	2.	1.	3.	heat exchangers		
22.	2.	1.	4.	tanks(dump,make-up,clean-up,trit.,hot storage)		
22.	2.	1.	5.	clean-up system		
22.	2.	1.	6.	thermal insulation, piping & equipment		
22.	2.	1.	7.	tritium extraction		
22.	2.	1.	8.	pressurizer		
22.	2.	1.	9.	other		
22.	2.	1.		primary coolant system		43.860
22.	2.	2.	1.	pumps & motor drives(modular & nonmodular)		
22.	2.	2.	2.	piping		
22.	2.	2.	3.	heat exchangers		
22.	2.	2.	4.	tanks(dump,make-up,clean-up,trit.,hot storage)		
22.	2.	2.	5.	clean-up system		
22.	2.	2.	6.	thermal insulation, piping & equipment		
22.	2.	2.	7.	tritium extraction		
22.	2.	2.	8.	pressurizer		

9.	other			
1.	intermediate coolant system			
2.	pumps & motor drives(modular & nonmodular)			
3.	heat exchangers			
4.	tanks(dump,make-up, clean-up, trit., hot storage)			
5.	clean-up system			
6.	thermal insulation, piping & equipment			
7.	tritium extraction			
8.	pressurizer			
9.	other			
	secondary coolant system		26.512	70.372
	main heat transfer & transport systems		10.000	
1.	refrigeration			
2.	fluid circulation driving system			
3.	tanks			
4.	purification			
5.	magnet cooling system		10.000	
1.	refrigeration			
2.	fluid circulation driving system			
3.	tanks			
4.	purification			
5.	shield & structure cooling system		1.371	
1.	refrigeration			
2.	fluid circulation driving system			
3.	tanks			
4.	purification			
5.	supplemental heating system cooling system			
1.	refrigeration			
2.	fluid circulation driving system			
3.	tanks			
4.	purification			
5.	power supply cooling system		11.371	
1.	other cooling systems			
2.	auxiliary cooling systems			
3.	liquid waste processing & equipment			
4.	gaseous wastes & off-gas processing system			
5.	solid waste processing equipment		2.456	
6.	radioactive waste treatment & disposal			
7.	fuel purification systems			
8.	liquefaction			
9.	fuel preparation			
1.	fuel injection			
2.	fuel storage			
3.	tritium recovery			
4.	emergency air detritiation			
5.	fuel handling & storage systems(fuel injection)		19.754	
6.	blanket & coil maintenance equipment			
7.	components rotated into service to allow maint.			
8.	other maintenance equipment			
9.	maintenance equipment			
1.	special heating systems(start-up, trace, etc.)			

22. 6. 3.	coolant receiving, storage & make-up systems	
22. 6. 4.	gas systems	
22. 6. 5.	building vacuum systems	
22. 6.	other reactor plant equipment	22.394
22. 7. 1.	reactor i&c equip.(burn control, diagnostics, etc.	
22. 7. 2.	radiation monitoring systems	
22. 7. 3.	isolated indicating & recording gauges, etc.	
22. 7.	instrumentation & control(i&c)	23.410
22.98.	spare parts allowance	10.454
22.99.	contingency allowance	78.406
22.	reactor plant equipment	611.567

1	1.	1.	turbine-generators & accessories	
2	1.	2.	foundations	
3	1.	3.	standby exciters	
4	1.	4.	lubricating system	
5	1.	5.	gas systems	
6	1.	6.	reheaters	
7	1.	7.	shielding	
8	1.	8.	weather-proof housing	
9	1.		turbine-generators	48.389
23.	2.		main steam (or other fluid) system	3.435
24	3.	1.	water intake common facilities	
25	3.	2.	circulating water systems	
26	3.	3.	cooling towers	
27	3.	4.	other systems which reject heat to the atmosphere	
28	3.		heat rejection systems	25.257
29	4.	1.	condensers	
30	4.	2.	condensate system	
31	4.	3.	gas removal system	
32	4.	4.	turbine by-pass systems(excl. piping)	
33	4.		condensing systems	10.483
34	5.	1.	regenerators & recuperators	
35	5.	2.	pumps	
36	5.	3.	tanks	
37	5.		feed heating system	5.404
38	6.	1.	turbine auxiliaries	
39	6.	2.	auxiliaries cooling system(excl. piping)	
40	6.	3.	make-up treat. system(excl. piping)	
41	6.	4.	chemical treat. & condensate purification systems	
42	6.	5.	central lubrication service system(excl. piping)	
43	6.		other turbine plant equipment	34.056
23.	7.		instrumentation & control(i&c) equipment	7.119
23.98.			spare parts allowance	2.683
23.99.			contingency allowance	20.121
23.			turbine plant equipment	156.947

1			
24.	1.	1.	generator circuits
24.	1.	2.	station service
24.	1.		switchgear
			6.339
24.	2.	1.	station service & startup transformers
24.	2.	2.	low voltage unit substation & lighting transformer
24.	2.	3.	battery system
24.	2.	4.	diesel engine generators
24.	2.	5.	gas turbine generators
24.	2.	6.	motor generator sets
24.	2.		station service equipment
			10.467
24.	3.	1.	main control board for electric system
24.	3.	2.	auxiliary power & signal boards
24.	3.		switchboards (incl. heat tracing)
			3.980
24.	4.	1.	gen. station grounding sys. & cathodic protection
24.	4.		protective equipment
			2.110
24.	5.	1.	concrete cable tunnels, trenches & envelopes
24.	5.	2.	cable trays & support
24.	5.	3.	conduit
24.	5.	4.	other structures
24.	5.		electrical structures & wiring containers
			14.330
24.	6.	1.	generator circuits wiring
24.	6.	2.	station service power wiring
24.	6.	3.	control wiring
24.	6.	4.	instrument wiring
24.	6.	5.	containment penetrations
24.	6.		power & control wiring
			29.650
24.	7.	1.	reactor building lighting
24.	7.	2.	turbine building lighting
24.	7.	3.	reactor auxiliaries building lighting
24.	7.	4.	radioactive waste building lighting
24.	7.	5.	fuel storage building lighting
24.	7.	6.	miscellaneous buildings lighting
24.	7.	7.	yard lighting
24.	7.		electrical lighting
			8.200
24.98.			spare parts allowance
24.99.			contingency allowance
			3.003
			11.261
24.			electric plant equipment
			89.341

