142
11-20-74

82-1068

UCRL 51617

CONCEPTUAL DESIGN OF A MICRO REACTOR FOR A FUSION ENGINEERING RESEARCH FACILITY (FERF)

T. H. Betzer, R. C. Burleigh, G. A. Carlson, W. L. Dexter,
G. W. Hamilton, A. R. Harvey, R. G. Hickman, M. A. Hoffman,
E. B. Hooper, R. W. Moir, W. J. Silver, B. R. Smith,
C. E. Taylor, R. W. Werner, and T. P. Wilson

August 28, 1974

Prepared for U.S. Atomic Energy Commission under contract No. W-7405-Eng-48

**LAWRENCE MASTER
LIVERMORE
LABORATORY**

University of California/Livermore

Printed in the United States of America
Available from
National Technical Information Service
U. S. Department of Commerce
5286 Port Royal Road
Springfield, Virginia 22151

 

100

TID-4500, UC-20
Controlled Thermonuclear Processes
and Plasma Physics

LAWRENCE LIVERMORE LABORATORY
University of California/Livermore, California/94550

UCRL-51617

**CONCEPTUAL DESIGN OF A MIRROR REACTOR
FOR A FUSION ENGINEERING RESEARCH FACILITY (FERF)**

T. H. Batzer, R. C. Burleigh, G. A. Carlson, W. L. Dexter,
G. W. Hamilton, A. R. Harvey, R. G. Hickman, M. A. Hoffman,
E. B. Hooper, R. W. Moir, W. J. Silver, B. H. Smith,
C. E. Taylor, R. W. Werner, and T. P. Wilcox

August 28, 1974

—NOTICE—

This report was prepared as an account of work sponsored by the United States Government. Neither the United States nor the United States Atomic Energy Commission, nor any of their employees, nor any of their contractors, subcontractors, or their employees, makes any warranty, express or implied, or assumes any legal liability or responsibility for the accuracy, completeness or usefulness of any information, apparatus, product or process disclosed, or represents that its use would not infringe privately owned rights.

Frontispiece. Model of FFRF reactor separated at midplane, showing superconducting magnet and support structure.

Contents

Abstract	1
Rationale for a Fusion Energy Research Facility (FERF)	1
Plasma Physics Assumptions	2
General Description of Reactor and Facilities	4
Cost Estimate	18
Construction Schedule	18
Safety	21
References	21
 Appendix A -- Plasma Physics	 23
Plasma Parameters	23
Energy Scaling	29
Magnetic Field Scaling	31
Plasma Stability	34
Adiabaticity	34
Startup of the FERG Plasma	36
References	44
 Appendix B -- Neutral Beam Injectors	 45
Selection of Injector Type	45
Injector Layout	46
Gas Pumping Requirements	48
Plasma Source Modules	49
Design Considerations: Acceleration-Deceleration Electrodes	49
Source Reliability	53
Injector Power Supply Requirements	54
Neutral Beam Control Equipment	58
Long-Life Filament Design	59
References	61
 Appendix C -- Wall Bombardment by Neutral Atoms	 63
Introduction	63
Analytic Model	63
Mathematical Description	64
Results of the Analysis	65
Results for FERG	69
References	73
 Appendix D -- Shield Analysis	 75
Introduction	75

Calculational Techniques	75
Geometrical Model	75
Materials Selected for Model	78
Results of Analysis	78
Shut-Down Activities and Dose Rates	83
Conclusions	85
References	86
 Appendix E -- Remote Assembly/Disassembly Procedures	 87
Objectives	87
General Requirements for Handling Systems	87
Translational Movement Using Integral Dollies	87
Use of an Overhead Crane	88
Remote Positioning and Clamping	89
 Appendix F -- Safety Analysis	 93
Philosophy	93
Source of Hazards	93
Tritium Use	93
Radiation Levels	94
Accident Analysis	95
Applicable Regulations	96
Environmental Assessment	97
Conclusions	97
References	97
 Appendix G -- Tritium	 99
Introduction	99
Fuel Cycle	99
Tritium in Magnet Shields	102
Containment of Tritium	103
Monitoring and Personnel Protection	109
Conclusions	110
References	110
 Appendix H -- Magnet Design	 113
Winding Configuration	113
Structural Design	114
Fabrication of Windings	120
Magnet Power Supply	121
 Appendix I -- Vacuum Pumping Requirements	 123

Injector Pumping System	123
Mirror Chamber Pumps	123
Recycling Pump	124
Appendix J – Heat-Flux Capability of the First Wall	125
Definition of the Problem	125
First-Wall Concept	125
Thermal Stress Limitation	125
Transient Behavior	127
Pressure Drop and Pumping Power Constraints	128
Design for FERF	129
Critical Heat Flux	130
Comparison of Materials	132
Effect of Parameter Variations	132
Conclusions	132
Annex 1 – Equations Used in This Study	133
Annex 2 – Method of Solution for the Maximum Allowable Heat Flux	135
Annex 3 – Example Calculation	136
References	138
Appendix K – Building Complex	139
Building Functions	139
Descriptions of Principle Areas	139
Site Requirements	144
Appendix L – Details of Reactor and Reactor Facilities Costs	145
Cost Estimating Methods	151

CONCEPTUAL DESIGN OF A MIRROR REACTOR FOR A FUSION ENGINEERING RESEARCH FACILITY (FERF)

Abstract

A small non-power-producing mirror reactor has been designed for testing and evaluating the materials and components which will be used in early fusion reactors. This small fusion reactor must produce a maximum useful 14-MeV neutron flux in a volume which is large enough to accommodate many engineering-scale test specimens, yet small enough to have reasonable cost. The required technology must be close to present practice, and the required extrapolation of plasma physics beyond present experimental knowledge must be minimal. The fusion engineering research facility (FERF) described in this

report produces about 1.1×10^{14} neutrons $\cdot \text{cm}^{-2} \cdot \text{s}^{-1}$ at the edge of the plasma for the "low-field" coil (with Nb-Ti superconductor) although about 3.4×10^{14} neutrons $\cdot \text{cm}^{-2} \cdot \text{s}^{-1}$ would be possible in a "high-field" version (with Nb₃Sn superconductor). The plasma radius is 25 cm at the center. With 13 MW of injected neutral beam, a fusion power of 3.4 MW is produced. The facility is capable of injecting 37 MW of beam in case end losses are greater than predicted. A tritium processing and containment system for the facility has been designed.

RATIONALE FOR A FUSION ENGINEERING RESEARCH FACILITY (FERF)

It is widely recognized that fusion reactors will create a very severe environment for construction materials. Just how severe is not known because there is little directly applicable experience. However, it is known that the neutron radiation damage in metals caused by each 14-MeV fusion neutron will be much greater than that caused by much lower-energy fission neutrons. The 14-MeV neutron fluxes anticipated at the first wall of conceptual fusion reactors range from 2×10^{13} up to 8×10^{13} n/cm². At lower energies, up to 10^{15} n/cm² are expected.

If it is assumed that the radiation damage leading to metal embrittlement is proportional to the displacements per atom, then the damage rates for a given metal in a fusion reactor spectrum can be compared to those in other energy spectra. For example, Logan, et al.,¹ estimate that 4.7 times more damage is produced in niobium by a 14-MeV neutron than by the EBR-II spectrum. In a fusion reactor, the uncollided, 14-MeV neutron flux at the first wall is accompanied by scattered neutrons which have a wide energy range depending on the specific design

of the neutron-absorbing blanket. For a typical blanket,² the total flux may be 4.3 times the uncollided 14-MeV flux from the plasma and the corresponding damage rate in niobium (per source neutron) is 10 times that expected (per incident neutron) in EBR-II. Another comparison, using somewhat different calculation techniques and a different reactor-blanket model,³ predicts 22 times greater damage rate (per source neutron) for 316 stainless steel than would be expected (per incident neutron) in EBR-II. It is apparent that a first-wall, 14-MeV neutron flux of 10^{14} n/cm² $\cdot \text{s}$ will result in a higher rate of damage than is possible in any existing or planned fission reactor. It is also possible that this damage may be of a different nature.

There are ways to simulate the neutron environment in a fusion reactor. For example, neutrons for simulation experiments can be produced by bombarding a tritium target with a deuteron beam from an accelerator. However, the intensity of such sources is limited by the ability of the target to carry away the heat deposited by the beam. The most intense source of this type now in existence, the rotating target neutron source (RTNS) at LLL⁴, produces about 4×10^{12} n/cm² $\cdot \text{s}$ at small samples. An improved version is now

being developed which will provide a flux in excess of 10^{13} n/cm²-s. Other beam-target devices using deuterium gas targets are being considered for this purpose by the AEC's Division of Controlled Thermonuclear Research; however, all such devices will have a very limited source size because of heat-dissipation problems. For example, a beam-target device yields about 3×10^9 14-MeV neutrons per joule of beam energy. Technical problems of heat dissipation would probably prohibit the operation of such a device because a spherical source with 1 m² of surface area and a 14-MeV neutron current of 10^{14} n/cm²-s would require 300 MW of beam power. By contrast, a plasma-confinement device such as the FERF described in this report will produce the same results (assuming classical ion confinement) with a beam power of about 8 MW.

First-wall surfaces will also be bombarded with energetic ions and neutral atoms ranging up to several hundred keV of D and T, with He⁺⁺ from the DT reaction up to 3.5 MeV, and with intense electromagnetic radiation ranging up to ~10-keV x rays. Small-scale experiments using the focused beams of particle accelerators are being used to study several of these effects. However, it is difficult to predict the effects of simultaneous neutron and atom bombardment: FERF would make these studies possible on a large scale.

Materials for use in fusion reactors should be tested as soon as possible using an actual reactor. The lesson has been learned because of serious material problems (e.g., swelling of core materials) in fission power reactors that were not recognized until high power-density reactors were built. These problems were not observed in small-sample testing. In-pile testing started many years ago on several materials now being considered for use in fission reactors: radiation-damage effects should also be simulated on the engineering scale required for reliable design of large fusion-power plants.

In addition to irradiation of materials, FERF would serve as a proving ground for engineering a complete, integrated, tritium-handling system, large superconducting magnets, and high-power neutral-beam injectors. All of these will probably be essential parts of full-scale fusion-power reactors of either the toroidal or mirror type.

On the pages that follow, we describe a small, non-power producing, mirror reactor designed for use in the testing and evaluation of materials and components for use in early fusion reactors. FERF must produce a maximum useful 14-MeV neutron flux in a volume which is large enough to accommodate many engineering-scale test specimens, yet be small enough to have a reasonable cost. The required technology must be close to present practice, and the required extrapolation of plasma physics beyond present experimental knowledge must be minimal. The FERF design described here is not completely optimized, but it is believed to have near-optimum physics parameters.

The report is divided into two presentation modes. The first contains a summary of the study, and brings up-to-date the contents of an earlier, preliminary summary.⁵ Appendices contain detailed information on each aspect of the study, including the reactor and its components, auxiliary facilities and buildings, relevant physics, safety considerations, preliminary development required prior to construction, estimated costs, and construction schedule.

PLASMA PHYSICS ASSUMPTIONS

We have assumed that a mirror-confined plasma can be produced which satisfies plasma equilibrium and stability considerations. The design presented in this report comes very close to satisfying known stability conditions. (Future designs will show a slightly modified coil shape which will stabilize interchange modes by producing a greater radial magnetic well depth.) The current design allows for an injected beam current about three times greater than would be required to sustain classical ion-ion collision losses. The degree to which microinstabilities might affect these end losses is not well known. A major goal of present experimental and theoretical efforts is to learn how these losses vary with energy and other confinement parameters. Because of the need for a maximum production of neutrons, parameters have been chosen to maximize neutron flux at the plasma surface. This leads to much lower energies and lower power gain than would exist in a power reactor design. The main plasma physics

considerations detailed in Appendix A may be summarized as follows:

$$P = \frac{(\bar{\sigma v})_{DT} E_f}{4} \int_V n^2 dv,$$

where n = ion density (we have assumed $n_T = n_D = 1/2 n$),

E_f = fusion energy = 17.58 MeV, and

$(\bar{\sigma v})_{DT}$ = reaction rate parameter averaged over the ion velocity distribution.

Except for the small fraction of beam lost by charge-exchange processes, the injected beam current approximately equals the current of ions lost from the plasma by coulomb collisions; that is,

$$I_{\text{loss}} = \frac{q}{n\tau} \int_V n^2 dv$$

where τ = mean ion lifetime

To maximize fusion power, we must maximize $(\bar{\sigma v})_{DT} n^2$. For a fixed maximum field limited by superconductor technology, $n^2 \propto 1/T^2$. $(\bar{\sigma v})_{DT}$ also decreases with temperature below 100 keV, but much more slowly than with T^2 . Therefore, over a large temperature range the minimum plasma temperature will result in maximum fusion power and, therefore, maximum neutron flux. However, a practical minimum of about 65 keV is dictated by practical limits placed on beam power, beam power density, beam penetration of the plasma, and wall bombardment by charge-exchange neutrals.

The power gain, Q , of concern to reactor designers is

$$Q = \frac{P}{I_{\text{loss}} W_{\text{inj}}} = \frac{(\bar{\sigma v})_{DT} (n\tau) E_f}{4 W_{\text{inj}}},$$

where W_{inj} is injection energy.

For a vacuum mirror ratio of 2, central vacuum magnetic field of 3.75T, injected ion energies of 65 keV for D^0 and 97.5 keV for T^0 , and injection

direction determined by geometrical constraints, we can calculate the quantities which give neutron production rate and injector power requirements: using the following constants:

1. n is limited by the mirror instability which, for our parameters, is $\beta < 0.65$ where $\beta = P/B_0^2/2u_0$
 P = total plasma pressure perpendicular to the magnetic field
 B_0 = vacuum central field.
2. End losses are determined from Fokker-Planck computations, (both one-dimensional multispecies and two-dimensional, single-ion species) give $n\tau$, $(\bar{\sigma v})_{DT}$, and the exact relationship between P and n_0 where n_0 = central density.
3. The axial density profile is also determined from these Fokker-Planck calculations. The radial density profile is symmetrical at midplane and is assumed to be cubic in r . This profile compares well with calculated equilibrium profiles which account for neutral beam input and charge-exchange loss processes.
 Thus, $\int n^2 dv$ is determined and the resulting plasma parameters are summarized in Table 1.

Three classes of instability are considered: Mirror modes, which limit β to less than ~ 0.65 according to present theory; interchange modes, which are suppressed by the sufficient depth of the radial magnetic well produced by the Yin-Yang coil; and microinstabilities, which could cause enhanced scattering losses. Our design allows for enhanced scattering by as much as a factor of three over the classical rate. Recent theory and experimental results on 2XII support the expectations that scattering losses will be within this range. A major goal of present mirror experiments is to verify the expected decrease of scattering losses with increasing temperature.

The size and strength of the FERF magnetic field is sufficient to contain the plasma adiabatically. A small nonadiabatic loss will occur from the high-energy "tail" of the T^+ distribution. The 3.5-MeV He^{++} fusion-produced ions will be rapidly lost and will not contribute to plasma heating. (The He^{++} would be contained in a device greater than about two times larger than FERF.)

Startup will proceed from neutral beam bombardment of a small ($\sim 90\text{g}$) target plasma. To provide sufficient target lifetime and buildup rate, this target

Table 1. Plasma Parameters (Low-Field Case).

Magnetic field intensities:

$$B_{\text{vac center}} = 3.75\text{T}$$

$$B_{\text{center}} = 2.22\text{T}$$

$$B_{\text{mirror}} = 7.5\text{T}$$

$$B_{\text{conductor}} = 9.0\text{T}$$

$$\beta = 0.65$$

$$\text{Vacuum mirror ratio, } (R_{\text{vac}}) = 2.0$$

$$\text{Actual mirror ratio } (R) = 3.38$$

$$\text{Injection energy } (W_{\text{inj}}) = 65\text{-keV } D^0, 97.5\text{-keV } T^0$$

(equal ion velocities for D^0 and T^0)

$$\text{Injection current } (I_{\text{inj}}) = 364\text{-A } D^0, 136\text{-A } T^0 = 500\text{A}$$

($I_{\text{classical}} = 136\text{A}$)

$$\text{Injection power } (P_{\text{inj}}) = 36.9 \times 10^6 \text{ W}$$

$$\text{Density at center } (n_{\text{center}}) = 2.95 \times 10^{14} \text{ cm}^{-3}$$

($n_D = n_T$)

$$\text{Deuteron energy in plasma } (W_D) = 73 \text{ keV}$$

$$\text{Triton mean energy in plasma } (W_T) = 93 \text{ keV}$$

$$\text{Electron mean energy in plasma } (W_e) = 7.2 \text{ keV}$$

$$\text{Plasma radius } (r) = 25 \text{ cm at midplane}$$

$$\text{Plasma length } (L) = 420 \text{ cm between mirrors}$$

$$\text{Fusion power density } (P_0/V) = 60 \text{ W} \cdot \text{cm}^{-3}$$

$$\text{Total fusion power } (P) = 3.38 \times 10^6 \text{ W}$$

$$\text{Fusion power/injected trapped power capability of}$$

this design (Q) = 0.11 (based on 17.58 MeV per fusion event) the corresponding $n\tau = 1.9 \times 10^{12} \text{ cm}^{-3} \cdot \text{s}$

$$\text{Fusion power/injected trapped power for classical}$$

end-loss process ($q_{\text{classical}}$) = 0.33 (based on 17.58 MeV per fusion event) the corresponding $n\tau_{\text{classical}} = 5.8 \times 10^{12} \text{ cm}^{-3} \cdot \text{s}$, where τ = ion confinement time

$$\text{Ion gyroradii at plasma center:}$$

$$a = 2.45 \text{ cm for } 75\text{-keV } D^+$$

$$a = 3.39 \text{ cm for } 93\text{-keV } T^+$$

$$a = 12.0 \text{ cm for } 3.52 \text{ MeV } H^{++}$$

$$r_p/a_{D^+} = 10.2$$

$$L/a = 171$$

$$n/B^2 = 6.0, \text{ where } n \text{ is in units of } 10^{13} \text{ cm}^{-3} \text{ and}$$

$$B \text{ is in } 10^4 \text{ G}$$

plasma must have approximately the following characteristics: density of $2 \times 10^{13}/\text{cm}^3$, $\tau = 160 \mu\text{s}$ ($n\tau = 2 \times 10^9$), and ion temperature of $\sim 1 \text{ keV}$. This plasma is similar to that already obtained in 2XII. It can probably be created by several techniques, one of which is pellet heating using a 1-kJ CO_2 laser.

During buildup and also during steady-state, the outer surface of the plasma is continuously bombarded by neutral gas from two sources: some streams through the neutral beam injection ports; and some results from the diffusion of DT gas out of the first wall where it is continuously being implanted as fast neutrals from charge-exchange reactions.

A calculation model of the buildup assumes that the plasma is a nest of concentric shells of finite thickness and constant density and includes the following effects: mixing effect of finite Larmor orbits, radial diffusion, scattering into the mirror-loss region of velocity space, beam attenuation, and charge exchange. Results show that the plasma can withstand these erosion effects; however, the ability of the FERF low-energy plasma to "burn-out" incoming gas is near the limit.

The low energy chosen for this plasma to maximize fusion power density results in a high flux of neutral atoms from charge exchange between the beam and the plasma ions. This effect is discussed in more detail in Appendix C.

About 5.9 MW of D^0 and T^0 reach the first wall. This results in non-uniform distributions of both heat deposition and sputtering. The maximum flux is $7 \times 10^{16} \text{ atoms/cm}^2 \cdot \text{s}$, which corresponds to a heat flux of $\sim 800 \text{ W/cm}^2$. The first wall must also occasionally withstand the neutral beam bombardment for short periods of time during startup or when the plasma is extinguished. The way the first wall is designed to withstand this bombardment is discussed briefly on page 6 and in more detail in Appendix J.

GENERAL DESCRIPTION OF REACTOR AND FACILITIES

Figure 1 shows a general layout of the reactor with the principle components identified. In the following pages, we will discuss the major components of the

Fig. 1. Vertical cross section through reactor.

reactor, including the superconducting coils, coil support structure, first wall, facilities for sample irradiation, shielding, neutral beam injectors, plasma collector plates, outer tank, maintenance facilities, reactor building complex, and tritium system.

Superconducting Coil System

The Yin-Yang coil shape (Fig. 2) is one of the most efficient in terms of providing a hydromagnetically stable magnetic well with a reasonable mirror ratio. It also provides a natural beam injection access between the two coils and facilitates the assembly and removal of the first wall. The conductor must be rather carefully distributed to minimize the magnetic field at the superconductor. Details of magnetic design, structural design, and electrical design are given in Appendix H.

For the low-field case of a 9.0-T maximum field at the conductor, Nb-Ti superconducting material can be used if it is refrigerated to about 3.5 K; also, a dual conductor system using Nb₃Sn in the higher-field regions is probably practical. For the high-field case of 12.0-T maximum, at least one third of the conductor must be Nb₃Sn or equivalent high-field material. A multifilamentary Nb₃Sn material, which promises to be more practical than present-day thin tape, is under active development and would probably be used throughout the entire coil. A detailed design of the conductors has not been made; however, enough space is allowed for an overall current density of 2700 A/cm² (for the Nb-Ti case). A preliminary structural design shows that support of the enormous magnetic forces is possible for both the low- and high-field cases using the structural system shown in Fig. 3. As shown in Fig. 4, the windings are enclosed in a massive, stainless-steel-coil tank. This coil tank is in turn enclosed in a thermal shield consisting of a nitrogen-cooled copper plate and a multilayered reflective heat shield. The coils, coil structure, and multilayer insulation are contained in the main tank, shown in Fig. 5, which is evacuated to minimize heat leak.

Coil-Support Structure

Enormous magnetic forces are produced by the coils. One component of these forces tends to straighten out the arc of the coil into a straight line.

This force can be resisted by the coil tank itself. Another component of these forces tends to open up the coil gap. Because it is impractical to make the coil tank strong enough in itself, massive C-clamps (Fig. 3) are provided to resist this force. These C-clamps are fabricated of stainless steel plates bolted together. For convenience in handling, each C-clamp may be divided into several subassemblies.

There is also a large force urging the two coils toward each other. This force is resisted by a double-walled, conically shaped, intercoil structure (see Fig. 3), which is pierced by holes for the four injectors and by holes for probes.

Both the C-clamps and the structure between the coils are cooled to the same temperature as the coils. Therefore, these members must also be surrounded by the same type of thermal shield as the coil. This arrangement eliminates the problems related to differential thermal contraction and the transmission of large forces across thermal barriers.

Coil parameters are given in Table 2. For the low-field case, the conductor weight is 7.3×10^4 kg, and the weight of the coil case and structure is 8.6×10^5 kg. The total heat input is 10 kW: 6.3 kW from radiation heating in the coil and structure, 2.5 kW from heating in joints, 800 W from thermal radiation and conduction through supports, and 400 W from transfer lines and current leads. About 3 weeks will be required for cool-down.

First Wall

The purpose of this first wall is

- To stop the high-energy particles which escape from the plasma across the magnetic field and to remove the heat deposited by these particles. This flux consists primarily of D⁰ and T⁰ from charge exchange between injected beam and plasma and He⁺⁺ from DT fusion reactions.
- To provide a high-vacuum environment for the plasma.
- To contain tritium within a prescribed volume.
- To withstand (occasionally) neutral beam bombardment for short periods of time during startup or when the plasma is extinguished.

Fig. 2. Perspective view of coils and plasma.

Fig. 3. Perspective view of coil support structure.

Fig. 4. Cross section of coil and shielding.

Fig. 5. Perspective view of coils in tank.

Thermal stresses are important and a comparison of several wall materials shows that refractory metals such as Mo-0.5% T and Ta-10% W are desirable, although an aluminum alloy such as 6061 may also be possible. The first-wall heat-transfer and thermal-stress problem is discussed in considerable detail in Appendix J.

The first wall consists of two layers (see Fig. 6). The inner layer bears the brunt of the heat flux and the outer layer provides a vacuum-tight barrier to prevent tritium contamination of the coils, coil structure, and sample tubes. Both layers carry cooling water.