254	1					
	25.	1.	1.	cranes, hoists, monorails, & conveyors		
		25.	1.	railway		
		25.	1.	roadway equipment		
		25.	1.	watercraft		
		25.	1.	vehicle maintenance equipment		
		25.	1.	transportation & lifting equipment	15.680	
	25.	2.	1.	air systems(excl. piping)		
		25.	2.	water systems(excl. piping)		
		25.	2.	auxiliary heating boilers(excl. piping)		
		25.	2.	air & water service systems	12.350	
	25.	3.	1.	local communications systems		
		25.	3.	signal systems		
		25.	3.	communications equipment	6.220	
	25.	4.	1.	safety equipment		
		25.	4.	shop, laboratory, & test equipment		
		25.	4.	office equipment & furnishings		
		25.	4.	change room equipment		
		25.	4.	environmental monitoring equipment		
		25.	4.	dining facilities		
		25.	4.	furnishings & fixtures	1.200	
	25.	98.		spare parts allowance	1.063	
		25.	99.	contingency allowance	5.317	
	25.			miscellaneous plant equipment		41.831
	26.	1.		reactor coolant	42.987	
	26.	2.		intermediate coolant		
	26.	3.		turbine cycle working fluids		
	26.	4.		other materials	0.250	
	26.	98.		spare parts allowance		
		26.	99.	contingency allowance		
	26.			special materials		43.237

TABLE B-VII
SAMPLE FUSION REACTOR COST AND PARAMETER SUMMARY FOR THE 500-MWe ATR/ST DESIGN

Los Alamos fusion reactor economic evaluation (ver. 1.9)						
0 designation:	designation:	designation:	designation:	designation:	designatid	10/30/85
0 acc. no.	account title					million dollars (1980)
20.	land & land rights					3.300
21.	structures & site facilities					274.261
22.	reactor plant equipment					611.567
23.	turbine plant equipment					156.947
24.	electric plant equipment					89.341
25.	miscellaneous plant equipment					41.831
26.	special materials					43.237
90.	direct cost					1220.484
91. 1.	temporary facilities					
91. 2.	construction equipment					
91. 3.	construction services					
91.	construction facilities, equipment & services (10%)					122.048
92.	engineering & construction management services (8%)					97.639
93. 1.	taxes & insurance					
93. 2.	staff training & plant startup					
93. 3.	owner's g&a					
93.	other costs (5%)					61.024
94.	interest during construction (IDC)					193.654
95.	escalation during construction (EDC)					0.000
99.	total cost					1694.850

thermal power (MMth)	=	2047.39	unit direct cost (\$/kWe)	[90]=	constant	then-current
gross electric power (MWe)	=	737.06	unit base cost (\$/kWe)	[93]=	2440.97	2440.97
net electric power (MWe)	=	500.00	unit total cost (\$/kWe)	[99]=	3002.39	3002.39
1/recirculating power fraction QE=		3.11	capital return (mills/kWeh)	=	50.99	102.02
plant availability factor	=	0.76	O&M (2.0%) (mills/kWeh)[40-47.51]=		9.03	12.10
construction= 6 yr: constant			B/FW replacement (mills/kWeh)[50]=		1.35	1.80
fIDC=	0.129	0.316	deuterium fuel (mills/kWeh) [02]=		0.04	0.04
fEDC=	0.000	0.190	COE (mills/kWeh)	=	61.40	115.97

TABLE B-VII (Cont)

advanced tokamak reactor (ATR) calculations (ver. 2.2)

plasma parameters

cbeta=	0.025	foetap=	0.129	xkappa=	3.000	
kd=	0.030	xe=	0.000	xshape=	2.064	qedge= 2.570
gdt=	1.690	gdd=	0.000	gohm=	22.240	
fbeta=	1.000	fpres=	0.500			
fjoh/xf=	1.000	frfa=	0.000	nmvutip=	1	
ipeng=	1	idiv=	1	icry=	0	ipar= 0
minor plasma radius, a (m)						1.500
major toroidal radius, rt (m)						2.700
average plasma radius, rp (m)						2.598
effective plasma major radius (m)						2.690
plasma aspect ratio, rt/a						1.800
plasma volume (m ³)						358.310
plasma current (MA)						39.835
toroidal plasma current density (MA/m ²)						2.879
Lawson parameter * t ₂ (1.0e22 s keV ² /m ³)						2.150
plasma temperature (keV)						15.000
Lawson parameter (1.0e20 s/m ³)						0.956
plasma density (1.0e20/m ³)						1.208
energy confinement time (s)						0.791
ignition margin						0.358
thermal diffusivity (m ² /s)						0.533
dt fusion power density (MW/m ³)						4.666
poloidal beta						0.232
toroidal beta						0.291
pinch parameter						0.470
reversal parameter, F						0.759
plasma/first-wall radius						0.666
streaming parameter (1.0e-14 A m)						1.556
plasma ohmic dissipation during burn (MW)						14.994
poloidal field quantities						
mass of DF coil set (tonne)						185.968
mass of EF coil set (tonne)						538.172
mass of PF coil set (tonne)						724.140
magnetic field at the plasma surface (T)						2.573
poloidal current density (MA/m ²)						7.500
ohmic dissipation during burn (MW)						36.849
volumetric heating during burn (MW/m ³)						1.446
toroidal field quantities						
TFC centerpost half-height						8.395
coil thickness (m)						0.899
outboard/inboard TF coil thickness ratio, fcoil						2.500
outboard/inboard TF coil thickness ratio, fcoilt						2.500
coil stress (MPa)						58.373
coil stress limit (MPa)						222.500
average minor radius of coil (m)						0.450
mass of coil (tonne)						3253.933
on-axis toroidal field (T)						2.297
magnetic field at the coil (T)						6.892
vertical field (T)						-1.630
toroidal field ripple (on-axis) (%)						0.175
toroidal field ripple (outboard) (%)						1.214
maximum energy stored in the coil (GJ)						5.890
toroidal-coil current (MA)						2.584
toroidal current density (MA/m ²)						12.187
inboard TFC ohmic dissipation (MW)						96.282
outboard TFC ohmic dissipation (MW)						35.981
ohmic dissipation during burn (MW)						132.264
volumetric heating during burn (MW/m ³)						0.297

TABLE B-VII (Cont)

engineering summary	
engineering q-value, qe	3.109
ohmic q-value, qt	9.081
plasma q-value, qp	111.503
rf driver power (MW)	0.000
fusion power (MW)	1671.851
total thermal power (MW)	2047.386
first-wall surface area (m ²)	415.793
14.06-MeV neutron loading (MW/m ²)	3.240
14.06-MeV blanket multiplication	1.270
2.45-MeV neutron loading (MW/m ²)	0.000
2.45-MeV blanket multiplication	0.000
circularized first-wall radius (m)	3.901
minor radius of system (m)	6.277
blanket volume (m ³)	403.597
inboard shield volume (m ³)	24.454
outboard shield volume (m ³)	89.461
divertor shield volume (m ³)	44.730
shield volume (m ³)	158.645
TF-coil centerpost volume (m ³)	77.760
TF-coil outboard-legs volume (m ³)	367.984
TF-coil volume (m ³)	445.744
DF-coil volume (m ³)	25.475
EF-coil volume (m ³)	73.722
PF-coil volume (m ³)	99.197
mass of first wall/blanket (tonne)	166.712
mass of shield (tonne)	1110.514
mass of TF coil set (tonne)	3253.933
mass of PF coil set (tonne)	724.140
total coil-set mass (tonne)	3978.073
mass of FPC structure (tonne)	1088.793
fusion-power-core mass (tonne)	6344.092
mass of divertor assembly (tonne)	558.463
mass of TF-coil centerpost (tonne)	567.648
mass of TFC-centerpost/inboard-shield assembly (tonne)	738.826
mass of drained blanket/outboard-shield assembly (tonne)	792.936
fusion-power-core mass utilization, m/pth (tonne/MWt)	3.099
FPC mass power density (kWe/tonne)	78.814
system power density (MW/m ³)	0.975
blanket thickness (m)	1.090
blanket power density (MW/m ³)	5.073
magnetic energy recovery time (s)	2.877