The inner layer of the first wall, the layer next to the plasma, consists of a close-packed array of small (~1-cm diameter), thin-walled tubes. The heat flux is, of course, highest near the center of the machine; therefore, the tubes here are small in diameter and the velocity of the water is necessarily very high. Away from the center, the heat flux is lower; therefore, the diameter of the tubes may be increased and the velocity of the water and consequently the required pressure and pumping power thereby reduced. As the perimeter of the first wall increases with distance from the center, the increase in tube diameter can be accommodated while the tubes are still kept in one layer. Because of the thermal stresses involved, it appears necessary to keep these tubes adjacent but

Table 2. Coil parameters.

	Nb-Ti	Nb ₃ Sn
Central field (T)	3.7	5.0
Mirror field (T)	7.5	10.0
Max. field at conductor (T)	9.0	12.0
Ampere turns	17.6×10^6	23.5×10^6
Ampere-meters	5.6×10^8	7.5×10^8
Current density (kA/cm ²)	2.7	3.6
Total force between coils (F _z) (10 ⁶ lb)	11.0	21.2
Total force normal to flat sections (F _v) (10 ⁶ lb)	~120	~210
F _x (10 ⁶ lb)	44	78
Minimum conductor radius (cm)	55	55
Minimum radius from reactor center to conductor (cm)	153	153
Maximum distance from reactor center to conductor (cm)	370	370
Distance between mirrors (cm)	500	500
Stored energy (10 ⁹ J)	1.4	2.5

not continuously attached. Therefore, a vacuum-tight tritium barrier need not be provided by this layer alone. The outer layer of the first wall is made of extrusions welded together lengthwise to provide a vacuum barrier. These extrusions have integral passages for cooling water.

Because of the extremely severe conditions to which the first wall is subjected, it will be periodically replaced. (In this sense, the first wall can be regarded as a very large irradiation sample.) However, because of its shape the first wall cannot be inserted into or withdrawn from the coils in one piece. The first wall is therefore split on the center line. The joint between the two halves would be very difficult to make up or take apart. This joint is therefore, in effect, carried right out to the joint between the two halves of the outer tank by means of two sheet-metal diaphragms. When the two halves of the outer tank

Fig. 6. Relationship of plasma, first wall, sample tubes, and coil shielding at center.

(and the coil and first wall contained therein) are pulled apart, the joint between the first wall and the diaphragm becomes accessible. Each half of the first wall may then be withdrawn through its respective coil gap. The sample tubes and a portion of the coil shielding must also be withdrawn at the same time as one subassembly. This subassembly is cantilevered from a section of tank intermediate between the outer tank proper and the expander tank. This intermediate tank also carries all the connections to the sample tubes and the first-wall tubes.

Facilities for Sample Irradiation

Radial Probes

It must be possible to expose samples to the plasma without an intervening first wall. As noted above, the first wall itself may be regarded as a sample. In addition, radiation sample probes are provided which pierce the first wall. One such probe, shown in Fig. 7, penetrates the C-clamps and enters the plasma zone through

the gap between the coils. The probe is a tube with a detachable sample at the end. The tube contains cooling lines and instrument leads. The probe may be withdrawn through an airlock without losing vacuum in the outer tank. An outer tube around the probe provides an inner vacuum sheath continuous with the first wall. Similar probes (not shown in the drawing) may be inserted into the machine between the two coils in a manner similar to that shown for the injectors. These latter probes would be positioned between the four injectors and would enter the inner zones through holes in the structure separating the two coils (see Fig. 3).

Fig. 7. Detail from Fig. 1 showing typical sample probe penetrating coil, shielding, and first wall.

Tubes

In addition to radial probes which penetrate the shielding and which may accommodate more complex instrumentation and coolant circulation, the entire volume between the vacuum wall and coils can be used for experimental access. Enough shielding must be retained to protect the coil. The design presented here has an experimental volume surrounding the plasma of about 1 m^2 by about 10-cm thick. This region can be modified to accommodate special requirements; however, for this study we have assumed that this space is nearly filled with a general-purpose array of about 60 5-cm-diameter sample tubes (Fig. 6). Samples to be irradiated can be inserted at the ends of the tubes (which may extend into a shielded area) and then positioned near the plasma region.

Two types of sample tubes are provided:

- "Blind" tubes provide straightforward insertion and removal of samples. As shown in Fig. 8, each tube consists of an inner and outer tube to provide coolant passages. A number of samples may be linked together and withdrawn by a suitable rod. Because these tubes terminate inside the biological shielding, appropriate manipulators must be used to load and unload them.

- "Through" tubes, shown in Fig. 9, are also immediately adjacent to the first wall. They are loaded in a manufacturing assembly area and unloaded in a disassembly and hot-cell area located outside the biological shielding. Thus, they have the advantage of being loaded and unloaded while the reactor is in operation. However, as the tubes unavoidably make a number of bends, sample carriers must have rolling contact. These sample carriers are strung like beads on a continuous cable (Fig. 8).

Shielding

The radiation shield protects the superconducting coils from excessive heat input and from radiation damage. At the same time, the shield must be as thin as possible so that the magnet size and cost can be

Fig. 8. "Blind" tubes and typical sample carrier.

Fig. 9. "Through" tubes and typical sample carrier.

minimized. In general, the shield is designed to cover the magnets with, first, a sheet of Boral.* The Boral prevents thermal neutrons from entering the magnet and then producing energy due to (n, γ) reactions. This sheet is followed by a 50-mm-thick lead layer to attenuate the gamma rays striking the magnets. The

*"Reference to a company or product name does not imply approval or recommendation of the product by the University of California or the U.S. Atomic Energy Commission to the exclusion of others that may be suitable."

main shield is then constructed to fill all the available space except for room for a test region (in which samples will be irradiated) next to the plasma.

Initial one-dimensional calculations indicated that a shield composed of 20 vol% borated water and 80 vol% stainless steel produced the minimum heat deposition in the magnet of all homogeneous shields composed of these two materials. (Subsequent work indicated that this composition was also near optimum for the three-dimensional shield.) The maximum 14-MeV neutron fluence in the magnet using this

shield is approximately twice that of a 100 vol% stainless steel shield but is still low enough to satisfy radiation damage criteria.

The neutron flux in the coil has an energy distribution approximately as shown in Fig. 10. Based on rather preliminary data on deterioration of superconducting properties in a "fast" neutron flux ($E \approx 1$ MeV), the shielding should be adequate. However, to properly select a shield design, more extensive data obtained under realistic operating conditions are needed on damage by high-energy neutrons.

Calculations were performed with an LLL version of the MORSE Monte Carlo Code using a 45-group, coupled, neutron-gamma cross-section set. Results for cases where the maximum uncollided neutron flux density in the test region was equal to 10^{14} n/cm²-s indicated the total energy deposited in the magnets and their steel covers to be 5.2 kW (0.011 MeV per source neutron). For the same case, 1.17 MW was deposited in the test region and 6.44 MW in the shield.

Shutdown dose rates and radioisotope activities throughout the system due to neutron interactions were calculated using coupled neutron-gamma cross-section sets in which the coupling (n, γ) cross sections represented the activation cross section. A time-dependent correction is then applied to the results to account for the reactor's operating history and the time shutdown. After one year's operation, the test

Fig. 10. One-dimensional calculations of the neutron spectrum in the magnet.

region contains 176 Ci of gamma emitters with half lives greater than 50. The shield contains 4.7×10^6 Ci, the lead 90 Ci, the magnet case 25 Ci, and the magnet itself 1 Ci. If no additional shielding is provided to prevent radiation from leaking out of the reactor, the dose rates outside the system could be as high as 12,000 rem/h immediately after shutdown and up to 100 rem/h 30 days after shutdown.

From a shielding point of view, the conceptual FERF design seems to be a workable design, and the required shield characteristics can be achieved within the system limitations.

Neutral Beam Injectors

The injection system is described in Appendix B and is capable of a nominal 500 A, 360 A of 65-keV D⁰ and 140 A of 100-keV T⁰; this is over three times the current required to sustain classical mirror losses and thus represents some degree of allowance for possible enhancement of mirror losses above the "classical" calculated rate.

The beam power density selected, 4 kW/cm², has been achieved in present sources; however, beam access is restricted by the magnet windings, magnet support structure, and shielding. Therefore, an array of converging beams is used to pass through the 20 X 20 cm reactor-beam access holes. There are four beam lines with a 2 X 2 m array of source modules located about 4 m from the plasma (as shown in Fig. 11). Positive ions are extracted from a multi-aperture arc chamber and neutralized by D₂ and T₂ gas flowing from the source modules through ducts of appropriate length. See Fig. 12 for an example (scaled from an existing LLL source) of an arc chamber and extraction electrodes. Figure 13 shows a conceptual layout of the neutralizer and columnator arrangement.

The large amount of neutral gas, about 3200 A, produced by the injectors is adequately pumped in two stages by cryopump panels which allow about 10 A to stream directly into the plasma. A nitrogen-cooled panel and multiple heat shields are provided outside the cryopumping surface to minimize the heat loss, as shown in Fig. 14.

The cryopumping surface must be periodically degassed. To perform this 2- to 3-minute degassing cycle, the ion sources are turned off and the flapper valve

Fig. 11. Detail from Fig. 1 showing typical section of one of the injector points.

near the inner end of the injector chamber is closed. (Because the reactor is designed to run on three injectors, any one injector may be turned off while the reactor is running.) The gate valve at the outer end of the chamber is opened to the line to the mechanical pumps, and the cryopumping surface is allowed to warm up to drive off the condensed gas. When this is completed, the gate valve is closed, the

cryopumping surfaces cooled, the flapper valve opened, and the ion sources turned back on. This process is automatically sequenced for all four injectors. Pumping calculations are given in Appendix I.

The ion sources must also be periodically replaced. To do this, the flapper valve is closed to maintain the main vacuum and to minimize the escape of tritium into the vault. The sources are handled directly by the crane or by dollies and crane.

Each injector assembly consists of 28 source modules and accompanying neutralizing chambers. The module design was obtained by scaling the performance of existing LLL 50-A, 20-keV pulsed sources and allowing for appropriate de-rating for continuous operation. The total injector power required, including about 25 MW of arc and filament power, is 122 MW.

Plasma Collector Plates

The leakage flux from the plasma expands outwardly along the magnetic field lines and is collected on plasma collector plates. These are simply water-cooled target plates. In a power-producing reactor, the energy carried by this leaking plasma would be recovered at high efficiency by utilizing electrostatic deceleration in a "direct converter".⁶ The added cost and complexity of a direct converter is not justified for FERF because of the small amount of power to be recovered approximately 13 to 37 MW). However, the feasibility of a simple, direct converter for a FERF is being investigated in a separate study.

The louvered geometry of the plasma collector plates is advantageous because it presents an extended area to the leakage flux and thus reduces the heating problems. At the same time, it presents a surface which is transparent to the large amount of gas which is produced when the charged particles are neutralized. Cryopumping is also used in this region. In this case, the cryopumping chambers are isolated for defrosting by "jalousie" valves as shown in Fig. 1. These valves simply consist of a number of rotatable shutters. In contradistinction to the flapper valves on the injectors, the jalousie valves do not need to be vacuum tight because this chamber is always under vacuum.

Figure 15 shows details of the plasma collector plates. The front face of the plate, which sees the incoming leakage flux, is water-cooled to remove the

0 5 10
cm

Fig. 12. Arrangement of ion source and neutralizing chamber.

Fig. 13. Conceptual layout of neutralizer and columnator arrangement.

Fig. 14. Detail of cryopumping panel.

heat. The back is nitrogen-cooled to reduce the heat load on the cryopumping surfaces. A multilayered reflective heat shield is placed between the water-cooled front face and the nitrogen-cooled back face to reduce heat transfer between the two.

The vacuum lines from all the cryopumping chambers, both for the injector and plasma collector plates, are manifolded together into one line to the mechanical pumps. Because only one cryopumping chamber will be defrosted at a time, this common line need be no larger than that required for one chamber. Cryopumping calculations for the plasma collector plates are shown in Appendix I.

Outer Tank

Although the first wall is a vacuum barrier, because of its shape and necessarily flimsy construction it cannot support atmospheric loading. The outer tank, shown in Fig. 5, provides a vacuum outside the first wall and thus removes the pressure loading on the first wall. The outer tank also provides the insulating vacuum for the superconducting coils.

Fig. 15. Detail of plasma collector plate.

Maintenance Facilities

Immediately after shutdown following 1 year of continuous operation, the dose rate at a representative location in the reactor room is 400 rem/h, decreasing to 100 rem/h in 30 days (see Appendix D). Disassembly and maintenance operations will, of course, require special equipment, manipulators, movable shields, and tools. An operator entering the vault must be enclosed in, say, a few inches of lead. This is true not only for major disassembly, but also for routine maintenance. During routine maintenance, the tritium contamination may be such that with proper purging of the atmosphere, special precautions may not be needed. However, during major disassembly it will be necessary to provide tritium protection also. This will be true even if the interior is first heated under vacuum to drive off the absorbed tritium. We plan, then, that operators must not only be provided with lead shielding, but also possibly with a source of air for breathing. Appendix D discusses residual radioactivity, Appendix G discusses tritium problems in greater detail.

The reactor is at least as complex as (for example) a research accelerator. There must be routine maintenance and occasional disassembly. The prospect of working on such a machine from inside a shielded enclosure poses many problems. However difficult this situation may be, it is by no means impossible. As a rather elementary first approach, we have shown a crane with a shielded cab, and a shielded vehicle ("cherry picker") with appropriate manipulators. A second shielded vehicle is required for rescue purposes in case either the first vehicle or the crane should fail. Each part of the machine must be very carefully designed with the thought in mind of removing it and replacing it remotely. Flanges, for instance, would probably not be of the simple bolted variety, but would have special quick-acting clamps. The possibility of using TV camera and remotely operated manipulators must be explored. For routine maintenance, local removable shielding might offer some possibilities; however, for major disassemblies this does not appear feasible. A more detailed discussion of the requirements for remote assembly-disassembly procedures is given in Appendix E.

The disassembly of the reactor is shown diagrammatically in Figs. 16a through 16c.

Reactor Building Complex

The reactor building, described in detail in Appendix K, is highly specialized. It must contain the penetrating radiation from the operating reactor. The reactor containment vault shown in Fig. 17 must be 152-feet long, 64-feet wide, and 60-feet high to allow for convenient disassembly of the reactor. It is 10-feet thick, is gas-tight, and has a stainless-steel liner. The large access doors shown in Fig. 18 lead to assembly and hot-disassembly areas at either end of the vault. Figure 18 also shows some of the facilities which are provided for preparation and irradiation of specimens and maintenance and repair of highly radioactive equipment.

Space and utilities are provided for a large variety of reactor auxiliaries such as the DT fuel system, liquid helium refrigerator, and injector power supplies, as well as appropriate office space and supporting shop facilities. Figures in Appendix K show building plans, sections, elevations, and an overall perspective view of the entire facility.

Tritium System

Tritium collected by cryopumps in the injectors and mirror loss regions is cycled through a purifier (Pd-Ag membranes) and isotopic separation plant (cryogenic distillation) and is reused in the injectors. An inventory of about 400 g is required in the system. Some tritium is generated by neutron capture in the boron used in the shield and will require periodic disposal. Care must be taken to outgas the equipment exposed to the plasma tritium prior to disassembly. The total release of tritium during disassembly should be less than 0.1 g.

During operation, the reactor vault would be filled with nitrogen to prevent the possibility of reactions between T_2 or D_2 and air. Low-level tritium contamination of the room nitrogen is removed by a tritium recovery system (TRS). The major portion of the tritium system costs are associated with this main TRS. Accidental failure possibilities of the various tritium-containing systems have been considered. All aspects of tritium handling and safety can be managed using techniques and hardware which, in most cases, are known today. A detailed analysis of tritium reactor problems is contained in Appendix G.

(a) Parting of reactor at midplane.

m 5

(b) Withdrawal of half of first wall, sample tube assembly, and associated intermediate and expander tanks.

m 5

Fig. 16. Steps in disassembly of reactor.

(c) Separation of expander tank.

(d) Disassembly of coil and coil support structure; removal from the vault of first wall, sample tube assembly, and intermediate tank.

Figure 16. Steps in disassembly of reactor (continued).

Fig. 17. Reactor in vault.

COST ESTIMATE

It must be emphasized that this cost estimate for a technologically advanced FERF facility is made far in advance of both detailed engineering and essential laboratory technology development. Therefore, a significant uncertainty is implied and is reflected in part by a large 50% contingency applied to the reactor and its facilities.

Of the total cost of about 226 million dollars, the distribution among major components is shown in Table 3 (assuming equal distribution of contingency and engineering; design, inspection, and administration fees).

Table 3. Distribution of costs among major FERF components

Item	Million \$ ^a	% of total
Superconducting coil system (including shielding and refrigeration)	75.5	33.3
Neutral beam injectors	44.3	19.6
Tritium system	40.8	18.0
Other	32.7	14.4
Utilities	19.4	8.6
Building	13.5	6.0
Totals:	226.4	100.0

^aThe costs are all in 1974 dollars, and an inflation factor must be applied for a future construction date.

A summary of the cost estimate is given in Table 4, divided into three sections; Reactor, Reactor Facilities, Building and Utilities. A more complete breakdown of the costs and a discussion of the cost estimating methods are given in Appendix L.

CONSTRUCTION SCHEDULE

This reactor is in an early conceptual stage of engineering design, and any construction time schedule is approximate. Estimates can be made by comparing FERF with other construction projects of similar complexity and then making adjustments based on the state of technology. The resulting schedule allows 3 years of engineering, 5 years of construction, and 1 year of shakedown. If a serious proposal is made and engineering starts in FY 76, a schedule such as that shown in Fig. 19 could be followed, and the reactor could be operating in FY 85.* This schedule assumes that development work on major areas of technical innovation such as coils, materials, remote handling, etc., is successful and is vigorously pursued in parallel with engineering design.

Because of the large investment required to build FERF, certain design features must be proven before hand to ensure achieving the design goals (or at least to minimize the risk of failing). The primary goals are of an engineering rather than a plasma physics nature, and to achieve these goals the plasma must perform to the designed value (for the low-field case).

- The magnet must deliver its designed field intensity because to fall short would drastically reduce the neutron flux ($\text{flux} \propto B^4$). Therefore, test magnets must provide high assurance that the magnet can in fact be built to specifications.
- The injectors must be developed. Fortunately, because of the modular nature of the injector system, a definitive test could be made on one module.
- The plasma performance, as measured principally by β , n , and $n\tau$, must provide a high assurance of achieving the desired goals.

*A major unknown is the time needed to satisfy licensing requirements. No similar device has ever been built.

Fig. 18. Section and plan view of reactor in vault showing sample tubes passing through vault walls.

Physics experiments to determine the feasibility of FERF will be necessary under FERF-like conditions. However, by employing a hydrogen plasma and having a low duty cycle, there would be little or no radioactivity.

Achievement of an $n\tau$ value of $\sim 1.6 \times 10^{12} \text{ cm}^{-3} \cdot \text{s}$ with high energy (65 keV), high density ($3 \times 10^{14} \text{ cm}^{-3}$), and large physical size (measured in $B_{\text{vac}}L \sim 16 \text{ T} \cdot \text{m}$) represents a considerable extrapolation from today's laboratory achievements ($n \sim 3 \times 10^{13}$, $W = 1\text{--}10 \text{ keV}$, $n\tau \sim 10^{10}$). Current experiments (BBII and 2XIIB) can explore some of

these parameters, but not all at once because the fields are too low and the sizes too small. At least one generation of experiments beyond BBII and 2XIIB will be needed before the plasma physics will be well enough understood to minimize the risks of building FERF. Design of the next generation of mirror experiments is now in the early planning stages.

However, how close to the FERF design should these experiments be? If results from BBII and 2XIIB will be as good as those machines could give (given their size and field limitations), then one might be willing to rely on extrapolation to the FERF param-

Table 4. Preliminary cost estimate summary.

Reactor:		Thousands of \$ ^a
Superconducting coils (includes refrigeration, power supplies, radiation shield, thermal shield, structure)	40,290	
Injectors	23,630	
First wall	340	
Irradiation facilities	1,730	
Plasma collector plates	280	
Outer tanks	1,510	
Support structure	1,100	
Diagnostics	750	
Controls and Instrumentation	1,850	
Vacuum system	800	
Cooling water distribution system	100	
Models and tests	430	
Spares	<u>3,610</u>	
Total reactor construction:		76,420
Reactor facilities:		
Tritium system	21,780	
Ancillary equipment	<u>4,960</u>	
Total reactor facilities construction:		26,740
Total reactor and reactor facilities		103,160
Engineering, design, inspection, administration, 25%		25,790
Contingency, 50%		<u>64,480</u>
Total reactor costs:		193,430
Building and utilities:		
Building	10,390	
Electrical substation	6,910	
Water cooling system	7,100	
Utilities and site improvements	<u>500</u>	
Total building and utilities:		24,900
Architect-Engineer, inspection, 15%		3,740
Contingency, 15%		<u>4,300</u>
Total building and utilities:		32,940
TOTAL PLANT:		<u>226,370</u>

^a1974 dollars

Fiscal year: '75 | '76 | '77 | '78 | '79 | '80 | '81 | '82 | '83 | '84 | '85 |

Fig. 19. Construction schedule.

eters. On the other hand, one might take a very conservative attitude and insist that a plasma physics experiment must verify all the FERF parameters with no appeal to extrapolation. Rather than either of these two extremes, a reasonable strategy would be to design new, special-purpose experiments with limited objectives and rely on some amount of extrapolation. What the parameters for these experiments should be, and how much extrapolation to use, should be the subject of wide discussion and more studies.

SAFETY

Safety considerations i.e., the applicable federal regulations, standards, and requirements for environmental assessment, are briefly discussed in Appendix F.

The most serious safety challenge centers around containment of tritium and neutron-induced radioactivity. In both of these areas, adequate technology is available, and the problems (see Appendices E and G) appear tractable.

REFERENCES

1. C. M. Logan, J. D. Anderson, and Z. A. Munir, Nucl. Tech. 21, (1974).
2. R. W. Werner, G. A. Carlson, J. Hovingh, and J. D. Lee, M. A. Peterson, Progress Report No. 1 on the Design Considerations for a Low-Power Experimental Mirror Fusion Reactor, Lawrence Livermore Laboratory, Rept. UCRL-74054, (1972).

3. UWMAK-1, A Wisconsin Toroidal Fusion Reactor Design, Univ. of Wisc., Fusion Design Study Team, Rept. UWFD-68 (1973).
4. H. H. Barschall, Intense Sources of High Energy Neutrons, Lawrence Livermore Laboratory, Rept. UCRL-74031, (1972).
5. C. E. Taylor, Summary of a Conceptual Design of a Mirror Reactor for a Fusion Engineering Research Facility (FERF), Lawrence Livermore Laboratory, Rept. UCRL-75293 (1974).
6. R. W. Moir and W. L. Barr, Nuclear Fusion 13, 35 (1973).

Appendix A - Plasma Physics*

The philosophy behind the FERF design is to see just how nearly classical the confinement of the plasma has to be in order to achieve the FERF goals while satisfying known plasma stability and equilibrium conditions. The discussion to follow will describe the plasma and the various constraints as we now understand them.

PLASMA PARAMETERS

We assume the plasma behaves in a way we will call "almost classical." In particular, the plasma is assumed to be stable to gross displacements (i.e., it is magnetohydrodynamically stable), and micro-instabilities are assumed to be suppressed to such an extent and to be of such a character as to be describable as enhanced coulomb binary collisional losses.

The plasma pressure perpendicular to the magnetic field, $P_{\perp}$, is given as

$$P_{\perp} = n_D \bar{W}_D \frac{2}{3} \gamma + n_T \bar{W}_T \frac{2}{3} \gamma + n_e \bar{W}_e \frac{2}{3}, \quad (A1)$$

where n_D , n_T , n_e , $\bar{W}_D$, $\bar{W}_T$, and $\bar{W}_e$ are the densities and mean energies of the deuterons, tritons, and electrons. He^{++} ions are not confined. The factor γ in Eq. (A1) accounts for the fact that more energy is perpendicular to the magnetic field in plasma confined by an anisotropic mirror than in plasma with an isotropic velocity distribution.