REFERENCES FOR APPENDIX B

1. S. C. Schulte, T. L. Wilke, and J. R. Young, "Fusion Reactor Design Studies--Standard Accounts for Cost Estimates," Battelle (Pacific Northwest Laboratory) report PNL-2648 (May 1978).
2. S. C. Schulte, W. E. Bickford, C. E. Willingham, S. K. Ghose, and M. G. Walker, "Fusion Reactor Design Studies-Standard Cost Estimating Rules," Battelle (Pacific Northwest Laboratory) report PNL-2987 (May 1979).
3. D. L. Phung, "A Method for Estimating Escalation and Interest during Construction (EDC and IDC)," Proc. 2nd Miami Int. Conf. on Alternative Energy Sources (December 10-13, 1979).
4. W. R. Hamilton, D. C. Keeton, and S. L. Thomson, "Cost Accounting System for Fusion Studies," Oak Ridge National Laboratory report ORNL/FEDC-85/7 (December 1985).
5. J. G. Delene, G. R. Smolen, H. I. Bowers, and M. L. Myers, "Nuclear Energy Cost Data Base," USDOE report DOE/NE-0044/2 (March 1984).
6. United Engineers and Constructors Inc., "Pressurized Water Reactor Plant," U.S.A.E.C. report 1230, Vol. 1 (June 1972).
7. A. A. Hollis, "An Analysis of the Estimated Capital Cost of a Fusion Reactor," AERE Harwell Laboratory report AERE-R.9933 (June 1981).
8. B. Badger, G. L. Kulcinski, R. W. Conn, C. W. Maynard, K. Audenaerde, H. Avci, et al., "Tokamak Engineering Test Reactor," University of Wisconsin report UWFDM-191 (1977).
9. TFCX Design Team, "Cost Summary Addendum to the Preconceptual Design Report," Princeton Plasma Physics Laboratory report A-Axxx-8407-010 (July 5, 1984).
10. K. I. Thomassen (principal investigator), "Conceptual Engineering Design of a One-GJ Fast Discharging Homopolar Machine for the Reference Theta-Pinch Fusion Reactor," Electric Power Research Institute report ER-246, Project 469 (August 1976).
11. R. E. Stillwagon, "Design Studies of Reversible Energy Storage and Transfer Systems for the Reference Theta-Pinch Reactor," Westinghouse report E.M. 4620 (September 1974).
12. R. L. Hagenson and R. A. Krakowski, "Compact Reversed-Field Pinch Reactors (CRFPR): Sensitivity Study and Design - Point Determination," Los Alamos National Laboratory report LA-9389-MS (July 1982).

13. C. C. Baker, M. A. Abdou, R. M. Arons, A. E. Bolon, C. D. Boley, J. N. Brooks, et al., "Starfire--A Commercial Tokamak Fusion Power Plant Study," Argonne National Laboratory report ANL/FPP-80-1 (September 1980).
14. C. G. Bathke, D. J. Dudziak, R. A. Krakowski, W. B. Ard, D. A. Bowers, J. W. Davis, et al., "ELMO Bumpy Torus Reactor and Power Plant Conceptual Design Study," Los Alamos National Laboratory report LA-8882-MS (August 1981).
15. B. Badger, I. N. Sviatoslavsky, S. W. VanSciver, G. L. Kulcinski, G. A. Emmert, D. T. Anderson, et al., "UWTOR-M: a Conceptual Modular Stellarator Power Reactor," University of Wisconsin report UWFD-550 (October 1982).
16. K. Evans, Jr., "A Tokamak Reactor Cost Model Based on Starfire/Wildcat Costing," Argonne National Laboratory report ANL/FPP/TM-168 (March 1983).
17. J. Sheffield, R. A. Dory, S. M. Cohn, J. G. Delene, L. Parsly, D. E. T. F. Ashby, and W. T. Reiersen, "Cost Assessment of a Generic Magnetic Fusion Reactor," Oak Ridge National Laboratory report ORNL/TM-9311 (1984).
18. B. G. Logan, C. D. Henning, G. A. Carlson, R. W. Werner, D. E. Baldwin, W. L. Barr, et al., "MARS Mirror Advanced Reactor Study Final Report," Lawrence Livermore National Laboratory report UCRL-53480 (July 1984).
19. R. L. Hagenon, R. A. Krakowski, C. G. Bathke, R. L. Miller, M. J. Embrechts, N. M. Schnurr, et al., "Compact Reversed-Field Pinch Reactors (CRFPR): Preliminary Engineering Considerations," Los Alamos National Laboratory report LA-10200-MS (August 1984).
20. R. L. Reid, R. J. Barrett, T. G. Brown, G. E. Corker, R. J. Hooper, S. S. Kalsi, et al., "The Tokamak Systems Code," Oak Ridge National Laboratory report ORNL/FEDC-84/9 (March 1985).
21. C. Copenhaver, R. A. Krakowski, N. M. Schnurr, R. L. Miller, C. G. Bathke, R. L. Hagenon, et al., "Compact Reversed-Field Pinch Reactors (CRFPR): Fusion-Power-Core Integration Study," Los Alamos National Laboratory report LA-10500-MS (August 1985).

APPENDIX C. MASS POWER DENSITY (MPD) AS A FIGURE OF MERIT

This appendix expands upon the merits and demerits of MPD as an economic figure-of-merit for fusion. The primary heat source for fusion is taken as the fusion power core (FPC, i.e., plasma chamber, first wall, blanket, shield, coils, and associated structure). The FPC mass divided by the useful thermal power generated therein is called the "mass utilization," MU(MWt/tonne). Whereas the ratio of net electric power to the FPC mass is termed the mass power density, MPD(kWe/tonne). The concept of MPD was developed in the course of the MFAC Panel X deliberations.¹ Figure C-1 summarizes both the MU and MPD for a range of fusion reactor designs. The MFAC Panel X preference for MPD is adopted for the following discussion.

Since most components of the FPC are costed on a unit-mass basis, the MPD gives a crude estimate of the FPC unit direct cost, UDC(\$/kWe). Combining UDC with operating cost, the cost of money during construction, and plant availability gives the cost of electricity, COE(mills/kWeh). The essential elements that impact the COE are summarized graphically in Fig. C-2. Although the MPD figure of merit does not account for these important cost components, any correlation of MPD with COE reflects primarily on the contribution of the FPC to the UDC.