The plasma pressure which a given magnetic field configuration can hold is measured by β where β is defined as

$$\beta_{\perp} = \frac{P_{\perp}}{\frac{B_{\text{vac}}^2}{2\mu_0}}, \quad (A2)$$

where B_{vac} is the vacuum magnetic field and μ is the permeability of free space.

*We acknowledge the help given by M. E. Rensink and A. H. Futch for their Fokker-Planck calculations, by C. C. Damm for target plasma considerations, and by J. H. Foote for calculation of nonadiabatic particle energy limits.

In a mirror machine, β can be quite high ($1/2 < \beta < 1$) and is limited by the so-called mirror, or firehose, instability. The most severely limiting instability is the mirror mode, which is stable if

$$\frac{4\pi}{\mu_0} \frac{dB_{\text{vac}}^2}{dB} = 8\pi \frac{B}{\mu_0} + \frac{dP_{\perp}}{dB} > 0 \quad (A3)$$

The ion density n is then given as

$$n = \frac{\beta_{\perp} B_{\text{vac}}^2}{2\mu_0 \left(\frac{1}{3} \gamma \bar{W}_D + \frac{1}{3} \gamma \bar{W}_T + \frac{2}{3} \bar{W}_e \right)}, \quad (A4)$$

where we assume $n_D = n_T = 1/2 n$. The thermonuclear power per unit volume is given as

$$P/V = \frac{n^2}{4} (\bar{\sigma v})_{DT} E_F, \quad (A5)$$

where $(\bar{\sigma v})_{DT}$ is the reaction rate parameter averaged over the ion velocity distribution, and E_F is the energy released in each fusion reaction. The total power, assuming $(\bar{\sigma v})_{DT}$ is spatially constant, is

$$P_{\text{fusion}} = \int_{\text{volume}} P/V dV = \frac{(\bar{\sigma v})_{DT} E_F}{4} \int n^2 dV. \quad (A6)$$

The loss rate due to coulomb collisions equals the injection rate if charge-exchange losses are neglected and is given as

$$I_{\text{loss}} = q \int \frac{n}{\tau} dV, \quad (A7)$$

where τ is the mean lifetime of particles at density n . If we multiply the numerator and denominator of Eq. (A7) by n and assume that the product $n\tau$ is independent of space, we get

$$I_{\text{loss}} = \frac{q}{n\tau} \int_{\text{volume}} n^2 dV. \quad (A8)$$

The power gain Q is defined as

$$Q = \frac{P_{\text{fusion}}}{I_{\text{loss}} W_0/q} = \frac{(\bar{\sigma}v)_{DT} (n\tau) E_F}{4W_0}, \quad (\text{A9})$$

where W_0 is the injection energy. The number of neutrons produced per second S is given as

$$S = \frac{P_{\text{fusion}}}{E_F}$$

We will now find a set of parameters from Eqs. (A1) through (A10) corresponding to the low-field case. The high-field case can be scaled from these numbers.

We take B_{vac} to be 3.75 T and β equal to 0.65. This value of β was found from Eq. (A3) using the two-dimensional Fokker-Planck code and was just at the threshold of the mirror mode. To find the mean energies γ and $n\tau$, we ran the one-dimensional Fokker-Planck multispecies code and the two-dimensional, one-species Fokker-Planck code. The input for the one-dimensional code and the results are given in Table A-1. This code gives us the mean energies of the several species and $n\tau$ but does not give information on perpendicular pressure. The two-dimensional code gives angular information but not multispecies information. The input and key results from the latter are given in Table A-2.

Because of the importance of the density to all aspects of the FERF, we show the calculation of density from Eq. (A4)

$$n = \frac{0.65 (3.75)^2}{2 \times 4 \pi \times 10^{-7} \left(\frac{1}{3} \times 1.3 \times 73.27 + \frac{1}{3} \times 1.3 \times 93.03 + \frac{2}{3} \times 7.17 \right) \times 10^3 \times 1.6021 \times 10^{-19}}$$

$$n = 2.954 \times 10^{14} \text{ cm}^{-3}$$

With the above density, the central power density is $59.3 \text{ W} \cdot \text{cm}^{-3}$ of neutrons and alpha particles. The fraction of β contributed by each species is

$$\frac{\beta_D}{\beta} = 0.413; \quad \frac{\beta_T}{\beta} = 0.525; \quad \frac{\beta_e}{\beta} = 0.062.$$

The plasma is assumed to be cylindrically symmetric at the midplane ($z = 0$) and to have a radial

Table A-1. One-dimensional multispecies Fokker-Planck input and results.

Input
W_D injection = 65 keV
W_T injection = 97.5 keV
$R = 2.3$
Normal-mode angular distribution
Results
$W_D = 73.27 \text{ keV}$
$W_T = 93.03 \text{ keV}$
$W_e = 7.17 \text{ keV}$
$n\tau = 5.84 \times 10^{12} \text{ cm}^3 \cdot \text{s}$
$I_D = 364 \text{ A}$
$I_T = 136 \text{ A}$
$I_{D+T} = 500 \text{ A}$
$(\bar{\sigma}v)_{DT} = 9.687 \times 10^{-16}$
$(\bar{\sigma}v)_{DD} = 1.8 \times 10^{-17}$
$n_D = 6.895 \times 10^{12} \text{ cm}^{-3}$
$n_T = 6.2329 \times 10^{12} \text{ cm}^{-3}$
$n_e = 13.2462 \times 10^{12} \text{ cm}^{-3}$
$J_D = 2.183 \times 10^{13} \text{ cm}^{-3} \text{ s}^{-1}$
$J_T = 0.817 \times 10^{13} \text{ cm}^{-3} \text{ s}^{-1}$
$J_e = 3.0 \times 10^{13} \text{ cm}^{-3} \text{ s}^{-1}$
$Q_{DT} = 0.417$
$\phi_P = 24.3 \text{ kV}$

density profile as compared with the buildup code, discussed later. Empirical curve fits have shown this radial profile to be best approximated by the following cubic:

$$n(r) = n_{00} \left(1 - \left(\frac{r}{R} \right)^3 \right).$$

The axial profile is that calculated by the two-dimensional Fokker-Planck code which computes n as

Table A-2. Two-dimensional Fokker-Planck results.

Parameter	Radius (cm)		
	0.0	12.5	17.5
W_0 injection D^+ (keV)	65	65	65
W_D (keV)	63.82	64.97	
W_e (keV)	6.26	6.28	
ϕ_p (kV)	21.2	21.1	
$n\tau$ ($\text{cm}^3 \cdot \text{s}$)	4.971×10^{12}	5.13×10^{12}	4.807×10^{12}
R	3.38	3.05	2.642
Injection angles (degrees)	55, 63, 70, 78	60, 68, 76, 83	63, 71, 78, 86
γ	1.302	1.33	
$\beta_{L\text{-crit}}$	0.679	0.5745	
β_L	0.65	0.569	0.427
n (cm^{-3})	2.954×10^{14}	2.586×10^{14}	1.262×10^{14}

a function of B . When we make the long slender approximation (only approximately true for our case) which says that axially the density varies slowly, we can show that

$$B = B_{\text{vac}} \sqrt{1 - \beta} = B_{\text{vac}} \sqrt{1 - \beta_0 \frac{P_1(\theta)}{P_{L0}}} \quad (\text{A10})$$

We calculate the vacuum magnetic field with the computer code MAFCO which gives B_{vac} as a function of space. The spatial variables we take are the midplane radius r and angle θ and the distance along a field line s . Figure A-1 shows the assumed radial and calculated axial density profiles. Figure A-2 shows the axial vacuum magnetic field and the field which is depressed by the plasma diamagnetism calculated from the two-dimensional code results and Eq. A14. Figure A-3 shows the same things in the radial direction at the midplane. Figure A-4 shows vacuum magnetic field lines and density profiles.

To compute the power produced and the injection current required, we evaluated Eq. (A8) numerically by breaking the volume integral into two integrals: an area integral at the midplane which cut tubes of plasma bounded by magnetic flux lines, and an integral along the flux tube measured by the variable s . The volume integral then became

$$\int n^2 dV = \int_0^R \int_{-\frac{L}{2}}^{\frac{L}{2}} n^2 2\pi r \frac{B_0}{B} dr ds, \quad (\text{A11})$$

where B_0 is the magnetic field at the midplane, and B is the value at position s along the flux tube. Thus, the ratio B_0/B takes into account the area contraction due to B increasing towards the magnetic mirrors. The integral in Eq. (A11) has been put into a standard form as follows:

$$G^i = \int_0^R \int_0^{L/2} \frac{2\pi r}{\pi R^2 L} \left(\frac{n}{n_{00}} \right)^i \frac{B_0}{B} dr ds, \quad (\text{A12})$$

where i is a superscript of G rather than a power.

The numerical integration divided L , the length between mirrors (420 cm), into 10-cm intervals, with

Fig. A-1. Radial and axial density profiles for $\beta = 0.5$ and the design value of $\beta = 0.65$.

Fig. A-2. Axial magnetic field profile for $\beta = 0$ (vacuum field), 0.5, and 0.65.

radial points at $r = 0, 8, 12.5, 17.5$, and 25 cm. The results were

$$G^0 = 0.6494, G^1 = 0.13589, G^2 = 0.06887.$$

The plasma volume, V , is

$$V = \pi R^2 L G^0 = 535.5 \text{ l.}$$

The total number of confined ions, N , is

$$N = \pi R^2 L n_{00} G^1 = 1.1206 \times 10^5 n_{00} = 3.310 \times 10^{19}.$$

For $n_{00} = 2.954 \times 10^{14} \text{ cm}^{-3}$, which is the central density for the low-field case, the value of the integral

$$\begin{aligned} \int n^2 dv &= \pi R^2 L n_{00}^2 G^2 = 5.680 \times 10^4 n_{00}^2 \\ &= 4.956 \times 10^{33} \text{ cm}^{-3} \end{aligned}$$

The power of 14-MeV neutrons plus 3.52-MeV He is, from Eq. (A6)

$$\begin{aligned} P &= \frac{4.956 \times 10^{33} \cdot 9.687 \times 10^{-16}}{4} \\ &\times \frac{17.58 \times 10^6 \cdot 1.6021 \times 10^{19}}{4} \\ P &= 3.38 \times 10^6 \text{ W} \end{aligned}$$

The classical loss rates will now be calculated for D^+ and T^+ separately instead of using Eq. (A8):

$$\begin{aligned} I_{\text{loss } D^+} &= q \int \frac{n_D}{\tau_D} dv \\ &= q \int J_D dv \end{aligned}$$

$$I_{\text{loss } T^+} = q \int J_T dv.$$

Now, from Table A-1 we have J , the source current per unit volume at the densities in the table. Since $J \propto n^2$, we have

$$\begin{aligned} J_{0D} &= 2.183 \times 10^{13} \left(\frac{2.954 \times 10^{14}}{13.2403 \times 10^{12}} \right)^2 \\ &= 1.0867 \times 10^{16} \text{ cm}^{-3} \end{aligned}$$

$$\begin{aligned} J_{0D} &= 0.817 \times 10^{13} \left(\frac{2.954 \times 10^{14}}{13.2403 \times 10^{12}} \right)^2 \\ &= 0.407 \times 10^{16} \text{ cm}^{-3} \end{aligned}$$

$$\begin{aligned} I_D &= q J_{0D} \int \frac{n^2 dv}{n_0^2} = J_{0D} \pi R^2 L \int \left(\frac{n}{n_{00}} \right)^2 \frac{dv}{\pi R^2 L} \\ &= 1.6021 \times 10^{19} \times 1.0867 \times 10^{16} \pi 25^2 420 \\ &\times 0.06887 \\ &= 98.88 \text{ A} \end{aligned}$$

Fig. A-3. Radial magnetic field profile for $\beta = 0, 0.5$, and 0.65 .

Fig. A-4. Density contours (for $\beta = 0.5$ and $\beta = 0.65$) and vacuum magnetic field lines.

$$I_T = 37.03 \text{ A}$$

$$I = I_D + I_T = 135.9 \text{ A}$$

$$P_{\text{classical}} = 37.03 \times 97.5 \times 10^3 + 98.88 \times 65$$

$$\times 10^3 = 10.04 \text{ MW}$$

$$Q(17.58 \text{ MeV}) = \frac{3.38}{10.04} = 0.337$$

The actual injection power for 500 A injected (the capability selected for the present design) is

$$P = 136 \times 97.5 \times 10^3 + 364 \times 65 \times 10^3 = 36.92 \text{ MW}$$

The trapping efficiency is calculated to be approximately 84%, giving a trapped injected beam capability of 31.38 MW. The design Q is then $3.38/31.38 = 0.11$. The classical average $n\tau$ for deuterons and tritons is given as

$$n\tau_{\text{classical}} = \frac{n^2}{J_D + J_T} = \left(\frac{13.24 \times 10^{12}}{3 \times 10^{13}} \right)^2 = 5.84$$

$$\times 10^{12} \text{ cm}^{-3} \cdot \text{s}.$$

Thus, the actual injection power is 3.1 times greater than that required to sustain classical losses; and the 500-A capability could sustain an $n\tau$ of $1.9 \times 10^{12} \text{ cm}^{-3} \cdot \text{s}$, an amount 3.1 times less than the classical $n\tau$.

This result can be compared with the classical injection current, I_{loss} , calculated from Eq. (A8) for a one-species plasma (D^+ only):

$$I_{\text{loss}} = \frac{q}{n\tau} \int n^2 dV = \frac{1.6021 \times 10^{-19}}{5 \times 10^{12}} 4.956$$

$$\times 10^{33} = 158.8 \text{ A}.$$

This is 3.15 times less than the 500-A design capability. The classical injection at 65 keV would be 10.3 MW; therefore, the classical Q (17.58 MeV) is 0.328. We see that treating the plasma with one species overestimates the losses somewhat.

The source of 14-MeV neutrons is

$$S = \frac{4.956 \times 10^{33} \times 9.687 \times 10^{-16}}{4}$$

$$S = 1.2 \times 10^{18} \text{ n} \cdot \text{s}^{-1}.$$

If this were a point source, the flux on a sphere at 25-cm radius would be

$$\Gamma = \frac{S}{4\pi R^2} = 1.53 \times 10^{14} \text{ n} \cdot \text{cm}^{-2} \cdot \text{s}^{-1}$$

If the source were uniformly spread throughout a 25-cm-radius sphere, the current would be the same as above but the flux would be 1.5 times larger, or

$$\Gamma = 2.29 \times 10^{14} \text{ cm}^{-2} \cdot \text{s}^{-1}.$$

The spatial dependence of the flux of 14-MeV neutrons is calculated by first deriving a line source

$$F(s) = \frac{(\partial \bar{v})_{DT}}{4} \int n^2 2\pi r dr \frac{B}{B_0}.$$

Then, the calculated flux is

$$\Gamma(r, s) = \int_{-L/2}^{L/2} \frac{F(s)}{4\pi r'^2} ds$$

where r' is the distance from the element ds to the point at r, s . The line source $F(s)$ and Γ at several positions are given in Figs. A-5 and A-6. At $Z = 0$ and $r = 35 \text{ cm}$, samples could be placed in the maximum flux ($0.531 \times 10^{14} \text{ neutron cm}^{-2} \cdot \text{s}^{-1}$). At the edge of the plasma where $r = 25 \text{ cm}$, the flux is 0.89×10^{14} .

Another calculation which assumes the axial profile to be the same on each line but otherwise calculates flux properly shows that the line source calculation gives a lower neutron flux than does a volume source close to the plasma. This correction is 8% at 35 cm and 20% at 25 cm. The flux at 35 cm is then $0.57 \times 10^{14} \text{ cm}^{-2} \cdot \text{s}^{-1}$ and $1.07 \times 10^{14} \text{ cm}^{-2} \cdot \text{s}^{-1}$ at a 25-cm radius.

Fig. A-5. 14-MeV neutron flux versus axial position at 25- and 35-cm radius for $\beta = 0.65$.

ENERGY SCALING

The 14-MeV neutron flux at the surface of the FERF plasma varies as

$$\Gamma \propto r_p n^2 (\bar{\sigma} \bar{v}) DT.$$

For constant magnetic field strength and β ,

$$n \propto \frac{1}{W_{inj}}.$$

Therefore,

$$\Gamma \propto \frac{r_p (\bar{\sigma} \bar{v}) DT}{W_{inj}^2}.$$

Because the size of the plasma has a strong influence on magnet size and beam power (both are major cost items), first consider the energy scaling of FERF with a constant plasma radius. FERF should have the minimum plasma size which will provide sufficient space for performing irradiation tests. For this study, the minimum size is judged to be a radius at the plasma midplane of about 25 cm. The neutron flux, Γ , is plotted as a function of injection energy W_{inj} in Fig. A-7 for the design point case of $r_p = 25$ cm. Note

that at the design point of $W_{inj} = 65$ keV, the neutron flux is $1.08 \times 10^{14} \text{ cm}^{-2} \text{ s}^{-1}$. The curve shows that the neutron flux increases rapidly with decreasing injection energy and is maximum at $W_{inj} = 20$ keV. Unfortunately, a 20-keV injection energy cannot be used with $r_p = 25$ cm because the required injection power is too large and the beam will not penetrate to the interior of the plasma.

The injection current which must be trapped by the plasma varies as

$$I_{inj} \propto \frac{nr_p^3}{\tau} = \frac{n^2 r_p^3}{n\tau}.$$

Fig. A-6. Calculated equivalent line source of 14-MeV neutrons.

Fig. A-7. Neutron flux and neutral atom power flux to the wall versus injection energy at constant plasma radius, $r_p = 25$ cm.

Fig. A-5. 14-MeV neutron flux versus axial position at 25- and 35-cm radius for $\beta = 0.65$.

ENERGY SCALING

The 14-MeV neutron flux at the surface of the FERF plasma varies as

$$\Gamma \propto r_p n^2 (\bar{\sigma} \bar{v}) D T.$$

For constant magnetic field strength and β ,

$$n \propto \frac{1}{W_{inj}}.$$

Therefore,

$$\Gamma \propto \frac{r_p (\bar{\sigma} \bar{v}) D T}{W_{inj}^2}.$$

Because the size of the plasma has a strong influence on magnet size and beam power (both are major cost items), first consider the energy scaling of FERF with a constant plasma radius. FERF should have the minimum plasma size which will provide sufficient space for performing irradiation tests. For this study, the minimum size is judged to be a radius at the plasma midplane of about 25 cm. The neutron flux, Γ , is plotted as a function of injection energy W_{inj} in Fig. A-7 for the design point case of $r_p = 25$ cm. Note

that at the design point of $W_{inj} = 65$ keV, the neutron flux is $1.08 \times 10^{14} \text{ cm}^{-2} \text{ s}^{-1}$. The curve shows that the neutron flux increases rapidly with decreasing injection energy and is maximum at $W_{inj} = 20$ keV. Unfortunately, a 20-keV injection energy cannot be used with $r_p = 25$ cm because the required injection power is too large and the beam will not penetrate to the interior of the plasma.

The injection current which must be trapped by the plasma varies as

$$I_{inj} \propto \frac{n r_p^3}{\tau} = \frac{n^2 r_p^3}{n \tau}.$$

Fig. A-6. Calculated equivalent line source of 14-MeV neutrons.

Fig. A-7. Neutron flux and neutral atom power flux to the wall versus injection energy at constant plasma radius, $r_p = 25$ cm.

and the required trapped injected power varies as

$$P_{inj} \propto \frac{n^2 r_p^3 W_{inj}}{nr}$$

Since $nr \propto W_{inj}^{3/2}$, it follows that

$$P_{inj} \propto \frac{r_p^3}{W_{inj}^{2.5}}$$

The required trapped injected power P_{inj} is plotted as a function of W_{inj} in Fig. A-8 for $r_p = 25$ cm. Note that P_{inj} increases very rapidly as the injection energy is reduced below 65 keV. Such an increase is unacceptable because the injection system is power-density-limited and nearly all of the possible injection area is utilized for the 65-keV design.

The beam-attenuation parameter, γ , has been defined such that the fraction of beam penetrating to depth x in the plasma is

$$\frac{I}{I_{inj}} = e^{-\gamma \frac{x}{2r_p}}$$

The attenuation parameter must be large enough to yield a large trapping fraction but small enough to permit penetration to the interior of the plasma. The design point value of γ (along the central injection ray) is 3.5. As shown in Appendix C, the attenuation parameter varies as

$$\gamma \propto \frac{r_p}{W_{inj}^{1.92}}$$

The attenuation parameter, γ , is plotted vs W_{inj} in Fig. A-7 for $r_p = 25$ cm. Note that only a small reduction in W_{inj} from 65 keV to 55 keV results in an increase in γ from 3.5 to 5.0. The upper limit on γ set by penetration requirements is not known, but 5.0 may be too high since only 8% of the beam will penetrate to the center of the plasma (vs 17% at $W_{inj} = 65$ keV).

The fraction of the total injected power which deposits on the first wall instead of in the plasma is calculated in Appendix C as a function of W_{inj} and γ .

For the design point ($W_{inj} = 65$ keV, $\gamma = 3.5$), the peak power density to the first wall is $\cong 790$ W/cm². At $W_{inj} = 55$ keV and $\gamma = 5.0$, the fraction of injected power which deposits on the wall is actually lower (0.11 vs 0.16), but the peak power density increases slightly to 820 W/cm².

In summary, we see that although a reduction in energy below 65 keV at constant plasma radius would yield a higher 14-MeV neutron flux, such scaling would result in undesirable increases in the required injection power and attenuation parameter, γ .

Now consider the energy scaling of FERF with a constant attenuation parameter, γ , instead of a constant plasma radius. Such scaling is of interest because it maintains constant the beam penetration to the interior of the plasma. The neutron flux, Γ , is plotted as a function of injection energy W_{inj} in Fig. A-8 for the design point case of $\gamma = 3.5$. Note that the neutron flux is maximum at the design point of $W_{inj} = 65$ keV.

Although the maximum in neutron flux is quite broad, other considerations strengthen the incentive to design FERF for 65 keV. As shown in Fig. A-8, scaling to higher energies at constant γ requires larger r_p and P_{inj} , while decreasing the energy results in a rapid increase in q_{max} , the peak power density to the first wall.

MAGNETIC FIELD SCALING

Most of the discussion so far has been on the low-field case; however, a higher field would allow the containment of higher densities and higher resultant neutron fluxes. These quantities scale as follows:

$$n \propto B^2$$

$$\phi \propto B^4$$

$$I_{loss \text{ classical}} \propto B^4$$

As shown in Fig. A-9, the potential for higher performance at a higher central field is clear. At a 5.0-T central field, the plasma density is 5.3×10^{14} cm⁻³, the classical loss rate is 430 A, and the neutron flux at 35 and 25 cm is 1.8×10^{14} and 3.4×10^{14} cm⁻²·s⁻¹, respectively. Higher fields can be obtained either by keeping the field at the conductor at 9.0 T

Fig. A-8. Neutron flux, neutral atom power flux to the first wall, classical loss current, plasma radius vs injection energy at constant beam attenuation parameter, $\gamma = 3.5$.

Fig. A-9. Neutron flux, central density, and classical loss current versus central vacuum magnetic field.

and lowering the mirror ratio or by raising the field uniformly everywhere up to the high-field value of 12.0 T at the conductor.

PLASMA STABILITY

Our basic assumption about the plasma is that it has a well defined spatial equilibrium and that the only appreciable losses result from coulomb collisions which cause scattering into the velocity-space loss cone and instabilities which are limited to not more than a factor of two more losses than the collisional losses. We will mention three classes of instabilities: mirror or firehose mode, interchange mode, and microinstabilities

The mirror mode is an instability, or possibly a loss of equilibrium, which can occur when the plasma pressure is strong enough to make a local field depression which locally confines more particles and leads to a further field depression in an unstable way. This mode can be avoided by not exceeding a critical plasma pressure or a critical β . For our case, the critical β was 0.68. Since this mode is a local instability, the distribution function can be tailored by orientating injectors to optimize β by smoothing the pressure gradients at the critical positions. So far, the angular orientation of the injectors has not been optimized to maximize β . Future work should explore this optimization.

The interchange mode, a gross movement of the plasma across the magnetic field, is suppressed by providing a radial magnetic well and is the purpose for the unusually shaped Yin-Yang coil. Many experiments have shown the effectiveness of radially increasing magnetic fields (good curvature) even at β values of several tenths. The plasma diamagnetism causes the magnetic field lines near the midplane to bulge out, thus tending to add bad field-line curvature. This effect is somewhat compensated for by the increased magnetic gradient in the bulk of the plasma. The uncertainty is quantitatively how high can β be. At present, a computational physics effort is underway to determine the limiting β . If the limiting β due to the interchange mode is less than the assumed 0.65 (as we are fairly certain will be the case), then either the coil must be redesigned to give more radial well or the central field strength must be raised to lower β without reducing the neutron output.

Finally, the high-frequency ($\omega \gg \omega_{ci}$) instabilities known as microinstabilities must be sufficiently

suppressed. Our designs allow for the loss rate to be enhanced over the classical rate by up to a factor of three. One recent theory^{A1} suggests that at high densities the anomalous losses are indeed within a factor of two to three of the classical losses. Recent 2XII experimental results partially support this theory in that at low ion energies (~ 1.3 keV) the losses were near classical. However, as the energy was raised, the losses remained about constant whereas the classical loss rate decreases as $W^{-3/2}$.

The central density of $2.95 \times 10^{14} \text{ cm}^{-3}$ and the magnetic field with plasma of $3.75\sqrt{1-0.65} = 2.22$ T gives an electron dielectric constant $(\omega_{pe}/\omega_{ce})^2 = 6.2$ and an ion dielectric constant $(\omega_{pi}/\omega_{ci})^2 = 2.3 \times 10^4$ for D^+ ions. Theory indicates that $(\omega_{pe}/\omega_{ce})^2 \gtrsim 1$ is perhaps a necessary condition for suppression of instability losses. If the central density should drop by a factor of two, then $(\omega_{pe}/\omega_{ce})^2 = 1.6$; that is, $(\omega_{pe}/\omega_{ce})^2 = nm_e/\epsilon_0 B_{vac}^2 (1-\beta)$. The latter shows the importance of high- β operation for suppression of microinstabilities.

The axial and radial sizes of a mirror plasma play a role in many theoretical criteria and are therefore given here for general information. The gyroradius, a_i , at the center is 2.45, 3.39, and 12.0 cm, assuming the 73-keV D^+ , 93-keV T^+ and 3.52-MeV He^{++} , are perpendicular to the field. The ratio of plasma radius, r_p , and length between mirrors, L , to the central D^+ gyroradius (taking the above energy perpendicular to the field) is $r_p/a_i = 10.2$ and $L/a_i = 171$.

Perhaps the biggest unknown and threat to FERF is the role of instabilities. This is one of the main arguments for a large-scale experiment to demonstrate the feasibility of the FERF. For the rest of this discussion of plasma physics, we will assume instabilities are adequately suppressed.

ADIABATICITY

The adiabatic confinement principle states that if the magnetic field varies slowly in one gyroperiod, then the magnetic moment is conserved and long-time confinement can result. For the FERF magnetic field, one quickly finds that the spatial magnetic field variation is small without the plasma but that the plasma depresses the field and makes rather strong field gradients particularly in the radial direction.

Foot^{A2} has discussed the adiabatic limit and gives the following formula for the maximum energy which can be contained.

$$W_{\max}(\text{keV}) = (8.5 \times 10^{-4}) \left(\frac{z^2}{A} \right) \frac{(B_M L)^2}{R(R-1)^3}$$

$$\frac{1}{L^2} = \frac{1}{L_z^2} + \frac{1}{L_R^2 (R-1)}.$$

The magnetic field is fitted by the following expression:

$$B = \frac{B_M}{R} \left[1 + \left(\frac{z^2}{L_z^2} + \frac{r^2}{L_r^2} \right) (R-1) \right].$$

B_M is the mirror field and is 7.58 T for FERF. Fits made to both the magnetic field and the mirror field in the radial and axial directions are shown in Figs. A-10 and A-11 for $\beta = 0.5$ and 0.65. The mirror ratio was 2.9 and 3.4. For $\beta = 0.5$, $L_r = 53$ cm and $L_z = 160$ cm. For $\beta = 0.65$, $L_r = 47$ cm. Therefore, $L = 66.5$ and 66.3 for the two values of β . The maximum triton energy was 361 keV and 152 keV for $\beta = 0.5$ and 0.65, respectively. Since the triton injection energy is 97.5 keV, there will be some loss from the high-energy tail of the distribution. To see the effect this might have on the confined ion distribution, we can look at Fig. A-12 and A-13. A rather small part of the T^+ distribution will be cut off and virtually no added loss will result for the D^+ ions. The 3.52-MeV He^{++} ions will be immediately lost out the ends due to nonadiabaticity. If the machine were approximately 1.8 times larger or the field 1.8 times larger at 0.65 β , then He^{++} ions would be contained. Future work on determining adiabaticity should be done with ion trajectory calculations in a self-consistent magnetic field.

Figures A-14 and A-15 show the distribution of the ions escaping out the ends and are included here for comparison to the confined distribution shown in Figs. A-12 and A-13 and for future reference with respect to direct energy conversion of these end losses.

Fig. A-10. Analytic fit to the axial magnetic field for calculating the scale length for the adiabatic energy limit.

Fig. A-11. Analytic fit to the radial magnetic field for calculating the scale length for the adiabatic energy limit.

Fig. A-12. Contained tritium particle distribution versus tritium energy.

Fig. A-13. Contained deuterium particle distribution versus deuterium energy.

Fig. A-14. Distribution of tritium current versus energy leaking out the ends of the machine.

STARTUP OF THE FERF PLASMA

Target Plasma Conditions

We shall now derive approximate conditions which a target plasma must satisfy in order to start up the FERF plasma. We assume that at time zero the full 500 A of D^0 and T^0 are switched on and that a target plasma (just dense enough to attenuate neutral gas) has already been created which has an ion energy much less than 65-keV injection energy of deuterons

Fig. A-15. Distribution of deuterium current versus energy leaking out the ends of the machine.

(tritons have the same speed). The beam is assumed to be 20-cm wide at the plasma and the target plasma diameter is also 20 cm. The axial-density profile for the target plasma is assumed to be the same as for the final steady-state hot plasma for the $\beta = 0.5$ case (see Fig. A-1). Actually, the target β is exceedingly small but the density profile shape is very similar to the $\beta = 0.5$ case.

The volume, V , of this plasma is given by

$$V = \pi R^2 L G^{(0)} = \pi \times 10^{-2} \times 420 \times 0.67 \\ = 88.4 \times 10^3 \text{ cm}^3$$

The average density is less than the central density by the factor $G^1/G^0 = 0.254$.

At the end of a certain time called τ_T (calculated below), we assume the target has decayed away according to e^{-t/τ_T} and that it has been replaced by a hot plasma which is dense enough to attenuate (by one e-folding) the neutral gas in a distance of one gyrodiameter ($2a_i$). For 65-keV deuterons in 3.75 T, $2a_i = 2.72$ cm. The required condition is

$$\lambda = 2a_i = \frac{1}{n_H (\Sigma \sigma v / v_{D^0})}$$

where n_H is the density of hot plasma, $W \sim 65$ keV, $V_{D_2}^0$ is the speed of incoming D_2 molecules at 0°C ($v_{D_2} \sim 1.1 \times 10^5 \text{ cm s}^{-1}$), and $\Sigma\sigma v$ are the reaction rates for ionizing incoming gas.

A 50-eV-Electron Cold-Ion Target Plasma

We assume that the ions are sufficiently cold that the attenuation is only by electrons at 50-eV mean energy.

$$(\bar{\sigma}v)_{ie} = 5.0 \times 10^{-8} \text{ cm}^3 \text{ s}^{-1}.$$

$$n_H = \frac{1.1 \times 10^5}{2.72 \times 5 \times 10^{-8}} = 0.8 \times 10^{12} \text{ cm}^{-3}$$

We assume that the plasma will build up from this $0.8 \times 10^{12} \text{ cm}^{-3}$ density to the final steady-state density; however, whether or not the plasma will build up under FERF conditions is discussed later and is by no means assured. The electron dielectric constant (ω_{pe}/ω_{ce})² for this plasma is 0.006.

We will now calculate the time for the hot plasma to build up to n_H , and this time will then set the allowable decay time for the target plasma. We assume that all 500 A hits the plasma but that the average cord length, ℓ , is calculated to be only 63% of the diameter, or 12.6 cm. The trapping rate is then R_t , so that

$$R_t = \frac{1}{q} \left[1 - \exp \left(-n_T \frac{\Sigma\sigma v}{v_0} \ell \right) \right]$$

$$n_T = n_{T_0} e^{-t/\tau_T}$$

$$n_H = \frac{1}{V} \frac{\bar{n}}{n_0} \int_0^{\tau_T} \frac{1}{q} \left[1 - \exp \left(-n_T \frac{\Sigma\sigma v}{v_0} \ell \right) \right] dt$$

The quantity $\bar{n}/n_0$ accounts for the central density n_0 being greater than the average density.

If the beam is not strongly attenuated, the quantity in brackets can be expanded, giving

$$n_H = \frac{1/q}{V} \frac{\bar{n}}{n_0} \frac{\Sigma\sigma v}{v_0} \ell \int_0^{\tau_T} n_{T_0} \tau_T \exp(-t/\tau_T) \frac{dt}{\tau_T}$$

$$= \frac{0.631/q \frac{\Sigma\sigma v}{v_0} \ell}{V \frac{\bar{n}}{n_0}} n_{T_0} \tau_T ;$$

$$\Sigma\sigma v = \sigma v_{cx} + \sigma v_{ii} + \sigma v_{ie}$$

$$= 1.5 \times 10^{-16} \times 2.5 \times 10^8 + 1.0 \times 10^{-16} \times 2.5 \times 10^8$$

$$+ 2.2 \times 10^{-8} = 8.45 \times 10^{-8}; \text{ and}$$

$$n_H = \frac{0.63 \times 500 \times 8.45 \times 10^{-8} \times 12.6/2.5 \times 10^8}{88.4 \times 10^3 \times 0.254 \times 1.6 \times 10^{-19}}$$

$$\times n_{T_0} \tau_T = 373.4 n_{T_0} \tau_T.$$

For the required value of n_H for attenuation of neutral gas of $0.8 \times 10^{12} \text{ cm}^{-3}$, the value of $n_{T_0} \tau_T$ which the target must satisfy is

$$n_{T_0} \tau_T = \frac{0.8 \times 10^{12}}{373.4} = 2 \times 10^9 \text{ cm}^{-3} \text{ s}.$$

For $n_{T_0} = 2 \times 10^{13} \text{ cm}^{-3}$, the target lifetime, τ_T , is 100 μs , which is well within the 2X plasma regime.

However, if for stability of the target plasma during build up, the electron dielectric constant is required to be unity ($\omega_{pe}^2/\omega_{ce}^2 = 1$), then $n_H = 1.9 \times 10^{14} \text{ cm}^{-3}$, and $n_{T_0} \tau_T = 3.4 \times 10^{11} \text{ cm}^{-3} \text{ s}$ which is beyond present practice.

The total number of ions in the target plasma is N_T , where

$$N_T = \bar{n} V = 0.254 \times 2 \times 10^{13} \times 68.4 \times 10^3 \\ = 4.5 \times 10^{17} \text{ particles}$$

We assumed that the quantity $n_T \Sigma\sigma v \ell / v_0 \ll 1$. In fact,

$$\frac{n_T \Sigma\sigma v \ell}{v_0} = \frac{2 \times 10^{13} \times 8.45 \times 10^{-8} \times 12.6}{2.5 \times 10^8} = 0.085,$$

thereby justifying the assumption.

We conclude that a target of $n_{T_0} \tau_T = 2 \times 10^9 \text{ cm}^{-3} \text{ s}$ which has a density of $2 \times 10^{13} \text{ cm}^{-3}$ and a

100- μ s decay time would be an adequate target plasma for startup purposes for attenuation of neutral gas. If $(\omega_{pe}/\omega_{ce})^2 = 1$ is required for stability, then a more stringent requirement on the target will be needed. The target plasma requirements need much further study; fortunately, this topic is a major part of the BBII and 2X experimental programs as well as LLL's theoretical studies.

Now we will discuss the effect of gas impinging on the plasma during start-up. Start-up conditions differ from those of steady-state in that during start-up the fast neutrals impinging on the wall will bury about 1 μ m into the wall and take some time to diffuse back, whereas in steady-state the return is equal to the input. During the transient state, we will assume that the wall absorbs all but 10% of the incident beam. For restarting the plasma after prolonged operation, the walls would be saturated and other assumptions would be called for to analyze the buildup. The other difference between startup and steady state is that the target plasma is much less dense and therefore is less efficient at burning out incoming gas.

We will assume that for the first few milliseconds no gas from the source will reach the plasma. Then, the trapping rate in the outer 2.72 cm (a layer one gyroradius thick) is given below for 6.5-keV electron mean energy

$$\ln H \frac{\Sigma \sigma v}{v_0} 2a_i$$

$$500 \times 0.8 \times 10^{12} \frac{4 \times 10^{-8} + 4.6 \times 10^{-8} + 1.3 \times 10^{-8}}{2.5 \times 10^8}$$

$$\times 2.72 = 0.43 \text{ A.}$$

The loss rate from a discussion to follow is I^* .

$$I^* = 0.1 \frac{fw}{2} I \times f = 0.1 \times \frac{0.1}{2} 500 \times 0.7 = 1.75 \text{ A.}$$

From this simplified model, it appears that the plasma boundary would erode away if the hot density is $0.8 \times 10^{12} \text{ cm}^{-3}$ at the plasma edge after the cold target disappears. However, if the hot density was four times more, or $3.3 \times 10^{12} \text{ cm}^{-3}$, then buildup

can be expected. Also, if the layer of plasma participating in ionizing incoming gas was thicker than a gyroradius due to noncoincidence of drift surfaces, then buildup would be more favorable.

Radial Density Profile and Charge-Exchange Losses

Previously, we assumed the radial density profile to be a cubic, i.e., $N_0 = N_{00} (1 - (r/R)^3)$, and we have based our neutron flux and injection current requirements on this assumption. However, we have actually calculated the radial density profile using a computer code which uses cylindrical zones of thickness equal to a gyroradius of the mean energy deuterons. The density in each zone is assumed to be uniform radially, but to be limited in extent axially as shown in Fig. A-1. The outer zone of thickness ℓ will attenuate nearly all the thermal gas impinging on the plasma surface. The plasma ions and electrons can stop the neutral gas (D_2^0) by ionization and by charge exchange. The charge-exchange process, however, results in a plasma ion being neutralized and lost unless it gets reionized by the plasma. This neutral particle will enter into the metal wall, diffuse back to the surface of the wall, and eventually reenter the plasma. The only way (since there is very little pumping) to remove the gas is by ionization. This process of the plasma pumping the gas by ionization tends to erode away the plasma surface. This erosion must be balanced by trapping the injected beam. For a stable gas/plasma interface, the following condition must be satisfied:

$$\int_0^{\ell} \ln \left[\frac{(\bar{\sigma}v)_{ii} + (\bar{\sigma}v)_{ie}}{v_0} \right] dx \geq I^* \quad (A13)$$

where I is the current of injected D_2^0 at speed v_0 , and ℓ is the distance (at least 2 gyro-radii) over which ions can contribute to ionizing neutral gas. The left-hand side is the source of current feeding the plasma, and I^* is the loss due to charge exchange.

There are then two distinct regions in the plasma: the outer layer of plasma with gas impingement and the core of the plasma with no gas. We will treat them separately.

The Plasma-Gas Boundary Layer

We will now evaluate the left-hand side of Eq. (A13). The injection current is 500 A of 65-keV D^0 ($v_0 = 2.5 \times 10^8 \text{ cm}^3 \text{ s}^{-1}$). The cross sections are $(\bar{\sigma v})_{ie} = 4.7 \times 10^{-8} \text{ cm}^3 \text{ s}^{-1}$, $(\bar{\sigma v})_{cx} = 4.2 \times 10^{-8} \text{ cm}^3 \text{ s}^{-1}$, $(\bar{\sigma v})_{ie} = 1.3 \times 10^{-8} \text{ cm}^3 \text{ s}^{-1}$. The layer thickness or mixing length will be taken as four Larmor radii of 65-keV D^+ in 3.75 T or 5.0 cm. The average density from the cubic profile is $0.77 \times 10^{14} \text{ cm}^{-3}$ in the last 5 cm of the plasma. The source of the D^+ ions in this outer zone is

$$500 \times 0.77 \times 10^{14} \left[\frac{(4.7 \times 10^{-8} + 4.2 \times 10^{-8} + 1.3 \times 10^{-8})^5}{2.5 \times 10^8} \right] \\ = 79 \text{ A}$$

The loss rate I^* due to charge exchange must not exceed 79 A. To derive I^* , we must follow the fate of a D_2 molecule as it enters the plasma.

As it enters the plasma, the D_2 molecule can be ionized or charge exchanged, which results in the loss of about one half a plasma ion to the wall since half goes outward and the other half goes inward and is reionized. The remaining D_2^+ can further ionize and be lost out the ends. We will not consider dissociation because at these energies it is a small effect.

The probability of charge exchange of D_2 is

$$\frac{\sigma_{cx}}{\sigma_T} = \frac{(\sigma_{cx v})/v_{Di}}{\sigma_{cx v}/v_{Di} + \sigma_{ii v}/v_{Di} + \sigma_{ie v_e}/v_{Di}}$$

For one molecule entering the plasma, on the average $1/2 \sigma_{cx}/\sigma_T$ of a fast ion will be lost. That is, we assume that on the average half the ions will head out and be lost and the other half will head in and be reionized.

The molecular ion can undergo charge exchange again and the number of fast ions lost is again $1/2 \sigma_{cx}/\sigma_T$; therefore, the fraction of fast ion lost per molecule in is

$$f = \frac{\sigma_{cx}}{\sigma_T}$$

However, each ion lost results in half a molecule returning, which in turn loses more ions. Therefore, the total number of ions lost per external source D_2 is

$$f + \frac{1}{2} f^2 + \frac{1}{4} f^3 + \frac{1}{2^3} f^4 + \dots = f \left(1 + \frac{f}{2} + \frac{f^2}{2^2} + \frac{f^3}{2^3} + \dots \right) \\ = \frac{f}{1 - \frac{f}{2}}$$

The loss current I^* is then given as

$$I^* = \frac{I^0 f}{1 - \frac{f}{2}},$$

where I^0 is the current of D_2 from external sources such as steaming gas from the neutralizer and from charge exchange of plasma ions on the beam.

At 70 keV,

$$\sigma_{cx} = 3.3 \times 10^{-16} \text{ cm}^2, \sigma_{ii} = 2.0 \times 10^{-16}$$

$$(\bar{\sigma v})_{ie} (7 \text{ keV} = \bar{v}_e) = 2.5 \times 10^{-8}, v_0 = 2.6 \\ \times 10^8 \text{ cm s}^{-1}.$$

$$\frac{\sigma_{cx}}{\sigma_T} = \frac{3.3 \times 10^{-16} 2.6 \times 10^8 +}{3.3 \times 10^{-16} 2.6 \times 10^8 + 2.0 \times 10^{-16} 2.6 \times 10^8 + 2.5 \times 10^{-8}} \\ = 0.527$$

$$f \approx 0.53$$

$$I^* = I^0 \frac{0.53}{1 - 0.53/2} = 0.72 I^0.$$

This means that it takes 0.72 ions to destroy each molecule that originates from an external source.

From Appendix C, we see that I^0 is made up of

$$I_{\text{ext}} + \frac{I_w}{2} I,$$

where f_w is the fraction of injected atoms which via charge exchange hit the wall, and at 70 keV,

$$f_w = 0.12 \text{ for } \gamma = 4.$$

The loss current is then

$$I^* = 0.72 I_{\text{ext}} + \frac{0.12}{2} 0.72 I.$$

For no external source and 500 A of D^0 , $I^* = 22$ A. We have allowed for 10 A of D_2 to stream into the plasma along the beam lines and 10 A to back-stream into the plasma from the end regions. Thus, I^* might be as much as 36 A. This simplified analysis indicates the plasma can maintain itself against the erosion effect of the incident gas. Much more refined studies of this plasma/gas boundary layer will be needed.

The computer code has the above assumption built in and many more refinements; however, for high-density cases of interest here, nearly all of the gas is stopped in the first zone so that all succeeding zones do not have so many processes going on. The code does take the following processes into account:

- Ions trapped in one zone spend some time in adjacent zones due to the finite gyroradius and mixing of ions from drift surfaces being split as in banana surfaces in tori. The zone thickness is taken as a Larmor diameter at the plasma edge.
- Diffusion from zone to zone due to coulomb collisions.
- Axial loss due to collisional scattering into the loss cone.
- Attenuation of the injected beam.
- Generation and reionization of fast neutrals resulting from charge exchange of plasma ions on the neutral beam. Of these neutrals, 99% are going in the beam direction rather than in the backward direction.

Buildup from Initial Target to Final State

In the previous section, the conditions were given for a target plasma which would be replaced by a beam-fed plasma by the time the target had decayed. The conditions were also given for this plasma to grow in radius as well as density. As the plasma grows, it also charge exchanges on the beam, the result being that neutral atoms impinge on the walls. Fortunately,

during buildup (which requires only a few 10's of milliseconds), the neutrals which bombard the wall do not have time to come into diffusion equilibrium and back out of the metal wall. After some time however, the wall will saturate and each atom entering will also return. The main questions then are

- Will the plasma spring up from the target?
- Will the radial density profile be roughly a cubic as previously assumed?
- After a long time, will the plasma be able to ionize the incoming gas without its surface eroding away?

To address these questions, a radial-buildup computer code was written to calculate time- and space-dependent evolution of the plasma density, attenuation of the injected neutral beam, and attenuation of the gas streaming into the plasma. The plasma was modeled by a set of concentric shells of constant density. The thickness of each shell was taken to be one gyrodiameter of the mean energy ion. The injection process due to the finite orbit size compared to a zone thickness resulted in an ion being ionized in one zone spending some time in the two adjacent zones. This was the main radial growth mechanism. Also, collisional diffusion from one zone to the next was included and contributed to radial growth. The main loss mechanisms were coulomb scattering into the loss cone and charge-exchange loss.

Several example problems were run on the code to answer the above questions. The first case had a 500-A, 16-cm-wide beam of D^0 injected perpendicularly to the field lines at the midplane. One ampere equivalent of D_2^0 gas was assumed to be streaming in along the beam line; 99 A of D^0 atoms resulting from charge exchange hit the walls. For this problem, the wall was assumed to be saturated so that 49 A of D_2^0 returned to the plasma. The unattenuated portion of the beam was the 1 A of D^0 which impinged on the opposite wall. This equilibrium distribution was established in 17 ms. The radial profile is shown in Fig. A-16. Two features should be noted: the radial profile is reasonably closely approximated by a cubic profile (as was assumed in computing the neutron flux); and the beam fills the center even though the beam is attenuated by e^{-6} across the plasma. One somewhat worrisome feature of Fig. A-1 is the way the density peaks at about a 7-cm radius. The interchange

Fig. A-16. Steady-state radial density profile and attenuated neutral beam. Note the cubic analytic fit to the calculated density profile.

instability might transport plasma inward as well as causing enhanced plasma losses outward and axially. The β there would exceed the critical β for the mirror mode. If enhanced losses would occur locally, the density might saturate at a value corresponding to the critical β .

The second case was similar to the previous one except 10 A of D_2^0 gas was included and an initial target at $3 \times 10^{14} \text{ cm}^{-3}$ density and 5-cm radius was imposed. The beam was switched on at time zero with 400 A of 65-keV D^0 . The evolution is shown in Fig. A-17. Rather than absorbing neutrals, the walls were assumed to be saturated. In spite of this the plasma did grow to its steady-state profile. Significant off-axis density peaking occurred during the buildup. Another run (not shown) was made with 50 A of streaming D_2^0 ; buildup occurred.