A figure of merit that is as integrated as MPD is not expected to account for all ingredients needed for an "attractive" reactor design. No fusion reactor design is expected to give a monotonic decrease in COE as MPD increases, although those designs that minimize COE for a given MPD generally show this behavior out to a point where COE becomes independent of MPD (usually around ~ 100-200 kWe/tonne). In fact, the COE may increase as MPD is increased beyond that point. This flattening or even increase in COE for high MPD reflects an ever decreasing FPC cost, relative to the total direct cost, coupled with possible decreased plant efficiency, decreased plant availability, and increased plant operating costs. Figure C-3 illustrates this point. Shown is the optimized COE (constant 1980 dollars) versus MPD for the compact RFP reactor (CRFPR)^{8,9} and the present study of an advanced tokamak reactor based on the spherical torus^{27,28} (ATR/ST). Variations with net electric power, P_E (MWe), are also shown. Each point on a given curve represents a minimum-COE design for the corresponding value of MPD. Both the CRFPR and ATR/ST concepts utilize resistive coils and, therefore, reflect cost tradeoffs between coil mass and

Fig. C-1. Summary of FPC or engineering power densities projected for recent fusion reactor designs plotted against the inverse of the mass utilization: The early University of Wisconsin UWMAK-I tokamak²; STARFIRE tokamak³; Culham MkIIB tokamak⁴; University of Wisconsin NUMAK tokamak⁵; superconducting Reversed-Field Pinch Reactor, RFPR^{6,7}; Compact Reversed-Field Pinch Reactor, CRFPR(20)^{8,9} and CRFPR(5)⁸; Los Alamos Modular Stellarator Reactor, MSR¹⁰; University of Wisconsin Modular Stellarator Reactor, UWTOR-M¹¹; ELMO Bumpy Torus Reactor, EBTR¹²; Mirror Advanced Reactor Study, MARS¹³; reactor based on Ohmically-Heated Toroidal Experiment, OHTE¹⁴; RiggatronTM tokamak¹⁵; Compact Spheromak Reactor, CSR¹⁶; generic fusion reactor, GENEROMAK¹⁷. The engineering power density for a number of fission reactor systems is shown, where the volumes are defined by the primary pressure vessel: Pressurized-Water Reactor, PWR^{18,19}; steam generator for a PWR, SG¹⁹; Superphenix Liquid-metal Fast Fission Breeder Reactor SP²⁰; Advanced Gas-Cooled Reactor, AGR²¹; Magnox Gas-Cooled Reactor, MAGNOX²²; Modular Fast Breeder Reactor, FBR(MRP)²³; Modular High-Temperature Gas Reactor, MHTGR²⁴; Pressurized Heavy-Water Reactor, CANDU²⁵; The cyclone furnace used in a modern coal-fired plant is also shown.²⁶ Except for the modular fission plants, RFPR 750 MWe(net), and NUMAK 660 MWe(net), most systems are in the ~ 1000-1200 MWe(net) class. The smaller numbers in parentheses on the mass-utilization plot give the net plant efficiency, $\eta_p = \eta_{TH}(1 - \epsilon)$, where ϵ is the recirculating power fraction and η_{TH} is the thermal-conversion efficiency. The mass-power-density plot incorporates directly the plant efficiency when expressed on the basis of net-electric power.

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Fig. C-2. Summary of influences on and essential elements of the cost of electricity (COE) for a fusion power plant.

Fig. C-3. Dependence of minimized COE on FPC mass power density for the compact RFP reactor, CRFPR,^{8,9} and an advanced tokamak reactor based on the spherical torus.^{27,28} These designs use resistive copper magnets. Shown also are two superconducting reactor designs; the STARFIRE tokamak³ and the MARS tandem mirror.¹³ The upper plot shows the variation of neutron wall loading for the CRFPR and ATR/ST concepts. The COE-optimized and MPD-maximized MINIMARS results reported in the October 30, 1985 memo are also shown. Numbers in parentheses give the net-electric powers, P_E (MWe).

recirculating power (i.e., added balance-of-plant costs). It should be noted that the neutron wall loading varies along these curves, as shown explicitly by the companion plot included on Fig. C-3. In these computations the blanket/shield thickness is fixed at a minimum value set by tritium breeding and heat-recovery constraints rather than allowing it to vary (i.e., decrease) and produce unreal results (i.e., high refrigeration power for superconducting magnets) for the sole purpose of increasing MPD. The shallow minima shown on Fig. C-3 result from a tradeoff between FPC capital cost, recirculating power, operating costs, and plant availability reflected primarily through increased wall loading for a fixed first-wall/blanket/shield life (MWyr/m^2). Included on Fig. C-3 are the two (interim) MINIMARS points after reducing those COE values from 1984 to 1980 dollars (average annual inflation of 7% giving a conversion factor of 131) to provide a common basis for comparison. Also included on this figure are results from the MARS tandem-mirror¹³ and STARFIRE tokamak³ designs, with the MARS results also appropriately adjusted to constant 1980 dollars.

The reasons why the 1000-MWe(net) CRFPR design selected a nominal 20-MW/m² neutron wall loading and (indirectly) an MPD comparable to a PWR fission reactor ($\sim 1000 \text{ kWe/tonne}$)⁸ warrant restatement. As seen from the attached figure, little COE benefit results from designs for $\text{MPD} \geq 100\text{--}200 \text{ kWe/tonne}$, at least as measured by the present costing models. Furthermore, although meeting crucial engineering constraints,⁸ the $I_w = 20\text{-MW/m}^2$ design, CRFPR(20), pushes technology in the area of high-heat-flux surfaces and high-power-density blankets. The main goal of the high-MPD CRFPR(20) design study^{8,9} was to examine the potential and technical requirements for fusion to provide an FPC that could be installed and maintained rapidly as a single, prechecked, and fully operational unit. That is, with the separated poloidal-field coils ($\sim 800\text{-tonnes}$, total, but movable in quadrants), the remaining reactor torus (i.e., plasma chamber, first wall, limiter, blanket, shield/structure, and toroidal-field coils) would be installed and replaced annually as a single, $\sim 300\text{-tonne}$ (drained of coolant) unit. Furthermore, these smaller units portend significant development advantages expressed in terms of reducing front-end risks, giving R&D flexibility, allowing a bolder fusion development program, and developing an experience base and associated "learning curves" more rapidly for early inclusion into a practical reliability database. This latter advantage remains, even if the commercial end-product should operate with lower MPD and neutron wall loadings than those chosen for the CRFPR(20) design. The ability to

develop rapidly and economically a reliability database addresses what is probably one of two key issues for commercial fusion power: FPC reliability and the ability to recover rapidly and economically from a serious breakdown. The second key issue, the overall cost of fusion development, is also addressed by the higher-MPD approaches. The very same CRFPR(20), TFTR-sized FPC could be operated at lower plasma current and/or beta to give power plants in the range 250-500 MWe, but of course with the usual economy-of-scale penalties are incurred (Fig. C-3); multiplexing the smaller FPC into a larger-net-power plant would, of course, help this problem.⁸ These "derated" CRFPRs, however, would operate with a MPD in the 400-500 kWe/tonne range.

Noted from Fig. C-3 is that the superconducting reactor designs (i.e., MARS and STARFIRE) generally achieve COEs comparable to the copper-coil designs at lower MPD, given that nominally the same cost database is used. The composite FPC unit cost for STARFIRE, MARS, ATR/ST, and CRFPR for $P_E = 1000-1200$ MWe(net) are, respectively, 19, 25, 45, and 42 \$/kg (Table 5.-I); the cost of the FPC and reactor plant equipment (RPE, Account 22.)²⁹ expressed as a percentages of the total direct costs are, respectively, 25.5/56.0, 30.5/64.2, 20.0/51.7, and 4.2/37.3%. The low COE for the low-MPD systems is achieved only for low (composite) FPC unit costs and for both FPC and RPE being a significant fraction of the total direct costs. Hence, in addition to permitting single (or few) piece FPC maintenance, the higher MPD designs downplay the role of the FPC (and associated physics, technology, and costing uncertainties) in the overall cost equation for fusion. These high-MPD designs have a built-in buffer with which to accommodate the surprises that will inevitably occur as fusion is pushed further toward commercial application.

An example of this last point is in order. It is sometimes contended that the low FPC unit costs (\$/kg) reported for the superconducting designs result because the FPC mass for these systems is dominated by relatively inexpensive (stainless steel) shielding. It is not beyond the realm of possibility, however, that once a 10-20 \$/kg stainless-steel shield is fabricated, qualified, installed, operated, and (eventually) removed from the high-priced nuclear zone between the blanket and coils, its unit cost may be much higher than that nominally assumed. If, for example, the FPC unit cost for STARFIRE was doubled to ~ 40 \$/kg, the total direct cost (and COE, everything else held constant) would increase by 30%, giving an added direct capital cost of 0.4-0.5 B\$. In order to regain the cost position originally reported for STARFIRE, the FPC mass

would have to be cut in half (to ~ 12 ktonne), with the MPD increasing from 50 to ~ 100 kWe/tonne. This somewhat simplified example illustrates the desire to achieve MPD values that are sufficient for the FPC to become a relatively minor ($\leq 5-10\%$) component of the total direct cost in order to give some economic (and physics) "breathing room" with which to accommodate minor surprises (i.e., 40 \$/kg rather than 20 \$/kg, 10% beta rather than 12% beta, etc.).

As for most figures of merit, their worth is highest when the merit of the system being evaluated is not as high as it should be. The mass power density for thermal solar is crudely estimated to be in the range 0.5-1 kWe/tonne* compared to ~ 1000 kWe/tonne for a PWR, again using only the mass of the primary heat source in computing the MPD. The fuel-free, reduced-technology, and safety advantages are seriously taxed in making thermal solar a competitive central power energy source today. The MPD for the pioneering UWMAK-I(1974) tokamak reactor design was 20 kWe/tonne; this design gives reasonable values of COE only because of the low FPC unit costs (7 \$/kg in 1980 dollars). The STARFIRE tokamak design projected an MPD of ~ 50 kWe/tonne, as did MARS, with commensurate increases in economic credibility as reasonable (i.e., within a factor of two of the competition) COE values were again reported, but now for unit costs that reflect more the advanced technologies required of fusion. The predictions of competitive fusion for $MPD \geq 100-200$ kWe/tonne by the GENEROMAK studies¹⁷ are in line with this trend of improved economics in the sequence: thermal solar $\rightarrow$ UWMAK-I $\rightarrow$ STARFIRE/MARS $\rightarrow$ GENEROMAK (MINIMARS, TPSS, RFP, ?). The concept of MPD is valuable in tracing this improvement. Beyond this ~ 100-200-kWe/tonne point, the value of MPD as a simple costing guide diminishes and in fact can be misleading, as illustrated in Fig. C-2 and the MINIMARS memo. As is well known, a long list of factors other than MPD enter into the equation for fusion with an economic edge, some being generic (e.g., coil current densities, materials radiation life, inherent safety, radwaste, etc.) and some being device specific (end-cell/central-cell coupling in TMRs, confinement efficiency versus magnet requirements in poloidal-field-dominated systems, plasma current level in driven tokamaks, etc.). The purpose for fusion-reactor conceptual design studies is to bring these other factors into the overall

*1 kWt/m² solar flux, 0.05-m effective thickness of collector (or reflector) and structure, average (smeared) mass density of ~ 2 tonnes/m³, duty factor of 1/3 (or equivalent density of 6 tonnes/m³ including thermal storage and a duty factor of ~ 1), and a thermal-conversion efficiency of 30%.

evaluation of fusion. The MPD, however, remains as one important figure of merit by which to monitor that evaluation.

In summary, the following points are made:

- ♦ Like any single figure of merit, MPD can be misused and misinterpreted. Specifically, it is unwise to maximize MPD at any cost.
- ♦ A wide range of fusion reactor designs indicates strong economic incentive for $\text{MPD} \geq 100\text{--}200 \text{ kWe/tonne}$.
- ♦ Going beyond the limitations inherent to present costing methods used in fusion, unquantified but potentially significant benefits can be identified for operating with MPD beyond the 100–200 kWe/tonne threshold. Specifically, the following potential merits should be considered.
 - reduced impact of physics and technology uncertainties on overall cost of fusion power
 - increased FPC operational flexibility
 - single-piece maintenance
 - ability to sustain and recover from significant FPC breakdowns
 - ability to improve FPC throughout plant life
 - more rapid development of learning curves and a practical reliability database
 - less expensive, bolder, more rapid development path
- ♦ MPD is a good figure of merit for graphically illustrating progress as fusion reactor designs become more realistic in providing an end-product that competes.