To illustrate how the gas erodes the edge of the

plasma, a case was run with a 20-cm plasma target at $3 \times 10^{14} \text{ cm}^{-3}$, but with 80 A of D_2^0 gas. The edges of the plasma did indeed shrink, and at the same time the density peaked. These results are shown in Fig. A-18.

Finally, a case very close to the FERF design was run at 500 A of D^0 with 34 A of D_2^0 impinging on the plasma (Fig. A-19). The density was generally lower than for a cubic profile and some off-axis peaking did occur.

Much more work is needed on buildup calculations, especially since the FERF design is at such a low energy that the plasma's ability to burn out incoming gas is near its limit. Also, the exact limit (discussed earlier) depends sensitively on the mixing-length concept, one which is not very well understood quantitatively.

Fig. A-17. Time sequence of radial density profile during buildup showing radial growth.

Fig. A-18. Time sequence of radial density profile showing the effect of the neutral gas eroding away the plasma edge.

Fig. A-19. Steady-state radial density profile.

REFERENCES

- A1. D. E. Baldwin and J. D. Callen, Phys. Rev. Letters **28**, 1686 (1972).
- A2. J. H. Foote, Plasma Phys. **14**, 543 (1972).

Appendix B - Neutral Beam Injectors

SELECTION OF INJECTOR TYPE

The principal factors determining the selection of an injector for a fusion reactor are the required energy and current of the injected beam; the tolerable gas load upon the trapped plasma; the beam access; the available beam densities; and the efficiencies of beam acceleration, neutralization, and trapping. The choice of primary ion species (atomic or molecular, positive or negative) is governed by the efficiency of conversion to neutral particles at the required energy and by the relative difficulty of ion production.

Available densities of the primary beam are determined by technological limitations and by fundamental limitations in the ion source and in the accelerator structure. Averaged over large extraction areas, these limitations are currently as follows:

- Available source densities are now about 300 mA/cm² for positive atomic ions (D^+ and T^+); 100 mA/cm² for molecular ions (D_2^+ and D_3^+); and 12 mA/cm² for negative ions (D^- or T^-).
- Limitations on beam power within the high-voltage accelerating structure are due to electrode heating, back-streaming electrons, x rays, gas evolution, and related problems. Power densities of about 4 kW/cm² have been achieved in continuously operated systems. Power densities are limited by electrode warpage or melting: this limitation (which will apply to the FERF injectors) will probably be eased but not overcome by improvements such as better optics and better cooling to protect the electrodes from the beam. However, such improvements do not appear essential to fulfilling the FERF injection requirements. Of course, higher densities can be produced by pulsed systems.^{B1}
- These beam densities apply to the primary beam produced by the ion source and accelerator. They are reduced by the efficiencies of the neutralizer (50% to 75%) and of trapping in the reactor (40% to 95%). Also, the beam extraction area may be as small as 15% of the total source assembly area, the desired ion specie (D^+ , D_2^+ , or D_3^+) will probably constitute no more than 70% of the total beam, and access of the beam to the reactor is restricted by magnet coils, neutron shielding, and other require-

ments. For these reasons, and also to reduce the gas flow (proportional to beam area) from the injection system into the main chamber, the density of the injected beam must be high (0.4 to 2.0 A/cm²). Therefore, it is necessary to increase the beam density by a factor of 50 to 300.

The beam density maybe increased either by a system of converging neutral beams or by a magnetic guide field to guide charged ions into the reactor. In one way or the other, the system must act as a funnel to concentrate the beam. The magnetic guide field^{B2} is usable with negative ions or molecular ions but is not usable with positive atomic ions (D^+ or T^+).

Riviere^{B3} has surveyed the neutralizing efficiencies for all possible D and T ion species as a function of energy. From his data (Fig. A3.8 in Ref. B3), it can be seen that for D^0 energies below 100 keV, the highest power efficiency for production of neutrals is attainable by charge exchange of D^+ in a gas neutralizer. This efficiency (for $D^+ \rightarrow D^0$) drops rapidly with increased energy, while the power efficiency for neutral production from negative ions ($D^- \rightarrow D^0$) is insensitive to energy. The result is that these two processes are competitive at 100 keV, but that D^+ is preferred at lower energies because of the greater ease of D^+ production. For the energy of optimum neutron yield (about 65 keV) the D^+ neutralization efficiency in D_2 gas is 68% for equilibrium cell thickness.

Much information is available on the use of various gases, vapors, and thin foils as neutralizing targets. The best results consistent with FERF requirements have been attained in the 2XII^{B4} and Ormak^{B5} experiments using hydrogen or deuterium gas in a neutralizing cell closely coupled with the ion source. In this configuration, the neutralizing gas does not perturb the ion source and charged-beam blow-up due to a residual space charge is avoided. However, this technique is suitable only if excitation of the atomic neutral beam is not required and if means can be found for pumping the large quantity of hydrogen gas.

Preliminary designs have been considered for two types of neutral injection systems (using D^+ and using D^- as the primary beam) and also for a D^- injection system using a magnetic guide field. The $D^+ \rightarrow D^0$ neutral injection system (similar to the 2XII and

Ormak systems) was chosen for FERF because it is most favorable for energies under 100 keV and because it requires the least amount of new development. This choice is subject to several assumptions, each of which will be examined in this report:

- The 65-keV of D^0 energy is optimum for neutron production, but must be proven acceptable with respect to bombardment of the first wall of the reactor by neutrals produced by charge exchange of the neutral beam penetrating the reactor plasma. The beam energy must also be proven suitable for beam penetration and buildup of a trapped plasma of the desired diameter, density, and spatial distribution.

- Ion sources and acceleration systems suitable for continuous operation at voltages of 65- to 100-keV must be developed. In this respect, the FERF requirements represent an advance over high-current, pulsed systems now in operation at lower voltages.

- The system must operate in a radiation environment of at least 10^{11} 14-MeV n/cm²-s in addition to radiation by slower neutrons, gamma rays, and x rays and contamination by tritium. The sources must operate continuously for very long periods (6 months, perhaps) without maintenance because of the difficulties imposed by neutron-induced radioactivity and by tritium contamination. Also, the possibility of insulator damage by neutrons must be examined.

INJECTOR LAYOUT

The yin-yang magnet provides the opportunity to install four wide-angle injection beam lines between the two coils. These wide-angle beam lines, two of which are shown by Fig. 1 (page 5) are well suited for converging neutral beams entering the magnetic field at an angle almost perpendicular to the flux lines. Therefore, the trapping will be almost 100% if the plasma density is sufficiently high, since none of the ionized particles will be in the loss cone. Beam exits will not be possible for these wide-angle beams. However, after passing through the main chamber the beams will be sufficiently well spread out for the cooled walls to accept the beam power even if there is no attenuation by the trapped plasma.

Additional beam entrances and exits are available near the x and y magnet axes, as in the case of the

Baseball and 2XII beam paths. However, these are not well adapted for neutral beams converging from a very broad source array and therefore will not be relied upon for the main injection system. The access paths along the x and y axes will be useful for irradiation of large samples, plasma diagnostics, and perhaps for reactor startup.

The layout indicated by Fig. 1 (page) provides a 20-cm² entrance window for each of the four beam lines. At a distance of about 4 m from the ion source extractors, the entrance window defines an acceptance angle of $\theta = \pm 1.4$ degrees. At 65 keV, the maximum transverse particle energy acceptable by this geometry is

$$65 \text{ keV} (1.4 \text{ degree}/57.3 \text{ degree})^2 = 20 \text{ eV}.$$

The 20-eV transverse energy acceptable by this geometry may be compared with a 7.4-eV Gaussian transverse energy measured on a 16-keV beam at LBL^{B6} and a 8- to 10-eV Gaussian transverse energy measured on a 1-keV beam at LLL^{B7}. These transverse energies represent the effects of source plasma temperature, optical aberrations, and minimal scattering and blowup in the neutralizer.

If the transverse energy in the FERF beam is 10 eV, the Gaussian 1/e divergence angle will be

$$\bar{\theta} = (10/65,000)^{1/2} = \pm 0.7 \text{ degrees}.$$

The fraction of the neutral beam lost at the entrance window will be

$$\exp - (\bar{\theta}/\bar{\theta})^2 = 0.02.$$

Each of the four injector assemblies consists of an array of ion source modules approximately 2 X 2 m in overall dimensions. Using plasma sources of the dimensions of the Berkeley 50-A source, the source array will consist of a maximum of 28 modules arranged in a 4 X 4 matrix as shown by Fig. B-1. The subdivision into D^+ and T^+ sections can be adjusted.

The injector layout now being described is designed for a nominal neutral beam requirement of about 500 A at 65 keV of D^0 energy and 100 keV of T^0 energy. This beam requirement includes a factor of three to allow for enhanced scattering losses. To inject 500 A

Fig. B-1. Layout of 28 source modules for each injector assembly. The subdivision into D^+ and T^+ sections can be adjusted to optimize this ratio.

of D^0 and T^0 into the reactor plasma at full energy, the sources must produce about 1200 A of primary ions in order to allow for the 67% neutralizer efficiency and 30% beam losses. The latter has two causes: 10% to 25% of the primary beam will probably consist of molecular ions; and 5% to 10% of the beam will be lost by collisions with duct walls, collimators, and background gas. The molecular ions will be included in computations of the power load and the gas loads, but will be excluded from computations of the neutron yield because the half-energy trapped ions derived from molecular ions will be rapidly scattered out of the reacting plasma.

The beam energies (65 keV for D^0 and 100 keV for T^0) have been chosen to maximize the neutron flux from a high- β , mirror-contained plasma and to equalize the speeds of the injected neutrals. Therefore, the neutralization efficiencies and the beam penetrations for D^0 and T^0 will be equal. To equalize the trapped densities of D^+ and T^+ and compensate for the effects of the ion mass and the ion energy upon the trapped lifetime, the injected beams should consist of approximately 364 A of D^0 and 136 A of T^0 .

The injector assemblies will be designed to fulfill the nominal requirement for a 500-A injected beam by using an average of only 3.5 of the four beam lines.

This will permit shutting down one beam line at a time to recycle the cryogenic pumps. The time-averaged injected beam will exceed the requirement if all four assemblies are operated part of the time, since the recycling of the cryogenic pumps will occupy a small fraction of the total operating time.

In Table B-1, the injected beam from four source assemblies of 28 modules is computed under the assumptions described above. It may be convenient (depending upon buildup calculations) to subdivide the 28 modules into 18 D^+ source modules and 10 T^+ modules, as shown by Fig. B-1. The source module specifications are based upon design considerations which will be explained later.

We must also consider the possibility that injection requirements may be upgraded at a later stage of FERF design because of increases in magnetic field strength, plasma volume, plasma loss rates, or neutron flux requirements. At this time, we shall indicate the respects in which the injector design may be upgraded, but shall not carry out the upgraded design in detail.

- The beam density limitation of 4 kW/cm^2 is technological, not fundamental. We expect that this limitation will be raised by technological improvements, but it is difficult to predict the ultimate limitation.
- The source module design now presented provides for a beam area of only 210 cm^2 , although the overall module size is 1500 cm^2 . Most of the module area is occupied by insulators, magnetic shielding, electrode mountings, and duct walls. The overall area could be more effectively utilized if the module size were increased, since the size of the insulators, shielding, and mountings do not scale directly with module size.
- It may be possible to increase somewhat the area of the injector array by lengthening the array in the direction perpendicular to the view shown by Fig. 1 (page 5). This increase would result in a nonrectangular array and probably would require relocation of the magnet structure and an increase of the window area at the entrance to the main chamber.
- The requirements for beam power and gas pumping will scale directly with the upgraded beam requirements. A large fraction of the gas load on the reactor plasma will also scale directly with the increased beam; however, the diffuse gas and streaming gas from the

Table B-1. Injected-beam parameters.

Source module specifications:			
Overall module size	27.5 X 52.5 cm = 1444 cm ²		
Ion extraction area	3 X 2 X 35 cm = 210 cm ²		
Beam power density	4 kW/cm ²		
Source module:	Deuterium	Tritium	Total
Beam energy (keV)	65	100	
Beam current density (mA/cm ²)	60	40	
Beam current per module (A)	12.6	8.4	
Total beam from 4 injector assemblies (A)			
Number of modules	72	40	112
Total beam current (A)	907	336	1,243
Neutral beam produced at 67% conversion efficiency	605	224	829
Charged beam to be dumped at first pumping stage (A)	302	112	414
Neutral beam losses (30%) due to molecular ions, background gas, collimation, etc.	182	67	249
Injected neutral beam from four injector assemblies	423	157	580
Time-averaged injected beam if average usage is 3.5 assemblies	370	137	507
Nominal injection requirement	364	136	500

neutralizers will depend only upon neutralizer area and not upon beam density.

GAS PUMPING REQUIREMENTS

Consideration of the requirements for pumping the source gas and neutralizer gas must precede detailed design of the ion source and neutralizer modules. The required gas flow rates and pressures are based upon source gas flow rates, neutralizer pressures and conductance, and beam-line pressure and pumping speed.

Source-Gas Flow Rates

The total ion beam produced by each injector assembly of 28 modules is 311 A. Source-gas efficiencies (the ratio of extracted ions to gas molecules fed to the source) are typically 30%, which implies a gas-feed rate of 1,040 equivalent amperes. Since the beam is mostly monatomic and the gas molecules (D₂ and T₂) are diatomic, the gas flow into the neutralizer

assembly is about 880 A. The pumping requirement for each injector assembly is 922 A, including the neutralizer gas and the gas produced by dumping the residual charged beam. This is recapitulated in Table B-2.

Neutralizer Pressures

The neutralizer dimensions and pressure will be adjusted to provide adequate gas thickness for neutralization of the beam by electron capture collisions, but the pressure will be limited to 10⁻³ Torr to avoid high-voltage breakdowns and premature electron capture during the acceleration. To attain an efficiency near the maximum the neutralizer thickness π should be about 0.8×10^{16} molecules/cm². A satisfactory neutralizer design, shown by Fig. 13 (see page 14), consists of a cooled, 60-cm-long, magnetically-shielded duct at a uniform gas pressure determined by the gas impedance of vanes at the output end. The desired thickness is attained at 10⁻³ Torr by cooling the warm gas by collisions with the cold wall.

Table B-2. Gas-pumping requirements.

Gas to be pumped, each beam line:	
From neutralizers:	880 A, diatomic
From charged beam:	103 A, monatomic
Less streaming gas and diffuse gas to main chamber:	10 A, diatomic
Total pumping load:	922 A = 180 Torr-ℓ/s
Gas to be pumped at first stage (pressure = 3.7×10^{-4} Torr): 166 Torr-ℓ/sec	
Required pumping speed:	4.5×10^5 ℓ/s
Gas to be pumped at second stage (pressure = 3×10^{-5} Torr): 14 Torr-ℓ/s	
Required pumping speed:	4.5×10^5 ℓ/s

In Appendix I, it will be shown that the required pumping speed can be attained by 5-m² cryogenic panels for each of the two pumping stages.

PLASMA SOURCE MODULES

The layout of the plasma source modules is based upon the 50-A modules developed at LBL^{B9} for pulsed injection into 2XII. However, they will operate at a lower current density because of the higher voltage and continuous operation required for FERF. Larger modules being developed at LLL will probably be preferable. The extraction area of the 50-A source is 7 × 35 cm; however, probe measurements indicate that adequate plasma for extraction is produced over a larger area. We expect that the usable extraction area will be reduced by the need for cooling circuits within the grid. To make ample allowance for cooling and rigidity of the grids, we shall design for only 210-cm² extraction area although the plasma area may be 350 cm² per module. Such a layout is shown by Fig. 11 (see page 12).

With an extracted beam density of 60 mA/cm², the plasma source must be designed for at least 21 A of extracted beam. In comparison with other LBL plasma sources (those not using magnetic fields), this implies an arc current requirement of about 2100 A and an arc voltage of about 50 V. To obtain the

required stability and uniformity of the arc discharge, the cathodes must be designed to emit the 2100 A thermionically, without relying on other emission processes which are subject to fluctuation.

We have studied a cathode design for emission of 2100 A from tungsten filaments at 2700K with a design lifetime of at least 6 months (4,000 h). Hot tungsten was chosen for the cathode material because of its predictable performance and lifetime (the latter being limited only by evaporation). However, this choice is probably not optimum because of the high temperature and high power requirement (120 kW/module) required to attain the emission from a tungsten surface. A large economy will be possible if low-temperature cathodes, perhaps oxide-coated, can be proven satisfactory for the FERF requirements. Further testing and development will be required to ascertain whether oxide or thin-film cathodes will adequately resist the effects of ion bombardment and poisoning under FERF conditions.

It is most convenient to heat the filaments ohmically, using low-voltage three-phase, 400-Hz a.c. heating power. A simple filament mounting, shown by Fig. 11 (see page 12), consists of four filament conductors in parallel connected to form each of the three sides of a delta-connected filament circuit.

The LBL sources typically have gas efficiencies of 25% to 35%, where we are defining the gas efficiency as the ratio of extracted beam ions (mostly D⁺) to gas molecules (D₂) fed to the source. Therefore, we are using a gas efficiency of 30% to compute pressures and pumping requirements in the neutralizer and in the beam line. Although conditions will be optimized for D⁺ production, we expect that 10% to 30% of the extracted beam will consist of D₂⁺ and D₃⁺.

DESIGN CONSIDERATIONS: ACCELERATION-DECELERATION ELECTRODES

It has been established in several ways^{B10} that the required beam optics and beam density can be achieved if the long-term problems of voltage-holding, rigidity, thermal distortion, and mechanical tolerance can be solved. Therefore, the detailed optimization of the beam optics will be deferred until solutions are found to these other problems.

The efficiency of conversion of D^+ or T^+ to D^0 or T^0 by electron capture from D_2 or T_2 gas is

$$F = \frac{\sigma_{10}}{\sigma_{10} + \sigma_{01}} (1 - \exp(-\pi(\sigma_{10} + \sigma_{01})))$$

= 0.67 for the assumed thickness π .

The cross sections for electron capture and for reionization are $\sigma_{10} = 3.2 \times 10^{-16}$ and $\sigma_{01} = 1.48 \times 10^{-16} \text{ cm}^2/\text{molecule}$ for both 65-keV D^0 and 100-keV T^0 .^{B8} Elastic scattering can be neglected at these energies; the only loss is by competition between the two charge-changing cross sections σ_{10} and σ_{01} . The exponent in this equation (equal to the average number of charge-changing collisions for each beam particle) is 3.8, which implies that the neutralizer is essentially infinitely thick. The neutralizer thickness specified here is two or three times greater than the thickness required at lower energies because of the reduction in capture cross sections with energy. However, the efficiency ($F = 67\%$) is still reasonably high. Electron capture is the most practical means of neutral production at this energy.

Beam-Line Pressure and Pumping Speed

After the neutrals emerge from the neutralizer, they must be transported through the main beam line without there being excessive losses by reionizing collisions with the background gas. Therefore, the maximum allowable background pressure in the beam line may be specified by the requirement that not more than 5% of the neutral beam be lost by reionizing collisions during the 3.5-m transit from the neutralizer through the main beam line to the reactor trapping chamber; i.e.,

$$n_{\text{gas}} \sigma_{01} L < 0.05$$

$$n_{\text{gas}} \leq 10^{12} \text{ molecules/cm}^3$$

$$\text{or } p_{\text{gas}} \leq 3 \times 10^{-5} \text{ Torr}$$

It must also be ascertained whether the gas loads on the reactor plasma due to diffuse background gas and the direct streaming gas from the neutralizers are tolerable. As the computation carried out in Appendix A shows, these gas loads are tolerable and are smaller than the gas loads from the first wall. Beam-line pumping may therefore be designed for the pressure requirement of 3×10^{-5} Torr, for a total flow rate from the neutralizers of about 910 A of gas, and also for gas produced by dumping 107 A of monatomic charged beam. At a pressure of 3×10^{-5} Torr, the required pumping speed would be impractically high ($6 \times 10^6 \text{ l/s}$). Therefore, to handle this large quantity of gas, most of it must be pumped at a higher pressure.

By installing an array of differential pressure ducts, as shown by Fig. 12 (page 13), a pressure ratio of 12/1 may be maintained between two pumping stages. A substantial economy can be achieved by pumping most of the gas at the higher pressure of the first stage. Conductances will be adjusted so that the two pumping stages will operate with approximately equal pumping speeds in l/s in order to minimize the total requirement. Because of the unequal pressures, 92% of the gas will be pumped at the first stage. The results of a preliminary optimization are shown in Table B-2 and by the profile of gas pressures shown in Fig. B-2.

Fig. B-2. Gas-pressure profile along beam line.

Voltage and Spacing Requirements

To avoid space-charge beam blowup, the applied electric fields should, on the average, be much larger than the space-charge electric fields in the acceleration-deceleration region where there are no neutralizing electrons; i.e.,

$$V_{\text{applied}}/d \gg jx/\epsilon_0 v$$

$$\text{or } j \ll \epsilon_0 \sqrt{2q/m} v^{3/2}/xd, \quad (\text{B1})$$

if $V_{\text{applied}} = 1/2 m v^2/q$, and where x is the half-width of the beam. This space-charge limitation for beam blowup is more rigorous in most cases than the familiar Child's Law space-charge limitation for beam extraction:

$$j \leq \frac{4}{9} \epsilon_0 \sqrt{2q/m} v^{3/2}/d^2. \quad (\text{B2})$$

Also, to prevent electron backstreaming, the deceleration voltage must be larger than the beam space-charge potential; i.e.,

$$V_{\text{decel}} > j x^2/2 \epsilon_0 v.$$

To maximize the beam density, j , at a given velocity, v , we are led by these requirements to minimize the electrode gap, d , and the aperture half-width, x . Other limitations associated with the beam space charge also constrain the electrode dimensions to be small. The space-charge limitations are all related in general to voltage, beam density, and dimensions as shown by Eqs. (B1) and (B2). We now must ascertain the minimum practical values of the dimensions d and x which are consistent with FERF requirements for voltage-holding, rigidity, mechanical tolerance, and thermal distortion.

Voltage-Holding Requirements

The scaling law for breakdown across a low-pressure, high-voltage gap is usually reported as

$$V_{\text{breakdown}} = C d^{1/2}, \quad (\text{B3})$$

where the empirical constant, C , is typically found to have values between 46 and 60 $\text{kV} \cdot \text{cm}^{-1/2}$ for ion

source conditions and may be as high as 300 $\text{kV} \cdot \text{cm}^{-1/2}$ for idealized high-vacuum conditions. The value of the exponent has been empirically debated, but with general agreement that the breakdown condition is nonlinear and that the exponent is less than unity. This scaling law depends very weakly upon the background gas pressure if the following conditions apply:

- The mean free path for ionizing collisions should be longer than the gap to avoid cascades of electrons. This effect can be seen from the Paschen curve,^{B1} which shows the worst conditions for breakdown at $p_0 d = 1 \text{ Torr} \cdot \text{cm}$. In the ion sources, we shall use $p_0 d \leq 2 \times 10^{-3} \text{ Torr} \cdot \text{cm}$, for which the Paschen curve does not apply.

- By eliminating all magnetic fields, we shall avoid pressure-dependent Penning discharges.

- Voltage gradients at surfaces of insulators must be substantially lower than voltage gradients across vacuum gaps. This requires stepped electrode mountings, which also serve as shields to protect the insulators from bombardment by particles and photons.

By combining the space-charge limitation with the voltage-holding limitation, we find that the maximum current density is attainable with the smallest possible value of d and with the correspondingly small value of $V_{\text{breakdown}}$. Here we must recognize that x and d are not entirely independent, but must be related in some way ($x = kd$, perhaps) to make the extraction geometry correct for beam optics.

$$j \propto v^{3/2}/xd = (C d^{1/2})^{3/2}/k d^2$$

$$\propto d^{-5/4} \propto V_{\text{breakdown}}^{-5/2} \quad (\text{B4})$$

This result indicates that at the first acceleration stage, where space-charge limitations are most severe, we should use the minimum values of d , x , and $V_{\text{breakdown}}$ consistent with other requirements. The same limitations apply to other acceleration stages, but are less severe because of the higher velocity. Therefore, we may use higher voltages and larger gaps at the second and third acceleration stages.

Electrode Design

Figures 11 (page 12) and B-3 show a preliminary layout of a two-stage acceleration-deceleration electrode assembly designed for a total voltage of 65 kV.

Fig. B-3. Two-stage acceleration-deceleration electrodes used for a total acceleration of 65 kV (side view).

The electrode dimensions shown by this layout are compatible with the relatively high voltage and the modest beam density required for FERF, but are two or three times larger than the electrode-dimensions currently used for high-density pulsed beams.^{B10} Here we are guided largely by a series of tests in 1967-1968 which were directed toward development of a 5-A, 100-kV neutral injection system^{B12} which essentially fulfilled the FERF requirements. These tests (limited at that time to an area of 3 cm² and voltages up to 50 kV) indicated that the goals can be achieved with a larger source area and with a third acceleration-deceleration stage to attain a total of 100 kV. Recent improvements have occurred at several laboratories in respect to densities, optics, and areas of pulsed beams at voltages up to 40 kV.^{B10} The FERF voltage requirements (65 to 100 kV) represent an advance with respect to existing wide-area multiaperture sources, but have been greatly exceeded by single-aperture sources such as the injectors for DCX-2.^{B13}

The phenomena requiring consideration with respect to the higher voltage and continuous operation of FERF are electrode heating and sputtering, x-ray and gas evolution due to backstreaming electrons, mechanical rigidity and tolerances, and thermal distortion. It has been shown at several laboratories that the beam power density limited by these effects is at least 4 kW/cm² for large-area beams. This limitation is technological, not fundamental, and will probably be raised by improvements such as water-cooled grids.

Free electrons will be produced in large quantities throughout the assembly by ionization of the background gas, by bombardment of surfaces by ions, electrons, and photons, and by ionizing radiation. Backstreaming must be prevented by a deceleration stage at the end of the acceleration column and probably also (to suppress electron multiplication) by intermediate deceleration stages after each acceleration stage. Because electrostatically trapped electrons neutralize the beam space charge, the deceleration electrodes may be made relatively thick (perhaps 10-mm) and therefore can be well cooled and will have a long sputtering lifetime. The thick deceleration electrodes can also be utilized to shield the thinner acceleration electrodes from ion and electron bombardment.

Electrodes can be cooled and mounted so that the thermal expansion is minimal and one dimensional. The stepped electrode mountings will accommodate the expansion and avoid thermal deformation. These design problems are simple with the reasonably large dimensions (~5-mm) shown by Fig. 11 and B-3 but are difficult with the thin grids required for high-density pulsed beams.

Beam deflection due to electrode misalignment has been measured at several laboratories and has been found to vary inversely with aperture width and electrode gaps. Therefore, we expect that misalignments of the order of 0.1 mm will be tolerable with respect to the ±14 degree FERF acceptance angle. This statement is extrapolated from beam deflections measured at LBL and Oak Ridge^{B14} using sources operating at lower voltages and with smaller electrode dimensions; therefore, it will require verification under FERF conditions. The 0.1-mm alignment tolerance is well within standard technology in respect to fabrication, assembly, rigidity, and thermal expansion.

SOURCE RELIABILITY

One of the critical requirements of the FERF injector system is the long-term reliability of source operation. Because of neutron activation and tritium contamination, it will be virtually impossible to work on the sources after they have been placed in operation. It will be necessary to assure reliable, maintenance-free performance of the source assemblies for at least 6 months (4,000 h). At the end of 6 months of FERF operation, it will be possible (if necessary) to replace the injectors with a completely new assembly. Probably, the used injectors must be discarded or stored until the level of activity permits maintenance work.

A large body of experience is available on the reliability of sources which have been continuously operated for long periods. This has been the subject of intensive development for electrostatic propulsion^{B15} in which power densities and plasma densities were somewhat lower than, but with reliability exceeding, the FERF requirements. In this report, we shall refer to experience acquired in the course of many thousands of hours of continuous operation of Calutron^{B16} and MATS^{B2} ion sources. We have also compared experiences with many other laboratories.^{B17, B18} The most

important results were attained because certain source components failed. We have learned, for example, that the lifetimes of oxide cathodes and graphite electrodes are limited by ion erosion and that Lucite insulators are subject to deformation under heat and stress. Such components must be replaced with more durable ones for the FERF injectors, although they may be satisfactory in other applications.

The operating experience of the MATS sources is relevant to the lifetime projected for the FERF plasma sources. After a developmental period, both MATS I and MATS II^{B2} have been operated with no maintenance or repairs whatsoever for periods of one year, including over 1,000 h each of continuous Baseball operating time (up to 12 h per day), except for periodic replacement and recoating of oxide filaments and repairs to external water, gas, and electrical systems. At the end of these runs, the sources were disassembled and found to be in good condition except for one or two weak spots that are correctable by redesign.

The LLL Calutron^{B16} has also operated for several months with essentially no maintenance except for replacement of graphite electrodes. This experience provides some information on the lifetime of a tungsten filament (comparable in size and temperature to the FERF filaments) exposed to intense ion bombardment. Because of a 2.5×7.5 -mm defining aperture limiting the 5- to 10-A arc current, the Calutron filament was subjected to ion bombardment with an arc current intensity of 25 to 50 A/cm². Under these conditions, the ion erosion was more severe than the tungsten evaporation; the lifetime of the 3-mm filament was 300 h. Therefore, we do not expect that ion erosion will determine the lifetime of the 8-mm FERF filaments which will operate with an arc current density of only 2 A/cm².

Long-term operating experience is also available at other laboratories, notably, for example, on the DCX sources at ORNL.^{B18} We prefer, however, to permit other laboratories to report their own experiences on source reliability, because the details of these experiences are largely unpublished.

A survey is being made by Battelle of the available data relevant to radiation damage to ion source insulators. Results of this survey will not be available in time for this report, but will necessarily be con-

sidered in the choice of insulator materials. If radiation effects are proven to be severe in respect to ion source insulators, it may be necessary to shield the ion sources from neutrons and other radiation by using a magnetic guide field^{B2}, rather than converging neutral beams.

By drawing upon such experiences and using care in design, selection of materials, and source assembly, we expect that the FERF reliability requirement is attainable. A developmental period under realistic operating conditions will be required.

INJECTOR POWER SUPPLY REQUIREMENTS

The requirements for injector power supplies are determined by the requirements of the injected beam (see Table B-1) and by the injector design. Table B-3 shows the injector power supply requirements and the total connected load determined by these requirements. However, for the reasons listed below, the average power required by the injectors will be only about 75% of the total connected load:

- On the average, the nominal requirement for a 500-A injected beam will be fulfilled by the operation of not more than 3.5 beam lines.
- The filament power requirement may be reduced by developing low-temperature cathodes with the required lifetime. The filament power requirement will also be reduced by heat from adjacent filaments, from the arc plasma, and from heat shields.
- The filament power requirement may be reduced by developing low-temperature cathodes with the required lifetime. The filament power requirement will also be reduced by heat from adjacent filaments, from the arc plasma, and from heat shields.
- The deceleration current actually supplied will be determined by the ion beam intercepted by the deceleration electrodes. This beam will be less than 2% of the total beam. However, to maintain constant voltages in spite of source fluctuations, the deceleration power supplies must be current-rated for 20% of the total beam current. This constitutes a small fraction of the total, connected power load.

Table B-3. Injector power supply requirements determined by injected beam requirements shown by Table B-1.

	Deuterium	Tritium	Total
Number of modules in 4 beam lines	72	40	112
Acceleration power supply requirements:			
Total voltage; sum of 2 or 3 stages (kV)	65	100	
Current/module at 4 kW/cm ² (A)	12.6	8.4	
Total acceleration current (A)	907	336	1,243
Total acceleration power (MW)	59	33.6	92.6
Deceleration power supplies			
Total voltage, possibly required for sum of 2 or 3 deceleration stages (kV)	15	15	
Deceleration current; 20% of acceleration current (A)	194	67	261
Deceleration power (MW)	2.91	1.01	3.92
Arc power:			
Arc current/module (kA)	2.1	2.1	
Arc voltage (V)	50	50	
Arc power/module (kW)	105	105	
Arc power, total (MW)	7.56	4.2	11.76
Filament power:			
Filament current, each conductor (kA)	1.25	1.25	
Filament voltage; rms, 400 Hz (V)	8	8	
Filament power, 12 conductors/module (kW)	120	120	
Total filament power (MW)	8.64	4.8	13.44
Total connected load for 112 modules (MW):			
Acceleration power	59.00	33.60	92.60
Deceleration power	2.91	1.01	3.92
Arc power	7.56	4.20	11.76
Filament power	8.64	4.80	13.44
TOTAL	78.11	43.61	121.72

Systems schematics for a 65-kV, 8.3-A neutral deuterium source and for a 100-keV, 5.6-A neutral tritium source are shown in Figs. B-4 and B-5, respectively. These sources are basically scaled-up versions of our present sources. The cathode area is increased and the emission density is reduced so that the life expectancy would be about 6 months (4,000 h). To maximize the filament life by compensating for the effects of cathode evaporation, the filament voltage is regulated and programmed over the 6-month period by the control computer.

For this study, it is assumed that a pure tungsten filament would be used. Based on this assumption, the cathode power for each source is about 120 kVA. Because the filament voltage must be below the first ionization potential (about 15 V), the filament current is very high and the filament transformer must be located near each filament. Because the large concentration of sources makes it difficult to accommodate large 60-Hz transformers, we chose to go to smaller, 400-Hz units.

Fig. B-4. Ion-source power-supply interconnections (65 kV, 8.3-A neutral deuterium source).

Fig. B-5. Ion-source power-supply interconnections (100-keV, 5.6-A neutral tritium source).

Arc Power Supply

The deuterium and tritium sources operate in the positive-resistance region of the arc characteristics and do not require additional stabilizing resistance. To start the arc, the arc voltage must rise initially to about 60 V and then settle back to about 50 V. For this purpose, we chose the current-regulated power supply shown schematically in Fig. B-6.

A Delta-Star rectifier configuration was chosen because it is one of the most economical circuits for this voltage and current range. The regulating equipment and crowbar are included in the power supply. The regulator reference is adjusted by a time-sequential, BCD input which is transmitted to high voltage by means of a photocoupler.

The crowbar is needed to protect the filament strands in the event of a spark within the arc. In addition, there is an external fault input located at ground potential which is coupled to the crowbar circuitry by a photocoupler. This input can be used to interrupt the ion beam in the event of a "spark-over" between grids.

The interlock chains have been omitted in the simplified schematics. They would operate entirely at high voltage with only the chain-complete information being transmitted to ground potential by means of a photocoupler. Also, our voltage and current information would be measured at high voltage with digital voltmeters and transmitted to ground potential by photocouplers.

Fig. B-6. Arc power-supply circuit schematic.

Acceleration-Electrode Power Supplies

The power supplies for the acceleration electrode are voltage-regulated, thyristor-controlled, power supplies with a filter to remove the ripple. A typical supply schematic is shown in Fig. B-7. Each supply contains an ignition crowbar and a voltage regulator. The fault detector senses overcurrent, fires the ignition and sends a turnoff signal to the firing chasis of the ignition. The fault detector contains a recycler to reestablish voltage once the products of the fault have been cleared. The interlock chain and voltage monitoring are handled in the same way as in the arc power supply.

The power supplies for the deceleration electrode are similar except for being very much smaller.

NEUTRAL BEAM CONTROL EQUIPMENT

Each neutral beam source is a straightforward piece of electronic equipment so far as electrical excitation and control are concerned. That is, each is simply a scaled-up version of the neutral beam sources which we routinely operate. However, the increase in size, voltage, and number of sources coupled with the hostile environment in which they operate makes the problem more formidable. Under these circumstances, it is best to make each source operate automatically and to restrict information transmitted between the control room and the source to the minimum essential for proper control. Even with minimum information transmission, the quantity is still beyond the capability of a human operator with only a manual con-

Fig. B-7. Acceleration power-supply circuit schematic.

tro] system. However, with a control computer to accumulate, organize, and present the data to the operator, a thorough understanding of the simultaneous operation of these sources can easily be achieved. Also, the computer's ability to transmit programmed commands according to the operator's judgment enables a single operator to control the large source array with a high degree of efficiency.

LONG-LIFE FILAMENT DESIGN

The plasma source for each injector module will include three extraction areas (see Fig. 12 page 13), each being a 2×35 cm array through which the average extracted beam density is 60 mA/cm^2 for D^+ or 40 mA/cm^2 for T^+ . With the allowance for the blank spaces between the extraction areas, plasma must be produced for 21 A of ions. This requires a 2100-A arc current per module which must be emitted thermionically to prevent plasma fluctuations at the cathodes.

To avoid frequent replacement and repair of components activated by neutrons, a lifetime of about 6 months is required for all source components. The most predictable cathode materials for the long lifetime are refractory metals (such as pure tungsten) for which the lifetime is determined by evaporation and not by ion bombardment. Although thin-film cathodes such as oxides and thoriated tungsten are preferable because of their lower work functions and lower power requirements, they are subject to sputtering and erosion and therefore cannot be relied upon until they have been tested and proven suitable.

Tungsten cathodes can be heated to the required emission temperature either ohmically (at a voltage less than the arc voltage) or indirectly (with higher voltage and lower current). Ohmic heating, although appearing simpler requires a large filament transformer and a sufficiently large filament cross section to prevent hot spots from developing because of non-uniform evaporation.

According to standard practice, we will require that the ohmically heated filaments not reduce their diameter by evaporation by more than 6% during their 6-month lifetime. Using a 3×10^{-8} g/cm²-s evaporation rate, we find this requires a relatively low temperature (2700 K) and a heavy tungsten filament (0.8-cm diameter). The temperature is low only in comparison with the pulsed filaments (3200 K) at LBL. Most tungsten filaments operate at even lower temperatures.

The dependence of thermal radiation, thermionic emission, and evaporation rate upon the temperature of a tungsten filament must be considered. The thermal radiation rises with the fourth power of the temperature; the thermionic emission and evaporation rise exponentially, much more steeply. Therefore, when we reduce the temperature from 3200 K to 2700 K,

we reduce the evaporation per unit area by a factor of 250 and the thermionic emission by a factor of 29, but we reduce the thermal radiation only by a factor of 2. Consequently, to maintain the same emission we must provide 29 times more area, and the total heat load goes up by 14.5. This price is paid to improve the lifetime of the filament by a factor of 4000.

We can compute^{B19} the specifications for the FERF filament and compare them to those for the LBL 10-A pulsed source. The tabulation (Table B-4) indicates that an array of 0.8-cm-diameter filaments would operate satisfactorily at 2700 K, but would constitute a large heat load. The 120-kW requirement for filament power will be reduced by reflected heat from heat shields and from adjacent filaments.

Table B-4. Specifications for ohmically heated tungsten filaments.

	FERF module	LBL 10-A pulsed source
Filament temperature (K)	2700	3200
Total emission requirement (A)	2100	1940
Thermionic emission density (A/cm ²)	2	57
Emission area required (cm ²)	1050	34
Design lifetime (continuous)	4,000	1.2 (hours)
Filament diameter required for lifetime (cm)	0.8	0.05
Filament current required for temperature ^a (A)	1250	26
Total length of conductor = area/ πD (cm)	420	216
Filament voltage desired (V)	8 (rms)	14
Voltage/length ^a (V/cm)	0.225	1.3
Length of each conductor (cm)	35	10.8
Number of conductors in parallel (connected for 3-phase a.c.)	12	20
Shape of filament array	rectangular	circular cylinder
Dimensions (cm)	16 × 41 × 5.4	13 × 5.4
Perimeter (cm)	114	41
Area of filament array (cm ²)	616	221
Spacing between conductors = area/total length of conductors		
Center-to-center (cm)	1.5	1.0
Clear spacing (cm)	0.7	0.95
Magnetic field at conductor surface due to filament current; $B = \mu_0 I / 2\pi r$ (T)	62.5	30.8
Total filament current; each feed-through; delta-connected (kA)	8.7	.500
Total filament power (neglecting effects of reflected heat and heat from adjacent filaments) (kW)	120	7

^aReference B19.

The magnetic forces and the temperature fluctuations due to the a.c. heating current have been computed and found negligible except for a time-averaged, frequency-independent magnetic force which may be a maximum of 2.2 times the force of gravity upon a conductor. Although the 400-Hz heating frequency is preferred in order to economize on the size of the transformer core, it is not required for thermal or mechanical reasons. The filament temperature, which will be affected by heat reflected from heat shields, from adjacent filaments, and from the arc plasma, will be sensed by the filament resistance and regulated by the transformer.

REFERENCES

- B1. H. C. Cole, D. P. Hammon, E. M. Jones, A. C. Riviere, and J. Sheffield, "Electrode Heat Dissipation Limits on Multi-Aperture Ion Source Performance", in Proc. of the Second International Conference on Ion Sources, 1972 (Vienna, Austria, (1972), p. 271.
- B2. G. W. Hamilton and J. E. Osher, "A Design Study of a 14,000-Ampere Injection System for a Fusion Reactor" Rept. UCRL-74096 LLL, (1972).
- B3. A. C. Riviere, Neutral Injection Heating of Toroidal Reactors, Appendix 3, Culham Laboratory, United Kingdom, Rept. C1.MR-112 (1971).
- B4. K. H. Ehlers, W. R. Baker, K. H. Berkner, W. S. Cooper, W. B. Kunkel, R. V. Pyle, and J. W. Stearns, Design and Operation of an Intense Neutral Beam Source, Lawrence Berkeley Laboratory, Rept. LBL-1589 (1973).
- B5. W. L. Stirling, R. C. Davis, T. C. Jernigan, O. B. Morgan, T. J. Orzechowski, G. Schilling, and L. D. Stewart, "Intense Ion Beam Production Below 40 keV", in Proc. of the Second International Conference on Ion Sources (Vienna, Austria, 1972), p. 278.
- B6. W. C. Cooper, K. B. Berkner, and R. V. Pyle, "Multiple-Aperture Extractor Design for Producing Intense Ion and Neutral Beams", submitted to Applied Physics Letters (1971).
- B7. J. E. Osher and G. W. Hamilton, "An Intense 1-kV Ion Source for Controlled Fusion Research", in Proc. of the Symposium on Ion Sources and Formation of Ion Beams (Brookhaven, N. Y., 1971).
- B8. H. Tawara and A. Russek, "Charge Changing Processes in Hydrogen Beams", Rev. Mod. Phys. **45**, 178 (1973).
- B9. K. W. Ehlers, Bull. Am. Phys. Soc. **18**, 1296 (1973).
- B10. Proc. of the Second International Conference on Ion Sources (Vienna, Austria, 1972), papers B-6, D-3, D-5.
- B11. M. J. Druyvesteyn and F. M. Penning, Rev. Mod. Phys. **12**, 87 (1940).
- B12. Research in High-Temperature Plasma, Aerojet-General, Rept. AFAPL-TR-68 (1968); also Five-Ampere Multimomentum Injection System for Controlled Fusion Research, Aerojet-General, Rept. SRRO-68-4 (1968).
- B13. O. B. Morgan, G. G. Kelley, R. C. Davis, Rev. Sci. Instr. **38**, 467 (1967).
- B14. R. F. Pyle, Lawrence Berkeley Laboratory, and L. D. Stewart, ORNL, private communications (1974).
- B15. D. B. Langmuir, E. Stuhlinger, and J. M. Sellen, Jr., Eds., Electrostatic Propulsion (Academic Press, New York, 1961).
- B16. F. J. Gordon and C. C. Damm, "High Intensity Source of 20-keV Hydrogen Atoms", Rev. Sci. Instr. **34**, 963 (1963).
- B17. G. W. Hamilton, "Ion Source Technology", in Proc. of the Symposium on Ion Sources and Formation of Ion Beams (Brookhaven National Laboratory, 1971).
- B18. O. B. Morgan, G. G. Kelley, and R. C. Davis, Rev. Sci. Instr. **38**, 467 (1967).
- B19. W. H. Kohl, Handbook of Materials and Techniques for Vacuum Devices (Reinhold, N. Y., 1967).

Appendix C - Wall Bombardment by Neutral Atoms

INTRODUCTION

A fraction of the injected neutral beam is ionized and trapped in the FERF plasma. The remainder of the neutral particles* either penetrate through the plasma or charge-exchange with plasma ions. The penetrating neutrals impact the first wall. The neutral atoms formed in the charge-exchange collisions, if they do not undergo reionization, also impact the first wall. The first wall absorbs the energy of the impacting neutrals, may be eroded by sputtering, and re-emits the particles as cold gas. Knowledge of injection-beam/plasma interaction is vital to FERF design.

- The trapped fraction of the injected beam must be known in order to size the injector system.
- The magnitude and distribution of the wall bombardment is important to the design of the first wall.
- The magnitude of the cold gas re-emission is important to the prediction of plasma behavior. The magnitude of the wall bombardment is very energy-sensitive, and the distribution is very nonuniform because the cross section for charge-exchange decreases steeply with increasing relative velocity between plasma ion and injected neutral.

The FERF plasma has a nonuniform ion density, non-Maxwellian ion energy distribution, nonisotropic ion velocity distribution, and a complex shape. These characteristics plus the multitude of injector beam lines make a complete analysis of injection-beam/plasma interaction very difficult. However, an approximate analysis has been carried out which retains two very important aspects of the problem, namely, the energy distribution of the plasma ions and the relative velocity dependence of the charge-exchange cross section. Simplifying assumptions in the analysis include a uniform ion density, an isotropic angular distribution of plasma ion velocities, and a spherical plasma geometry. The analysis yields the distributions of the neutral particle flux and power density at the surface of the plasma as functions of

the angle relative to the beam axis, the injection energy, the plasma energy distribution, and the beam attenuation thickness of the plasma. General results are discussed and compared to those of a previous, more simple analysis.^{C1} Then the results are used to predict the peak and average neutral particle flux and thermal load on the first wall of FERF.

ANALYTIC MODEL

The model used for the approximate analysis is shown in Fig. C-1. A monenergetic, single-species neutral of energy E_0 (velocity v_0) and particle current I_0 is injected diametrically across a spherical plasma of radius r_p . The beam diameter is infinitesimal. The plasma has a uniform density, n , of a single ion species, an arbitrary energy distribution $f(E_p)$, and an isotropic velocity distribution.

Fig. C-1. Analytic model.

At a position x along the beam path, the neutral beam current has attenuated due to ionization and charge-exchange collisions with plasma ions. The beam neutrals which are ionized in either type of collision are assumed to be trapped on magnetic field lines passing through the plasma. At $x = 2r_p$, the surviving neutral beam particles are called penetrating neutrals. A charge-exchange collision which occurs at position x between a beam neutral and a plasma ion with energy E_p and velocity vector inclination to the beam path θ results in a neutralized plasma ion at the same energy E_p and the same angle θ , where $0 \leq \theta \leq \pi$.

*Henceforth referred to as "neutral(s)."

Charge-exchange collisions with forwardly directed plasma ions predominate because the charge-exchange cross section is large for particle pairs with small relative velocities. The newly formed charge-exchange neutral particle* of energy E_p and inclination θ must travel the distance y to reach the surface of the plasma. It may undergo an ionization or charge-exchange collision which results in trapping and thus not reach the surface. Of course, if it charge-exchanges, then another neutral is born. In the present model, this second-generation charge exchange is neglected, and the attenuation of the charge-exchange-neutral current along the path y is assumed to be by ionization only. The total current density of neutrals reaching the surface of the plasma is the sum of the penetrating neutrals and the charge-exchange neutrals which do not reionize, and is geometrically dependent only on the angle α shown in Fig. C-1.