REFERENCES FOR APPENDIX C

1. R. W. Conn (Chairman), "Panel X Report to the USDOE Magnetic Fusion Advisory Committee," University of California at Los Angeles (May 8, 1985).
2. B. Badger, M. A. Abdou, R. W. Boom, R. G. Brown, T. E. Chang, R. W. Conn, et al., "UWMAK-I, A Wisconsin Toroidal Fusion Reactor Design," University of Wisconsin report UWFDM-68 (March 1974).
3. C. C. Baker, M. A. Abdou, R. M. Arons, A. E. Bolon, C. D. Boley, J. N. Brooks, et al., "STARFIRE - A Commercial Tokamak Fusion Power Plant Study," Argonne National Laboratory report ANL/FPP-80-1 (September 1980).
4. A. A. Hollis, "An Analysis of the Estimated Capital Cost of a Fusion Reactor," UKAEA Harwell report AERE-R9933 (June 1981).
5. B. Badger, K. Audenaerde, H. I. Avci, J. Beyer, D. Blackfield, R. W. Boom, et al., "NUMAK: A Tokamak Reactor Design Study," University of Wisconsin report UWFDM-330 (March 1979).
6. R. Hancox, R. A. Krakowski, and W. R. Spears, "The Reversed-Field Pinch Reactor," Nucl. Eng. and Design 63(2), 251 (1981).
7. R. L. Hagenson, R. A. Krakowski, and G. E. Cort, "The Reversed-Field Pinch Reactor (CRFPR) Concept," Los Alamos National Laboratory report LA-7973-MS (August 1979).
8. C. Copenhaver, R. A. Krakowski, N. M. Schnurr, R. L. Miller, C. G. Bathke, R. L. Hagenson, et al., "Compact Reversed-Field Pinch Reactors (CRFPR): Fusion-Power-Core Integration Study," Los Alamos National Laboratory report LA-10500-MS (August 1985).
9. R. L. Hagenson, R. A. Krakowski, C. G. Bathke, R. L. Miller, M. J. Embrechts, N. M. Schnurr, et al., "Compact Reversed-Field Pinch Reactors (CRFPR): Preliminary Engineering Considerations," Los Alamos National Laboratory report LA-10200-MS (August 1984).
10. R. L. Miller, C. G. Bathke, R. A. Krakowski, F. M. Heck, L. Green, J. S. Karbowski, et al., "The Modular Stellarator Reactor: A Fusion Power Plant," Los Alamos National Laboratory report LA-9737-MS (July 1983).
11. B. Badger, I. N. Sviatoskvsky, S. W. Van Sciver, G. L. Kulcinski, G. A. Emmert, D. T. Anderson, et al., "UWTOR-M: A Conceptual Modular Stellarator Power Reactor," University of Wisconsin report UWFDM-550 (October 1982).
12. C. G. Bathke, D. J. Dudziak, R. A. Krakowski, W. B. Ard, D. A. Bowers, J. W. Davis, et al., "ELMO Bumpy Torus Reactor and Power Plant Conceptual Design Study," Los Alamos National Laboratory report LA-8882-MS (August 1981).
13. B. G. Logan (Principal Investigator), et al., "MARS: Mirror Advanced Reactor Study," Lawrence Livermore National Laboratory report UCRL-53480 (July 1984).

14. R. F. Bourque, "OHTe Reactor Concepts," Proc. 9th Symp. on Eng. Prob. of Fusion Research II, 1851, Chicago, IL (October 26-29, 1981).
15. R. A. Jacobsen, C. E. Wagner, and R. E. Covert, "Systems Studies of High-Field Tokamak Ignition Experiment," J. Fus. En. 3(4), 217 (1983).
16. R. L. Hagenson and R. A. Krakowski, "Steady-State Spheromak Reactor Studies," 6th ANS Top. Mtg. on the Technol. of Fus. En., San Francisco, CA (March 3-7, 1985), Fus. Technol. 8 (2B), 1606 (1985).
17. J. Sheffield, R. A. Dory, S. M. Cohn, J. G. Delene, L. Parsly, D. E. T. F. Ashby, W. R. Reirsen, "Cost Assessment of a Generic Magnetic Fusion Reactor," Fus. Tech. 9, 199 (1986) [also, Oak Ridge National Laboratory report ORNL/TM-9311 (March 1986)].
18. United Engineers and Constructors, Inc., "1000-MWe Central Station Power Plants Investment Cost Study," USAEC report WASH-1230 (June 1972).
19. A. Walker, "Design of the PWR for Sizewell 'B'," The Nuclear Engineer 24, 6, 176 (1984) [also, UK Central Electricity Generating Board Brochure, "A Technical Outline of Sizewell 'B': The British Pressurized Water Reactor," (1983)].
20. A. E. Waltar and A. B. Reynolds, Fast Breeder Reactors, Pergamon Press, NY 1982.
21. G. C. Dale (ed.) and J. M. Bowerman, "The Safety of the AGR," UK Central Electricity Generating Board and South of Scotland Electricity Board (1982).
22. J. Hall, The Story of the Construction of Berkeley Nuclear Power Station, Leonard Hill Books, London (1963).
23. R. Meyers, "For General Electric Tiny Breeders Are In," The Energy Daily (May 15, 1984).
24. C. F. McDonald and D. L. Sonn, "A New Small HTGR Power Plant Concept with Inherently Safe Features--An Engineering and Economic Challenge," Proc. of American Power Co. 45, 818 (1983).
25. L. R. Haywood, "The Candu Power Plant," Atomic Energy of Canada, Ltd. report AECL-5321 (January 1976).
26. Steam, Its Generation and Use, Babcock and Wilcox, 39th Ed., NY (1978).
27. Y-K. M. Peng, "Spherical Torus, Compact Fusion at Low Field," Oak Ridge National Laboratory report ORNL/FEDC-84/7 (January 1985).
28. Y-K. M. Peng, D. J. Stricker, S. K. Borowski, W. R. Hamilton, R. L. Reid, J. R. Haines, et al., "Spherical Torus: An Approach to Compact Fusion at Low Field - Initial Ignition Assessments," 6th Top. Mtg. on Tech. of Fus. En., San Francisco (March 3-7, 1985), Fusion Tech. 8, 1, Part 2(A), 338 (July 1985).

29. R. E. Allen (Proj. Manager), "Energy Economic Data Base (EEDB) Program," Argonne National Laboratory report UE&C-ANL-82115 (November 1982).

APPENDIX D. OSCILLATING-FIELD CURRENT DRIVE (OFCD)

Section IV-K. of Ref. 1 is given in this appendix in order to conveniently reproduce the derivation and basis of the global current-drive expression given by Eq. (2.4.-8). Oscillating-field current drive as a means of steady-state current drive have been proposed for the RFP,²⁻⁵ and, for the reasons given in Sec. 2.4., application of OFCD to the tokamak is less certain. Unlike the tokamak, the toroidal and poloidal currents in the RFP are closely coupled, and it is possible in principle to create a time-averaged current within the plasma without requiring a time-varying magnetic flux to link the torus. The near-minimum-energy RFP state is defined⁶⁻⁸ primarily by holding the toroidal flux, ϕ , and the magnetic helicity, $K = \int \vec{A} \cdot \vec{B} \, d\tau$, invariant within a conducting shell of radius r_w surrounding the plasma, where $\vec{A}$ ($\nabla \times \vec{A} = \vec{B}$) is the magnetic vector potential and the integration is performed over the volume enclosed by the conducting shell. The magnetic helicity is simply a measure of the degree to which flux tubes within the conducting shell are interlinked. The helicity remains invariant, and a global minimum energy is maintained while an external circuit parameter (e.g., voltage, V_θ , applied to a TFC) is varied to change the toroidal flux external to the plasma. Intrinsic plasma processes related to turbulence and instabilities, therefore, generate toroidal currents within the plasma in order to increase (or reduce) poloidal flux to maintain the helicity constant and the plasma in a minimum-energy state. This nonlinear coupling between plasma and magnetic fields through the Taylor F- θ diagram (Fig. 2.4.-8) can be used to "rectify" current oscillations created at external coils into a net steady-state current within the plasma. This "F- θ pumping" is envisaged to transform toroidal magnetic flux (poloidal currents) into toroidal currents (poloidal magnetic flux) through the plasma relaxation responsible for maintaining the near-minimum-energy RFP configuration. The existence of similar, non-disruptive channels for plasma relaxation in a tokamak configuration is an assumption.

Little analysis and design beyond that described in Refs. 1-5 are available to describe current-drive systems based on OFCD for either experiment or the reactor. This appendix describes the process and develops a methodology by which future engineering evaluations of OFCD can proceed. In developing and evaluating a global picture of OFCD along the lines described in Refs. 2 and 5, the circuit parameters, definitions, and approach summarized in Table D-I are used. The plasma is described in terms of K , ϕ , and ψ_M , and the relationship

TABLE D-I

SUMMARY OF DEFINITIONS AND NOTATION USED TO MODEL
OSCILLATING-FIELD CURRENT DRIVE (OFCD)

<u>Definition</u>	<u>Parameter</u>
Toroidal voltage on plasma	V_ϕ
Toroidal current in plasma	I_ϕ
Poloidal voltage applied to toroidal-field coil	$V_\theta = \partial\phi/\partial t \equiv \dot{\phi}$
Poloidal current flowing in external conductor	I_θ
Plasma resistance	R_p
Magnetic helicity, where $\vec{B} = \nabla \times \vec{A}$	$K = \frac{1}{\mu_0} \int \vec{A} \cdot \vec{B} \, dV_p$
Toroidal flux	$\phi = 2\pi \int_0^{r_w} B_\phi(r) r dr$
Total field energy within conducting shell	$W_M = \int (B^2/2\mu_0) d\tau$
Vacuum toroidal inductance	$L_O = \mu_0 r^2/2R_T$
Inverse aspect ratio	$\varepsilon = r_w/R_T$
Average toroidal field within shell	$\langle B_\phi \rangle = \phi/\pi r_w^2$
Reversal parameter	$F = \bar{B}_\phi(r_w)/\langle B_\phi \rangle = L_O I_\theta / \phi$
Pinch parameter	$\Theta = \bar{B}_\theta(r_w)/\langle B_\phi \rangle = L_O I_\phi / \varepsilon \phi$

between these parameters and the circuit variables (i.e., resistances, inductances, currents, voltages) constitutes the overall current-drive model. The time dependence of K , ϕ , and W_M results⁵ directly from the Maxwell equations:

$$\frac{dK}{dt} = 2\phi V_\phi - 2 \int \eta \vec{j} \cdot \vec{B} dV_p \quad (D-1)$$

$$\frac{d\phi}{dt} = + V_\theta \quad (D-2)$$

$$\frac{dW_M}{dt} = I_\phi V_\phi - I_\theta V_\theta - \int \vec{\eta} \vec{j} \cdot \vec{j} dV_p \quad (D-3)$$

$$\vec{E} = \vec{\eta} \vec{j} - \vec{v} \times \vec{B} \quad (E_\phi = \eta j_\phi - \alpha B_\phi) \quad , \quad (D-4)$$

where the last expression gives an Ohm's law corrected for the plasma dynamo effect. With the plasma resistance defined by

$$I_\phi^2 R_p = \int \vec{\eta} \vec{j} \cdot \vec{j} dV_p \quad , \quad (D-5)$$

the plasma helicity, flux, and energy equations become

$$\frac{dK}{dt} = 2\phi(V_\phi - I_\phi R_p) \quad (D-6)$$

$$\frac{d\phi}{dt} = +V_\theta \quad (D-7)$$

$$\frac{dW_M}{dt} = I_\phi V_\phi - I_\theta V_\theta - I_\phi^2 R \quad , \quad (D-8)$$

where in Eq. (D-1) the helicity dissipative term, $2 \int \vec{\eta} \vec{j} \cdot \vec{B} d\tau$, is given exactly by $2\phi I_\phi R_p$ for the Bessel-function model (BFM) which results for the special, $\beta = 0$ case of $\mu(r) \equiv \mu_0 \vec{j} \cdot \vec{B} / B^2$ a constant. It is readily seen from either Eq. (D-1) or (D-6) that if the time-average $\langle dK/dt \rangle$ is to equal zero, then $2\langle \phi V_\phi \rangle$ must equal the dissipation term, which means ϕ and V_ϕ should be nominally in phase; hence, V_ϕ and $V_\theta \approx \phi$ should be out of phase by 90° .

A statement of W_M and $\dot{W}_M$ in terms of plasma and circuit parameters is given by the following expressions:

$$W_M = \frac{1}{2} L_p I_\phi^2 + \frac{\phi^2}{2 L_o} \quad (D-9A)$$

$$\dot{W}_M = V_\phi I_\phi + V_\theta I_\theta - I_\phi^2 R_p . \quad (D-9B)$$

The plasma inductance, L_p , is a function of the field profiles and, for example, can be derived using the BFM [i.e., $\mu(r) = \text{constant}$] or variants thereof [i.e., $\mu(r) \rightarrow 0$ at cold plasma edge]. Combining Eqs. (D-7), (D-8), and (D-9) and using a positive Faraday's Law (i.e., $V_\theta = + \dot{\phi}$) lead to the following expression for the toroidal voltage around the plasma, V_ϕ :

$$V_\phi = I_\phi R_p + \left[L_p + \frac{\Theta}{2} \frac{dL_p}{d\Theta} \right] \dot{I}_\phi + \left[\frac{1-F}{\epsilon\Theta} - \frac{\epsilon\Theta^2}{2L_0} \frac{dL_p}{d\Theta} \right] V_\theta . \quad (D-10)$$

Adoption of a positive Faraday's Law for the toroidal circuit is based upon a convention that orients ϕ in the same direction as I_ϕ . When the toroidal and poloidal circuits are coupled through the F- Θ diagram, a) toroidal current can be driven by an applied poloidal (TFC) voltage, V_θ , and b) the plasma responds as a nonlinear circuit element through the F- Θ diagram. Sinusoidal variations in V_θ and V_ϕ applied to the TFC and PFC sets thereby couple nonlinearly to drive a net toroidal DC current. In order to solve Eq. (D-10), the plasma inductance, L_p , must be expressed as a function of the pinch parameter, $\Theta \propto I_\phi/\phi$.