MATHEMATICAL DESCRIPTION

At position x , the rate of production of charge-exchange neutrals of energy E_p emitted at angle θ which reach the surface of the plasma is

$$\frac{d^3 I_{cx}}{dE_p d\theta dx} = \left[\frac{I_0}{v_0} \exp\left(-\frac{x}{\lambda_T}\right) \right] \left[\frac{1}{2} \sin \theta \, n f(E_p) \right] \times \sigma_{cx} v_c \left[\exp\left(-\frac{y}{\lambda_i}\right) \right], \quad (C1)$$

where λ_T is the total mean free path for the injected neutral (taking into account both ionization and charge exchange), λ_i is the ionization mean free path for the charge-exchange neutral, σ_{cx} is the microscopic cross section for charge exchange, and v_c is the collision velocity between the injected neutral and the plasma ion; i.e.,

$$v_c = (v_0^2 - 2v_0 v_p \cos \theta + v_p^2)^{1/2},$$

where v_p is the plasma ion velocity.

The first square-bracketed term of Eq. (C1) gives the number of injected neutrals in a length dx , taking into account the attenuation due to both ionization and charge exchange. The second square-bracketed term of Eq. (C1) gives the charge-exchange reaction rate per injected neutral per unit $d\theta$ and dE_p . Note that division of this term by n followed by double integration over all θ and E_p yields the charge-exchange reaction rate coefficient as given by Riviere:^{C2}

$$\langle \sigma v_c \rangle_{cx} = \int_{E_{p,MIN}}^{E_{p,MAX}} f(E_p) \left[\int_0^\pi \sigma_{cx} v_c \frac{1}{2} \sin \theta \, d\theta \right] dE_p. \quad (C2)$$

The ionization reaction rate coefficient, $\langle \sigma v_c \rangle_i$, is evaluated in the same way as $\langle \sigma v_c \rangle_{cx}$. The total reaction-rate coefficient is defined as

$$\langle \sigma v_c \rangle_T = \langle \sigma v_c \rangle_{cx} + \langle \sigma v_c \rangle_i \quad (C3)$$

The third square-bracketed term of Eq. (C1) is the attenuation of the charge-exchange neutrals due to reionization as they travel the distance y to reach the surface of the plasma. From Eq. (C1), y is given by

$$y = r_p \frac{\sin \alpha}{\sin \theta}, \quad (C4)$$

and by the law of sines, α and θ can be shown to be related by

$$\tan \theta = \frac{\sin \alpha}{1 - x/r_p + \cos \alpha}. \quad (C5)$$

We define a plasma-attenuation parameter, γ , as

$$\gamma = \frac{2r_p}{\lambda_T}. \quad (C6)$$

Thus, by definition, the fraction of injected neutrals which penetrate entirely through the plasma is $e^{-\gamma}$.

*Henceforth referred to as "charge-exchange neutral(s)."

Recall that a mean free path may be defined in general by

$$\lambda \equiv \frac{v}{n \langle \sigma v_c \rangle} \quad (C7)$$

Using Eqs. (C6) and (C7), we rewrite Eq. (C1) as

$$\frac{d^3 I_{cx}}{dE_p d\theta d(x/r_p)} = \frac{I_0 \gamma}{4} \frac{\sigma_{cx} v_c}{\langle \sigma v_c \rangle_T} f(E_p) \sin \theta \exp \left(- \frac{x}{2r_p} \gamma \right) \exp \left(- \frac{y}{2r_p} \frac{v_0 \langle \sigma v_c \rangle_i}{v_p \langle \sigma v_c \rangle_T} \gamma \right) \quad (C8)$$

Note that v_c^* is the collision velocity between the charge-exchange neutral and the plasma ion and is not the same as v_c .

Along with Eq. (C8), we require analytic relations for $f(E_p)$, σ_{cx} , $\langle \sigma v_c \rangle_T$, and $\langle \sigma v_c \rangle_i / \langle \sigma v_c \rangle_T$. For the plasma ion energy distribution $f(E_p)$, we use the analytic expressions given by Riviere^{C2} for a mirror-confined plasma with mirror ratio $R = 3$:

$$f\left(\frac{E_p}{E_0}\right) = 0 \text{ for } \frac{E_p}{E_0} < 0.18$$

$$f\left(\frac{E_p}{E_0}\right) = -1.316 \left(\frac{E_p}{E_0}\right)^2 + 2.831 \left(\frac{E_p}{E_0}\right) - 0.4674 \text{ for } 0.18 \leq \frac{E_p}{E_0} \leq 1$$

$$f\left(\frac{E_p}{E_0}\right) = 1.684 - 1.0526 \left[-3.69 + 5 \left(\frac{E_p}{E_0}\right) - \left(\frac{E_p}{E_0}\right)^2 \right]^{1/2} \text{ for } 1 \leq \frac{E_p}{E_0} \leq 2.5. \quad (C9)$$

The energy distribution is normalized such that

$$\int_0^{2.5} f\left(\frac{E_p}{E_0}\right) d\left(\frac{E_p}{E_0}\right) = 1$$

Fig. C-2. Energy distribution of plasma ions.

Equation (C9) is plotted in Fig. C-2. Note that the energy distribution peaks at the injection energy E_0 .

For the charge-exchange cross section σ_{cx} , we use the expression given by Riviere^{C2} for protons in atomic hydrogen:

$$\sigma_{cx} = \frac{0.6937 \times 10^{-14} (1 - 0.155 \log_{10} E_c)^2}{1 + 0.1112 \times 10^{-14} E_c^{3.3}} \text{ cm}^2, \quad (C10)$$

where the collision energy E_c is expressed in electron volts. For D injected into D or T injected into T, we use Eq. (C10) after calculating E_c from

$$E_c = \frac{1}{2} \frac{m_{D(\text{or } T)} v_c^2}{e} \quad (C11)$$

Equation (C10) agrees very well with data plotted by Riviere for $100 \leq E_c \leq 10^5$ eV and yields a monotonically decreasing charge-exchange cross section with increasing E_c . For $E_c < 100$ eV, Riviere shows no data, and for such cases we calculate σ_{cx} assuming $E_c = 100$ eV.

For $\langle \sigma v_c \rangle_T$, we use the following approximation to a curve calculated by Riviere^{C2} for D injected into D:

$$\langle \sigma v_c \rangle_T = 5.90 \times 10^{-7} E_0^{-0.42} \text{ cm}^3/\text{s}, \quad (C12)$$

where the injection energy E_0 is expressed in keV.

For T injected into T, E_0 should be replaced by $2E_0/3$.

For $\langle \sigma v_c \rangle_i / \langle \sigma v_c \rangle_T$, we first make the simplifying assumption that $\langle \sigma v_c \rangle_i \cong \langle \sigma v_c \rangle_i$, i.e., that the ionization reaction rate coefficient is the same for charge-exchange neutrals and injected neutrals. Then, we use the following approximation to curves calculated by Riviere^{C2} for D injected into D:

$$\frac{\langle \sigma v_c \rangle_i}{\langle \sigma v_c \rangle_T} = 0.184 E_0^{0.29}, \quad (C13)$$

where the injection energy E_0 is expressed in keV. For T injected into T, E_0 should be replaced by $2E_0/3$. Equations (C12) and (C13) match Riviere's results within 10% in the range $40 \leq E_0 \leq 400$ keV.

The substitution of Eqs. (C9), (C10), (C12), and (C13) into Eq. (C8) completes the mathematical description of the problem.

RESULTS OF ANALYSIS

The triple integration of Eq. (C8) over all E_p , θ , and x yields the ratio I_{cx}/I_0 as a function of injection energy E_0 and attenuation parameter γ . The integral is written as

$$\begin{aligned} \frac{I_{cx}}{I_0} = & \frac{\gamma}{2\langle \sigma v_c \rangle_T} \int_0^2 \exp\left(-\frac{x}{2r_p} \gamma\right) \\ & \left[\int_{E_{p,MIN}}^{E_{p,MAX}} f(E_p) \left[\int_0^\pi \sigma_{cx} v_c \frac{1}{2} \sin\theta \right. \right. \\ & \times \exp\left(-\frac{y}{2r_p} \frac{v_0 \langle \sigma v_c \rangle_i}{v_p \langle \sigma v_c \rangle_T} \gamma\right) d\theta \left. \left. dE_p \right] d\left(\frac{x}{r_p}\right) \right] \quad (C14) \end{aligned}$$

The innermost exponential term in the integral of Eq. (C14) is a function of all three variables E_p , θ , and x , and is the only term which prevents the direct evaluation of the integral. As an approximation, we replace this term by

$$\exp\left(-\frac{1}{2} \frac{\langle \sigma v_c \rangle_i}{\langle \sigma v_c \rangle_T}\right),$$

which then can be removed from the integral. The approximation assumes that the "average" v_p is equal to v_0 , and the "average" y is equal to r_p . Substitution of the approximation into Eq. (C14) yields

$$\frac{I_{cx}}{I_0} \cong \frac{\langle \sigma v_c \rangle_{cx}}{\langle \sigma v_c \rangle_T} (1 - e^{-\gamma}) \exp\left(-\frac{\gamma}{2} \frac{\langle \sigma v_c \rangle_i}{\langle \sigma v_c \rangle_T}\right). \quad (C15)$$

Equation (C15) was previously given by Hovingh and Moir.^{C1}

The complete integration of Eq. (C14) was performed numerically for the use of deuterium, and the results are shown as solid curves in Fig. C-3. The approximate Eq. (C15) has been evaluated for the case of deuterium by Hovingh,^{C3} who used $\langle \sigma v_c \rangle$ values taken directly from Riviere's curves rather than our approximate formulae. His results are shown as points in Fig. C-3. Also shown in Fig. C-3 (the dashed curves) is the integration of Eq. (C14) for the artificial case of no reionization of charge-exchange neutrals (y was set equal to zero).

Fig. C-3. Fractional current loss of injected beam due to charge exchange.

The comparison of the dashed and solid curves of Fig. C-3 reveals the importance of the reionization of the charge-exchange neutrals in reducing the fractional loss of the injected beam. With no reionization, the loss is predicted to increase with γ , while with reionization the loss decreases with increasing γ . Although reionization must be included, the good agreement between Hovingh's approximation and the complete integration of Eq. (C14) indicates that the approximate representation is adequate for the calculation of the overall loss.

As previously given, the fraction of the injected beam which penetrates entirely through the plasma is

$$\frac{I_p}{I_0} = e^{-\gamma} \quad (C16)$$

The total fractional current loss of the injected beam due to both charge-exchange and penetration is the sum of Eq. (C16) and the curves of Fig. C-3. This total fractional loss is shown in Fig. C-4.

Equation (C8) can be rewritten for charge-exchange power, P_{cx} , by multiplying the right-hand side by the plasma ion energy E_p . The triple integration of this

Fig. C-4. Fractional current loss of injected beam due to charge exchange and penetration.

new equation over all E_p , θ , and x was performed numerically to yield the ratio of P_{cx} to the injected power, $P_0 = I_0 E_0$. For the present case, P_{cx}/P_0 is almost equal to I_{cx}/I_0 . The ratio $(P_{cx}/P_0)/(I_{cx}/I_0)$ is shown in Fig. C-5.

To investigate the spatial distribution of the charge-exchange neutrals which reach the surface of the plasma, we perform two change of variable operations on Eq. (C8). First, we change from the variables (E_p, θ, x) to (E_p, α, x) by multiplying the right hand side of Eq. (C7) by the Jacobian

$$J \left(\frac{E_p, \theta, x}{E_p, \alpha, x} \right) = \frac{\partial \theta}{\partial \alpha} \quad (C17)$$

From Eq. (C5) we derive

$$\frac{\partial \theta}{\partial \alpha} = \frac{\sin \theta \cos \theta \cos \alpha + \sin \alpha \sin^2 \theta}{\sin \alpha} \quad (C18)$$

Next, we change from the variables (E_p, α, x) to (E_p, s, x) where ds is an element of the spherical plasma surface; i.e.,

$$ds = 2\pi r_p \sin \alpha_p d\alpha \quad (C19)$$

This change of variables is accomplished by multiplying the right-hand side of Eq. (C8) by the Jacobian

$$J \left(\frac{E_p, \alpha, x}{E_p, s, x} \right) = \frac{\partial \alpha}{\partial s} = \frac{1}{2\pi r_p^2 \sin \alpha} \quad (C20)$$

Fig. C-5. Ratio of fractional power loss to fractional current loss due to charge exchange.

Performing the two change of variable operations in Eq. (C8) yields

$$\frac{d^3 I_{cx}}{dE_p ds d(x/r_p)} = \frac{I_0}{4\pi r_p^2} \frac{\gamma}{2} \frac{\sigma_{cx} v_c}{\langle \sigma v \rangle_T} f(E_p) \times \frac{\sin^2 \theta \cos \theta \cos \alpha + \sin \alpha \sin^3 \theta}{\sin^2 \alpha} \times \exp \left(-\frac{x}{2r_p} \gamma \right) \exp \left(-\frac{y}{2r_p} \frac{v_0 \langle \sigma v \rangle_i}{v_p \langle \sigma v \rangle_T} \gamma \right) \quad (C21)$$

The double integration of Eq. (C21) over all E_p and x yields the current density of charge-exchange neutrals which reach the surface of the plasma as a function of injection energy E_0 , attenuation parameter γ , and angle α . The integral is written

$$\frac{dI_{cx}}{ds} = \frac{\gamma}{2 \langle \sigma v \rangle_T} \int_0^2 \exp \left(-\frac{x}{2r_p} \gamma \right) \times \frac{\sin^2 \theta \cos \theta \cos \alpha + \sin \alpha \sin^3 \theta}{\sin^2 \alpha} \times \left[\int_{E_{p,MIN}}^{E_{p,MAX}} f(E_p) \sigma_{cx} v_c \right] \times \exp \left(-\frac{y}{2r_p} \frac{v_0 \langle \sigma v \rangle_i}{v_p \langle \sigma v \rangle_T} \gamma \right) dE_p \left] d \left(\frac{x}{r_p} \right) \quad (C22)$$

The integration of Eq. (C22) was performed numerically for the case of deuterium. Then the results were divided by the previous results for I_{cx}/I_0 (from the numerical integration of Eq. (C14)). The final result is the ratio of local to average current density of charge-exchange neutrals at the surface of the plasma. This ratio is plotted in Figs. C-6 and C-7 as a function of angle α for various values of E_0 and γ .

Fig. C-6. Ratio of local to average current density of charge-exchange neutral particles for $E_0 = 65$ keV.

Fig. C-7. Ratio of local to average current density of charge-exchange neutral particles for $\gamma = 3$.

The current density of charge-exchange neutrals is peaked on the injection beam axis, in both the forward and backward directions. The direction of the higher peak depends on the injection energy E_0 and the attenuation parameter γ . As shown in Fig. C-6, at constant $E_0 = 65$ keV the higher peak is forward for $\gamma = 1$ and 3 and backward for $\gamma = 5$. As shown in Fig. C-7, at constant $\gamma = 3$ the higher peak is forward for $E_0 = 100$ and 65 keV and backward for $E_0 = 40$ keV.

The current density of charge-exchange neutrals is mathematically infinite at both $\alpha = 0$ and $\alpha = 180^\circ$ because the injected beam of finite current was assumed to have an infinitesimal cross-sectional area. To estimate the peak-to-average current density ratio of charge-exchange neutrals for a finite area injection beam (but small beam diameter compared to plasma diameter), we assume that the current density at the plasma surface within the beam is uniform and equal to the integrated average from $\alpha = 0$ or 180° to the angle α_e corresponding to the edge of the beam. The average is calculated as follows:

$$\left(\frac{dl_{cx}}{ds} \right)_{\text{within beam}} = \frac{\int_0^{\alpha_e} \frac{dl_{cx}}{ds} 2\pi r_p^2 \sin\alpha d\alpha}{\int_0^{\alpha_e} 2\pi r_p^2 \sin\alpha d\alpha} \quad (C23)$$

or 180°

The average current density of charge-exchange neutrals within a finite beam with $E_0 = 65$ keV is shown in Fig. C-8 as a function of the angle α_e corresponding to the edge of the beam. Results are shown for both the forward and backward directions. In all cases, the average is somewhat greater than the corresponding local current density at $\alpha = \alpha_e$ for the infinitesimal beam (Fig. C-6), but not much greater because of the sine term in Eq. (C23).

Fig. C-8. Ratio of average current density of charge-exchange neutral particles within beam to overall average current density for $E_0 = 65$ keV.

Equation (C22) can be rewritten for charge-exchange power distribution, dP_{cx}/ds , by multiplying the right-hand side by the plasma ion energy, E_p . This new equation was integrated numerically and the results were found to be very similar to those for current distribution. For completeness, Fig. C-9 gives the ratio of local to average power density of charge-exchange neutrals at the surface of the plasma as a function of angle α for $E_0 = 65$ keV. Figure C-10 gives the average power density of charge-exchange neutrals within a finite beam in both the forward and backward direction.

RESULTS FOR FERF

The results of the analysis presented in the preceding section are not directly applicable to FERF without additional interpretation because of several complications. Foremost among these is the complex geometry of FERF. FERF has four injectors, two in the vertical plane and two in the horizontal. The individual

Fig. C-9. Ratio of local to average power density of charge-exchange neutral particles for $E_0 = 65$ keV.

elements of each injector are arranged on a curved surface so that the many individual beams of each injector converge to a focus at the injector port (see Fig. 1, page 5). Beyond the injector port, the beams diverge. Thus, the cross-sectional area of plasma and first wall which is illuminated by the injector is considerably more than the area of the injector port. The beam-attenuation parameter varies across the beam because of the density variation of the plasma and the different path lengths through the plasma.

An additional complication in FERF is the injection of deuterium and tritium at the same velocity and hence at different energies (65-keV D and 97.5-keV T). This injection results in a two-humped plasma ion energy distribution. Fokker-Planck calculations have shown the deuterium distribution to be peaked at 65 keV and the tritium distribution at 90 keV. The speed distributions are very similar for both. Since the collisional processes which we consider are dependent on speed, not energy, we shall assume that the fractions of the beam which charge exchange and ionize in FERF are the same as if the plasma ions were all deuterium and the injection energy was 65 keV. This is the case we have considered in the preceding analysis. Of course, when calculating power we must

Fig. C-10. Ratio of average power density of charge-exchange neutral particles within beam to overall average current density for $E_0 = 65$ keV.

remember the fraction of high-energy tritium.

Some Geometry Considerations for the Injectors

Each of the four injectors is capable of injecting 145 A of D^0 and T^0 into the plasma chamber (106 A of 65-keV D^0 and 39 A of 97.5-keV T^0). The injected power for each injector is 10.7 MW. FERF operation may require simultaneous use of two, three, or even all four injectors. The neutrals from each injector enter the plasma chamber through a 20-cm square injector port. At the injector port, the current density is 0.363 A/cm^2 , and the power density is 26.8 kW/cm^2 .

Consider the path of the injected beam with no plasma present. The area of the far wall which is illuminated by the diverging beam is about 0.81 m^2 . Therefore, the average power density on the illuminated portion of the wall due to the unattenuated beam is about 1.3 kW/cm^2 . The maximum power density is at the point on the wall nearest to the injector port and is about 3.3 kW/cm^2 . If possible,

the illuminated portion of the first wall should be designed to withstand this thermal load, at least for a short time. Such a design feature would lessen the problems of plasma startup and shutdown. The power densities involved are encouragingly close to those which are considered feasible for steady-state cooling of the first wall (see Appendix J). Aside from the thermal load of the unattenuated beam, we must be concerned about wall damage due to the impinging particles. The particle fluxes on the illuminated portion of the first wall are 1.1×10^{17} particles/cm²-s average and 2.8×10^{17} particles/cm²-s maximum.

Attenuation Parameter in the FERF Plasma

In the preceding analysis, we have used a plasma-attenuation parameter which can be calculated from Eqs. (C6) and (C7):

$$\gamma = \frac{\bar{n}(\sigma v_c)_T}{v_0}, \quad (C24)$$

where we have substituted the path length through the plasma, ℓ , for the more restrictive $2r_p$. The average density is to be calculated by

$$\bar{n} = \frac{n_0}{\ell} \int_0^\ell \frac{n}{n_0} d\ell, \quad (C25)$$

where n_0 is the central plasma density. We have calculated attenuation parameters for FERF using the calculated plasma-density contours and find a variation from $\gamma = 2.6$ for the steepest injector beam ray, to $\gamma = 3.5$ for the central ray, to $\gamma = 6.4$ for the shallowest ray.* We shall now apply the preceding analysis to FERF using these attenuation parameters.

*These values of γ may be somewhat inaccurate because they were scaled from a $\beta = 0.5$ case to a $\beta = 0.65$ case simply by multiplying by $0.65/0.5$

Total Losses Due to Charge Exchange and Ionization

Table C-1 shows the fractional current and power losses for the three representative injector beam rays. The values are estimated from Figs. C-3, C-4, and C-5. The injection energy, E_0 , is 65 keV.

The sums of the charge exchange and penetration losses from Table C-1 are shown in Fig. C-11 as a function of distance across the injected beam. We integrate under the curves to estimate the total current and power loss from the injected beam. The results are

$$\left(\frac{I_{cx} + I_p}{I_0} \right)_{Tot} = 0.16,$$

and

$$\left(\frac{P_{cx} + P_p}{P_0} \right)_{Tot} = 0.16.$$

So, 16% of both the injected current and injected power in FERF is lost to the first wall. The remaining 84% of the injected current is trapped in the plasma. For each 145-A injector (recall that FERF operation may require two, three, or even all four injectors to operate simultaneously), 23 A of neutral deuterium and tritium atoms impact the first wall and deposit a total power of 1.7 MW. Once the wall is saturated, we can expect the full amperage of deuterium and tritium to be re-emitted as cold gas. The ability of the plasma to tolerate this gas load is discussed in Appendix A.

Fig. C-11. Sum of the charge-exchange and penetration losses as a function of distance across the injected beam.

Table C-1. Fractional current and power losses for representative injector beam rays.

		Steepest ray $\gamma = 2.6$	Central ray $\gamma = 3.5$	Shallowest ray $\gamma = 6.4$
Charge Exchange:	I_{cx}/I_0	0.171	0.134	~0.05
	P_{cx}/P_0	0.165	0.131	~0.05
Penetration:	$I_p/I = P_p/P_0$	0.074	0.030	0.001
Total:	$(I_{cx} + I_p)/I_0$	0.248	0.164	~0.05
	$(P_{cx} + P_p)/P_0$	0.239	0.161	~0.05

Current Densities and Power Densities on the First Wall

We have previously defined the illuminated portion of the first wall as that portion which is in the direct path of the injected beam. As previously given, the area of the illuminated portion of the first wall for each injector is about 0.81 m^2 . The average power density on the illuminated portion of the first wall will be somewhat less than

$$\frac{1.7 \text{ MW}}{0.81 \text{ m}^2} = 210 \frac{\text{W}}{\text{cm}^2}$$

because some of the charge-exchange neutrals impact the first wall outside the illuminated portion. Likewise, the average particle flux density on the illuminated portion of the first wall will be somewhat less than

$$\frac{23 \text{ A}}{0.81 \text{ m}^2} = 1.8 \times 10^{16} \frac{\text{particles}}{\text{cm}^2 \cdot \text{s}}$$

The peak particle flux density and power density will occur at the point on the first wall illuminated by the steepest injector beam ray (for which $\gamma = 2.6$). As given previously, the particle flux density at this point with no plasma attenuation ($\gamma = 0$) is 2.8×10^{17} particles/cm²·s and the power density is 3.3 kW/cm². With plasma attenuation, the particle flux density and power density at this point due to penetration alone is just $e^{-\gamma}$ times these values, or 2.0×10^{16} particles/cm²·s and 250 W/cm². The particle flux density and power density at this point due to charge-exchange neutrals cannot be estimated from Figs. C-6 through C-10 because the FERF injector beams are nearly as large in cross section as the plasma, a case for which the analysis is invalid. We shall overestimate the peak contribution by charge-exchange neutrals by assuming that all charge-exchange neutrals travel in the same direction as the injected neutral. Then, the peak contribution by charge-exchange neutrals is just the unattenuated particle flux density or power density multiplied by I_{cx}/I_0 or P_{cx}/P_0 for $\gamma = 2.6$ (from Table C-1). The results are a peak particle flux density of 4.8×10^{16} particles/cm²·s and a peak power density of 540 W/cm². Adding the contributions due to penetration and charge-exchange neutrals yields a peak neutral-particle flux density of 6.8×10^{16} particles/cm²·s and a peak power density of 790 W/cm².