The special case of the BFM is amenable to analytic solution and is reproduced here. Using the BFM field profiles [i.e., $B_\theta(r) \propto J_1(\mu r)$ and $B_\phi(r) \propto J_0(\mu r)$] and equating the total magnetic energy stored within the plasma to $\frac{1}{2} L_p I_\phi^2$ give the following expression for L_p :

$$L_p = \frac{2L_0}{\epsilon^2} \left[1 + (2F + 1)(F - 1)/2\Theta^2 \right] , \quad (D-11)$$

where $F = \Theta J_0(2\Theta)/J_1(2\Theta)$ for the BFM, and included in Eq. (D-11) is the energy needed to compress an initially uniform toroidal flux into the $B_\phi(r) \propto J_0(\mu r)$ distribution. The purely poloidal inductance for the BFM is $L_{p\theta} = 2W_{B\theta}/I_\phi^2 = (L_0/\epsilon^2) [1 + (F/\Theta^2)(F - 1)]$. The plasma resistance, R_p , used in Eq. (D-10) is also dependent upon profiles through the factor g_{OHM} , with R_p given by

$$R_p = 2g_{OHM}\langle\eta\rangle/\varepsilon^2 R_T, \quad (D-12)$$

where $\langle\eta\rangle$ is the classical resistivity evaluated at the average plasma temperature. For the BFM and a flat temperature distribution, g_{OHM} is given by

$$g_{OHM} = 2(\Theta^2 + F^2) - F. \quad (D-13)$$

The profile factor, g_{OHM} , is often referred to as the resistance "anomaly" factor, a notation that can be misleading in that as given above g_{OHM} for $Z_{eff} = 1$ is a measure of the extended current path resulting from the "screwing-up" of the high-pitch field lines. For the BFM model without deep reversal ($\Theta < 1.6$), g_{OHM} typically is ~ 3 .

The poloidal- and toroidal-circuit equations as coupled through the F- Θ curve can be solved numerically for I_ϕ once driver functions are selected for ϕ (i.e., $\phi = \phi_0 + \delta\phi \cos \omega t$, $V_\Theta = \dot{\phi} = \delta\phi \omega \sin \omega t$) and V_ϕ (i.e., $V_\phi = \delta V_\phi \cos \omega t$). This procedure must be performed while assuring that the time-averaged magnetic helicity is constant. The constraint that $\langle dK/dt \rangle$ is zero can be used to establish limits on the magnitude of the field oscillations required to sustain a given toroidal plasma current. If the ohmic dissipations for both the induced and driven cases are similar, and if the induced case is characterized by ϕ_0 and $V_{\phi 0}$, then dK/dt for the driven case is given by Eq. (D-6) with $V_{\phi 0} \equiv I_\phi R_p$. Hence,⁴

$$\frac{dK}{dt} \approx 2\phi(V_\phi - V_{\phi 0}). \quad (D-14)$$

Using the driver functions given below,

$$\phi = \phi_0 + \delta\phi \cos \omega t \quad (D-15A)$$

$$V_\phi = \delta V_\phi \cos \omega t, \quad (D-15B)$$

the following expression for the time average of dK/dt for the driven case results:

$$- \langle dK/dt \rangle = \delta\phi \delta V_\phi + 2\phi_0 V_{\phi 0}. \quad (D-16)$$

For $\langle dK/dt \rangle \approx 0$, the following condition on the amplitudes of the toroidal flux and voltage oscillation results:

$$\left(\frac{\delta\phi}{\phi_0} \right) \left(\frac{\delta V_\phi}{V_{\phi 0}} \right) \approx -2. \quad (D-17)$$

Given that toroidal flux oscillations much above $\delta\phi/\phi_0 \approx 0.2-0.3$ are expected to impact seriously the RFP configuration (i.e., loss of toroidal-field reversal), the AC toroidal voltage needed to drive a DC toroidal current with $\langle dK/dt \rangle \approx 0$ can be 6-10 times greater than the voltage needed to sustain an inductively driven RFP current drive. The AC toroidal voltage probably will have to be applied at a frequency that is above $1/(\text{diffusion time})$ but lower than $1/(\text{relaxation time})$. As shown in Sec. 2.4., even more stringent limits on $\delta\phi/\phi_0$ may exist for the tokamak because of the large toroidal fields and realistic constraints on reactive power.

To assess the tradeoff between $\delta\phi/\phi_0$, $\delta V_\phi/\langle I_\phi R_p \rangle$, ω , $\delta I_\phi/\langle I_\phi \rangle$, reactive power, P_R , and the energy stored in the current-drive circuits, E_S , Eq. (D-10) has been solved numerically for BFM profiles and L_p dependence on Θ under the constraint that $\langle I \rangle_\phi$ and $\langle K \rangle$ are zero. The voltage drive functions given by Eq. (D-15) are used, and plasma parameters typical of those given in Ref. 1 are used ($r_p = 0.71$ m, $R_T = 3.8$ m, $T = 20$ keV and flat, $n = 5(10)^{20} \text{ m}^{-3}$, $I_\phi = 18.5$ MA, $\beta_\Theta = 0.25$, $\langle n \rangle = 10^{-10} \text{ ohm m}$). The dependencies of $\delta I_\phi/\langle I_\phi \rangle$, $\delta V_\phi/\langle I_\phi R_p \rangle$, P_R , and E_S on $\delta\phi/\phi_0$ and ω are shown on Fig. D-1.

Equation (D-6) becomes an approximation to Eq. (D-1) if more realistic μ -profiles are used because of the varying μ profile. The conservation of helicity and energy denoted by Eqs. (D-6) and (D-8), therefore, cannot be satisfied simultaneously. A conserving set of equations can be recovered by introducing a multiplicative factor into the $2\phi I_\phi R_p$ term of Eq. (D-6), which contains the appropriate profile weighting. This approach, however, is not warranted since the resistive dissipation described by Eq. (D-5) and appearing in Eq. (D-3) is not classical in nature but results from current fluctuations. The procedure adopted here maintains Eq. (D-6) intact and replaces R_p in

Fig. D-1. Parametric evaluation of Eq. (D-10) for P- Θ pumping current drive using Eqs. (D-15) as drivers and BFM plasma parameters.

Eqs. (D-8) and (D-10) with the product $f_A R_p$, where f_A is a factor giving the ratio of resistance required in the helicity and energy equations. By requiring $\langle K \rangle = \langle I_\phi \rangle = 0$, the factor f_A is determined numerically for the operating conditions other than for the BFM, in which case $f_A = 1$.

It remains for future work to model OFCD using more realistic μ profiles in conjunction with a detailed time-dependent simulation of both plasma and circuit response. The results of such a simulation would be needed to perform a complete circuit design and to integrate the resulting current-drive circuitry realistically into both the FPC mechanical/electrical/thermal design as well as into the overall reactor energy balance. Ongoing work being conducted as part

of the TITAN/RFP reactor study⁹ shows that the impact of shells and driver coils that are less-tightly coupled than reflected by Eq. (D-10) can distort wave forms, give phase lags, and generally increases the reactive power predicted in Fig. D-1 or Fig. 2.4.-6.