Table C-2 summarizes the predicted bombardment of the first wall of FERF by neutral atoms.

Table C-2. Summary of FERF first-wall bombardment.

Amps injected = 145 A per injector
(106 A of 65 keV % D⁰)
(39 A of 97.5 keV % T⁰)

Power injected = 10.7 MW per injector

Area of first wall illuminated by each injector
= 0.81 m²

With no plasma

Amps to the wall = 145 A for each injector

Power to the wall = 10.7 MW for each injector

Average particle flux density to wall
= 1.1×10^{17} particles/cm²-s

Peak particle flux density to wall
= 2.8×10^{17} particles/cm²-s

Average power density to wall = 1.3 kW/cm²

Peak power density to wall = 3.3 kW/cm²

With plasma

Amps to the wall = 23 A for each injector

Power to the wall = 1.7 MW for each injector

Average particle flux density to wall
< 1.8×10^{16} particles/cm²-s

Peak particle flux density to wall
< 6.8×10^{16} particles/cm²-s

Average power density to wall < 210 W/cm²

Peak power density to wall < 790 W/cm²

REFERENCES

- C1. J. Hovingh and R. W. Moir, Efficiency of Injection of High Energy Neutral Beams into Thermonuclear Reactors, LLL, Rept. UCRL-51419 (1973).
- C2. A. C. Riviere, Nuclear Fusion 11, 363 (1971).
- C3. J. Hovingh, LLL, private communication (1973).

Appendix D - Shield Analysis

INTRODUCTION

This section summarizes the results of a preliminary study of a shield design for the FERF. The calculation method and results will also be useful for the design and construction of other types of reactors.

The principle result is the amount of heat deposited in the superconducting magnet coils. Since these coils run at cryogenic temperatures, and since the required refrigeration systems have a low efficiency ($\sim 0.1\%$), sufficient shielding is required to minimize the consumption of electrical power by the refrigerator. Additional results are neutron and gamma fluxes throughout the system during operation, heating in other components, buildup of radioisotopes produced during operation, and the dose rates from these radioisotopes after shutdown.

It should be stressed that the purpose of these calculations is to show the feasibility of a FERF design and to bring to light potential problem areas. Insufficient effort has been expended to design a shield of optimum behavior or to assess the shielding impact upon other parts of the design. Although four separate system-geometry designs were made, several more designs are required for proper consideration of all possible points of interaction. Many more cases must be considered to arrive at an "optimum" system. In particular, shield materials should be considered which will reduce the residual radioactivity.

CALCULATIONAL TECHNIQUES

The majority of the neutron and gamma transport problems solved in this study made use of the MORSE-L* Monte Carlo code. This code makes use of multigroup techniques to simulate the energy dependence of the transport problem and is able to describe coupled neutron-gamma problems as well as all-neutron and all-gamma problems. A generalized, three-dimensional geometry routine is employed to describe the system. Special biasing and exponential transform techniques are available, and were used, to

solve the deep penetration problems encountered in this study. A few problems made use of the ANISN-L** one-dimensional, discrete-ordinates code.

Basic cross-section data used in both these codes were obtained from the following sources:

- Neutron data were obtained from the standard LLL evaluated cross section library^{D3}.
- Gamma data cross sections were obtained from the Plechaty-McMaster compilation^{D4} and scattering from the standard Klein-Nishina equations.
- Gamma production data were based upon experimental spectra and the LLL evaluated neutron cross sections and were normalized to conserve gamma energy.

The 30 neutron energy groups used spanned a energy range of 18 MeV to thermal. Fifteen gamma groups, from 10 MeV to 0.5 MeV, were employed. The neutron cross sections used in this study have also been used to calculate all the LLL pulsed-sphere experiments.^{D5} The results have been satisfactory for all cases. The gamma data have been employed in the solution of benchmark gamma problems with good success. A few coupled neutron-gamma problems have been solved with reasonable accuracy but there is, in general, a lack of such benchmark problems.

Activation cross-section data were obtained from the compilation of Alley and Lesser^{D6} and averaged over the neutron group structure. Decay gamma energies and intensities were taken from the sixth edition of the Table of Isotopes.^{D7} It should be noted that activations have been calculated only for those isotopes which are gamma emitters and only gammas greater than 0.5 MeV have been considered.

GEOMETRICAL MODEL

It is evident that the heating in the magnet coils, as well as other quantities dependent on neutron and gamma fluence, is very sensitive to the geometry of the system. The FERF geometry cannot be adequately described by one- or two-dimensional models with

*MORSE-L is an LLL version of the MORSE^{D1} code originally developed at ORNL.

**ANISN-L is an LLL version of the ANISN^{D2} code.

any assurance that the results will be meaningful. For this reason, a full, three-dimensional description was made of the FERF system. The model used followed the designed layout as closely as possible with limits imposed only by the available computer routines and by a limited time schedule.

Figure D-1 illustrates a cross section of a single magnet section as described for the shielding analysis. This is a cut through one magnet section at its center. The full magnet is formed by rotating this section 80 degrees each side of this point (toward and away from the viewer), with the outermost surfaces rotated about a point 80 cm behind the center of the system and the innermost rotated about the center. At the 80 degree point, the magnet section "rolls" around a fixed axis into a similar magnet section located below the first to form a complete magnet. A second, similar magnet is positioned at right angles to and to the left of the first. Figure D-2 illustrates a cut through the entire model at its center plane. Shielding is added between the magnets to fill part of the volume that normally would be left with a void gap as illustrated on this figure. Four conical holes are cut into the resulting model to simulate the rectangular holes cut to admit particles from the ion sources. The ion sources themselves are simulated by four 75-cm-thick conical frustums, located in each of these cones at positions roughly approximating the designed ion sources. The entire machine is placed in the exact center of a room 100-feet long by 60-feet wide by 60-feet high. The walls of the room are formed of a 2-cm-thick steel liner and then 3 feet of concrete.

Fig. D-1. Basic geometry used in the shielding calculations.

Fig. D-2. Cross-sectional view of the geometry model used for shielding calculations ($z = 0$ plane).

Several approximations have been made in forming this model:

- The test region, which includes the first wall and the structure to hold the test specimens (but not the specimens themselves), has been omitted in several places. (Fig. D-2 does not show this region in places where it should be.) In general, the volume of the omitted region is small compared to the volume included in the problem.
- As mentioned, the holes for the ion sources are circular rather than rectangular.
- Figure D-1 illustrates a slight volume removed from the left side of the bottom of the magnet. The designed system has a flat bottom. The extra cut made it possible to use an existing model with minimum perturbation (necessary because of a lack of time). Very little error should result from this change.
- The physical structure (C-clamp) which holds the magnet together has been omitted. This is outside of the described geometry and will not alter the magnet heating. Since it is in a position where no direct beams of unattenuated neutrons hit it, its presence will not appreciably affect the shutdown dose rates.
- The structure which holds the two magnets apart from each other was not included. As illustrated on at least one drawing, this volume could be part of the cryogenic system and could alter the radiation heating

results. It is not completely clear what this volume would look like.

- Small portions of the shield at the outer part of the "roll" sections (away from the source) have been omitted. These should not change in the calculated results.
- Items external to the described system have been omitted. These will alter the dose rate after shut down

and the calculated activation.

Figure D-3 is a computer-drawn view of the geometry and illustrates the various materials found on the faces of a box cutting through the defined geometry. A cut into the box is made to show the inside of the system. Several dozen different pictures of the system were drawn to insure that the computer model correctly duplicates the designed system.

Fig. D-3. Computer illustration of geometry model used in shielding calculations. Numbers refer to material compositions and statistical regions.

MATERIALS SELECTED FOR MODEL

Table D-1 shows the compositions used for each of the regions of the problem. Note that the region labeled "thermal insulation" is really the sum of a 5-cm-thick region containing thermal insulation and a region containing a sheet of 0.25-inch-thick Boral. The two materials are mixed homogeneously into the one region.

The composition of the main shield was chosen after a one-dimensional study of the energy deposition in a magnet located behind a water/steel shield. Figure D-4 illustrates the geometry used in this preliminary one-dimensional study as well as results obtained when the ratio of water-to-steel was varied. This study produced an optimum shield composition of 80 vol % steel/20 vol % water, and this same composition was used in the FERF study. It is expected that this may not be an optimum composition when considerations such as minimum activation and cost are studied in future designs.

In the activation of aluminum and lead, the dose rates calculated for a long time after irradiation will be the result of small impurities in the material. Tables D-2 and D-3 indicate the impurities used in

Fig. D-4. Geometry used in one-dimensional study.

this study. Note that in practice, the impurities present may vary a great deal from those listed in any handbook tabulation.

The initial source distribution of fusion neutrons in space was designed so that it would adequately simulate the distribution existing in the FERF machine. The source was broken into 12 pieces of equal length along the x axis, and each of these pieces was then simulated by four concentric rectangular boxes. The source within each box was set as a fixed number per unit volume. By specifying the dimensions of each of the 48 resulting boxes and the number of source neutrons generated in each, it was possible to simulate the actual source. Figure D-5 illustrates the outermost dimensions of the boxes while Table D-4 gives the complete description.

The source was normalized so that the uncollided 14-MeV neutron fluence at the first wall at a point closest to the center of the machine (a point located at $[0, 40, 0]$ with the coordinators used) would be 10^{14} n/cm² s. This resulted in a total integrated source of 3.00×10^{18} n/s.

RESULTS OF ANALYSIS

Energy Deposition

Table D-5 indicates the energy deposition in various parts of the FERF system. It should be noticed that

Fig. D-5. Outermost limits of source distribution.

Table D-1. Regional compositions as used in shielding calculations.

Region	Definition	Composition (at/b·cm) ^a	(g/cm ³)
Test	50 vol % SAP-895	Al	0.0294
	10 vol % H ₂ O	C	0.00258
	40 vol % void	H ₂ O	0.1
Shield	80 vol % SS304	SS304 ^b	6.336
	20 vol % H ₂ O with	H ₂ O	0.2
	6.35 g/100 cm ³ of H ₃ BO ₃ added	B	1.24 × 10 ⁻⁴
Lead shield	100 vol % Pb	Pb	0.033
Thermal insulation	11.3 vol % Boral (B ₄ C in	Al	0.00456
	Al, clad with Al)	B	0.00365
	88.7 vol % void (thermal insulation neglected)	C	0.000914
Magnet case	100 vol % SS304	SS304	7.92
Magnet	6.8 vol % Nb ₃ Sn	Cu	0.07578
	3.2 vol % NbTi	Nb	0.00379
	90.0 vol % Cu	Sn	0.000877
		Ti	0.00116
Ion sources	10 vol % SS304	SS304	0.792
	90 vol % void		
SS wall liners	100 vol % SS304	SS304	7.92
Concrete walls		H	0.01386
		C	0.00012
		O	0.04582
		Na	0.001
		Mg	0.00011
		Al	0.00174
		Si	0.01063
		K	0.00046
		Ca	0.00152
		Fe	0.00035

^aat/b·cm ≡ atoms/barn·cm, where 1 barn = 10⁻²⁴ cm.

^bSS304, at a density of 7.92 g/cm³, is assumed to have the following composition:

Element	Composition (at/b·cm)
C	0.0001584
Si	0.000792
Cr	0.017424
Mn	0.000792
Fe	0.05972
Ni	0.00792

the shield has been broken into three parts: the flat sections (Fig. D-1) within 62 cm of the centerlines (including the filler between the magnets) which is called the "inside;" the flat sections beyond 62 cm of the centerline which is called the "outside;" and the

Table D-2. Aluminum impurities (Alcoa 1199).

Element	ppm (by weight)
Si	30
Fe	10
Mn	10
Mg	1
Pb	1
Cu	1
Cr	3
Zn	14
Ti	2
V	3
Sn	2
Bi	2
B	4
Ga	10
Zr	3
Co	<2
Mo	<2
Na	<1
Ca	<1

"roll" sections where the two flat sections of the magnet roll into each other.

Allowing for a 20°C rise in the temperature of the shield cooling water, flow rates of about 5×10^4 g/s would be required. This would change the water in the shield approximately once per minute. There is adequate space available in the shield and in the test region for the required cooling.

The calculations producing the above results were biased to produce valid results in the magnet region. They also provided fluxes averaged over the various regions of the problem. Additional calculations were performed to obtain results at fixed point detector locations. Table D-6 indicates nine of these locations. The first seven are within the magnets and approximately on the first wall. Point eight is on the extreme outer location of the plasma, outside the magnets, while point nine is on the inside of an ion source. Figure D-2 indicates these positions.

Table D-3. Lead impurities (desilverized lead)

Element	ppm (by weight)
Ag	20
Cu	25
Fe	≤20
As-Sn-Sn	≤150
Zn	≤20
Bi	≤2500

Table D-4. Neutron source distributions.^a

X-position (cm)	Integrated source (n/s)	Half-width and half-height of source boxes (cm)							
		A ₁	B ₁	A ₂	B ₂	A ₃	B ₃	A ₄	B ₄
0 to 20	1.78×10^{17}	26.9	30.6	21.5	24.5	13.5	15.3	8.1	9.2
20 to 40	4.50×10^{17}	22.6	36.6	18.1	30.9	11.3	19.3	6.8	11.6
40 to 60	3.43×10^{17}	18.4	46.4	15.7	37.1	9.2	23.2	5.5	13.9
60 to 90	1.68×10^{17}	14.1	49.6	11.3	39.7	7.1	24.8	4.2	14.9
80 to 100	0.51×10^{17}	9.9	46.3	7.9	37.0	5.0	23.2	3.0	13.9
100 to 120	0.10×10^{17}	5.6	33.4	4.5	26.7	2.8	16.7	1.7	10.0

^aEleven percent of the integrated source is inside box 1 and outside box 2, 27.9% inside box 2 and outside box 3, 33.5% inside box 3 and outside box 4, and 27.6% inside box 4. The source description for negative values of X is identical except that the A's and B's are interchanged.

Table D-5. Operational energy depositions caused by neutron and gamma reactions.

Region	Energy Deposition	
	(MeV/source neutron) ^a	(kW)
Test	2.440 (0.0136)	1173 ± 16
Shield:		
flat section—inside	8.192 (0.005)	3937 ± 20
flat section—outside	0.922 (0.025)	443 ± 11
roll section	4.282 (0.018)	2058 ± 37
Lead shield	0.025 (0.189)	12.1 ± 2.3
Thermal insulation	0.0057 (0.158)	2.76 ± 0.44
Magnet cases	0.0029 (0.170)	1.41 ± 0.24
Magnets	0.0079 (0.110)	3.82 ± 0.42
Ion sources	0.0787 (0.126)	37.8 ± 4.8
SS wall liners	0.311 (0.080)	150 ± 12
Concrete walls	0.979 (0.083)	471 ± 39
Total	17.246	8290

^aValues in parentheses are the fractional standard deviation (one standard deviation divided by the quantity calculated) associated with the given result. These errors represent statistical variations in the Monte Carlo techniques used and do not include data uncertainties.

Neutron and Gamma Fluxes

Integrated neutron fluxes at the detectors are listed in Table D-7. The statistics associated with these results are poor (probably believable within only a factor of two). Since the statistics are much better for the results of the energy deposition calculation, the average fluxes in the test region were normalized to point four and are also shown in Table D-7. The neutron fluence and the gamma fluence are shown as functions of energy for the test region in Figs. D-6 and D-7, respectively.

Table D-6. Point detector positions.

Detector no.	Coordinates (cm)		
	x	y	z
1	-300	130	0
2	-200	95	0
3	-100	65	0
4	0	40	0
5	100	20	0
6	200	10	0
7	300	10	0
8	630	0	0
9	180	340	0

Table D-7. Integrated neutron fluxes at point detectors.

Detector no.	Neutron flux (n/cm ² ·s)				
	Uncollided	> 10 MeV	> 5 MeV	> 1 MeV	Total
1	2.2×10^{12}	2.8×10^{12}	3.0×10^{12}	3.4×10^{12}	4.6×10^{12}
2	5.3×10^{12}	8.5×10^{12}	9.6×10^{12}	2.7×10^{13}	5.7×10^{13}
3	2.2×10^{13}	2.6×10^{13}	2.7×10^{13}	4.0×10^{13}	8.1×10^{13}
4	1.0×10^{14}	1.1×10^{14}	1.2×10^{14}	1.3×10^{14}	2.0×10^{14}
ATR ^a	1.0×10^{14}	1.4×10^{14}	1.5×10^{14}	2.3×10^{14}	5.3×10^{14}
5	4.9×10^{13}	5.4×10^{13}	5.9×10^{13}	7.5×10^{13}	1.1×10^{14}
6	7.0×10^{12}	8.3×10^{12}	2.0×10^{13}	3.0×10^{13}	4.7×10^{13}
7	2.6×10^{12}	3.1×10^{12}	3.3×10^{12}	3.7×10^{12}	5.6×10^{12}
8	4.7×10^{11}	5.0×10^{11}	5.1×10^{11}	5.6×10^{11}	8.1×10^{11}
9	7.2×10^{11}	8.6×10^{11}	8.9×10^{11}	9.8×10^{11}	1.3×10^{12}

^aAverage test region.

Fig. D-6. Energy-dependent average neutron fluence in test region normalized to an uncollided value of 10^{14} n/cm²-s.

Fig. D-7. Energy-dependent gamma fluence in test region normalized to an uncollided neutron value of 10^{14} n/cm²-s.

Table D-8 gives the biological dose rate at the nine point detectors as well as the heating that would be caused at these points in a very small volume of SAP and stainless steel (small enough to not perturb the fluxes). Additional calculations at points outside the machine indicate dose rates from 10^5 to 10^7 rem/h for places where the neutron source cannot be directly seen.

The uncollided 14-MeV neutron flux at a point within the magnet where this flux would be maximum

Table D-8. Calculated quantities at point detectors.

Detector no.	Dose rates (rem/h)		Neutron and gamma heating rates (W/cm ³)	
	Neutron	Gamma	SAP	SS
1	7.2×10^8	2.7×10^6	0.1	0.2
2	5.0×10^9	1.0×10^8	1.3	3.3
3	8.3×10^9	1.1×10^8	1.7	3.9
4	2.5×10^{10}	1.5×10^8	3.8	6.9
5	1.5×10^{10}	2.0×10^8	3.3	6.9
6	5.0×10^9	7.6×10^7	0.9	2.1
7	7.6×10^8	1.2×10^7	0.2	0.4
8	1.2×10^8	4.8×10^5	0.02	0.03
9	2.1×10^8	1.0×10^6	0.03	0.05

has been calculated to be 9×10^7 n/cm²-s. The average value of this quantity throughout the magnet volume will be approximately a factor of 100 times less than the maximum value. The average value of the total integrated neutron fluence (over all energies) has been calculated to be 3×10^{10} n/cm²-s.

Problem output indicated that 5.25 uncollided 14-MeV neutrons per 100 source neutrons hit the outer walls of the room. An additional 0.67 collided neutrons also hit the walls. Of the 5.25 uncollided neutrons, 4.96 went between the magnets while the remaining 0.29 went through one of the ion sources (1.62 uncollided neutrons hit the ion sources; 1.33 of these had reactions with the simulated materials present).

Tritium Generated by Neutron Capture Reactions

The tritium generation in the nitrogen in the reactor room (9.2×10^{-4} gm/cm³) was calculated to be 3.17×10^{13} atoms/s or 48.5 curies/year of full operation. In the borated shield water, the production from the B(n, 2α)T reaction will be 7.80×10^{13} atoms/s or 142 curies/year. If this borated water is recycled over a several-year period, the B(n, α)Li⁷ reaction followed by a Li⁷ (n, α)³H reaction could produce far larger amounts of tritium.

SHUTDOWN ACTIVITIES AND DOSE RATES

With the large neutron fluxes present in the FERF system, there will be large amounts of activation products formed. Many of these will be within the system where they will be of interest only when it is necessary to service the machine (e.g., during replacement of the inner wall) while some will be outside in the room itself. The latter will increase difficulty in gaining access to the machine and its associated equipment.

The long-lived activation products formed within the machine were determined from the fluxes calculated during the heating calculation. Table D-9 lists the products in the test region. The Na-24 isotope is listed because it is formed from aluminum; the remainder are formed from impurities existing in aluminum. Table D-10 lists the long-life gamma emitters found in the steel shield. Calculations indi-

Table D-9. Long-life gamma-emitting activation products in test region.

Isotope	Half life	Activation (Ci)	
		After 1 year	Equilibrium
Na-24	14.96 hour	2.48×10^6	2.48×10^6
Pb-203	52.1 hour	0.06	0.06
Mo-99	66.7 hour	4.11	4.11
Zr-89	78.4 hour	5.41	5.41
Sc-48	1.83 day	1.68	1.68
Ca-47	4.535 day	0.03	0.03
Nb-92M	10.16 day	0.11	0.11
Nb-95	35 day	0.38	0.38
Fe-59	45.6 day	1.70	1.71
Sr-89	52.7 day	0.03	0.03
Y-91	58.8 day	0.18	0.19
Nb-91M	64 day	0.06	0.07
Zr-95	65.5 day	1.07	1.09
Co-58	71.3 day	10.45	10.76
Sc-46	83.9 day	0.52	0.55
Zn-65	245 day	23.1	35.8
Mn-54	303 day	47.8	84.3
Na-22	2.62 year	0.07	0.32
Co-60	5.263 year	92.7	750.8

Table D-10. Long-life, gamma-emitting activation products in shield

Isotope	Half life	Activation (Ci)	
		After 1 year	Equilibrium
Ni-57	36 hour	4.26×10^4	4.26×10^4
Fe-59	45.6 day	8.65×10^4	8.68×10^4
Co-58	71.3 day	2.62×10^6	2.70×10^6
Co-57	270 day	1.33×10^6	2.18×10^6
Mn-54	303 day	6.30×10^5	1.11×10^6
Co-60	5.263 year	1.51×10^4	1.23×10^5

cate that after one year of operation the lead shield contains approximately 80 curies of 52-hour Pb-203, 2 curies of 245-day Zn-65, and 8 curies of 253-day Ag-110m. The steel container for the magnet contains about 25 curies of 270-day Co-57 and the magnet itself about one curie of 5.2-year Co-60.

Dose rates from the listed activation products were calculated only for the 14.96-hour Na-24 produced in the test region. At a point 630 cm outside the system (point 8 of the previous list), a dose rate of 1.1×10^4 rem/h exists immediately after shutdown. This point can "see" into the reactor.

Activation of the ion sources and the room walls was calculated as if no additional shielding were to be placed around the FERF reactor. (In reality, additional components will be present and some shielding will be present.) The reactor was simulated as a large sphere, and the leakage neutrons were simulated as beam sources on the simulated ion sources and on the two end walls. Figure D-8 shows the location of the reactor and eight detector positions. Point A corresponds to detector 0.8, which is at the centerline of the reactor. The remaining seven detectors are 1 m above the floor of the room.

Table D-11 lists the activation products formed and the dose rate from each product at point A immediately after shutdown. A 1-year run is assumed. Figure D-9 indicates the dose rate at shutdown at each of the detector locations. The dose rate at detector no. 5 is larger than the rest because of closeness of this detector to the lower ion source. Figure D-10 indicates similar rates 30 days after shutdown. In both figures, the dose rate at point A includes an estimate

Table D-11. Room walls and ion source activation.

Isotope	Half life	Activity after 1 year of operation (Ci)			Dose rate at point A (rem/h)
		SS liner	Concrete	Ion sources	
V-53	2.00 min.	6.41×10^2	0	4.62×10^2	1.24
Al-28	2.31 min.	1.57×10^3	1.90×10^5	9.48×10^2	87.5
V-52	3.75 min.	1.75×10^4	0	1.00×10^4	44.3
Ti-51	5.79 min.	4.94×10^1	0	3.58×10^1	0.005
Al-29	6.60 min.	9.74×10^1	9.42×10^3	5.66×10^2	2.40
K-38	7.71 min.	0	3.36×10^1	0	0.012
Fe-53	8.51 min.	2.23×10^2	6.56×10^0	1.59×10^2	0.30
Ca-49	8.80 min.	0	6.72×10^2	0	0.42
Mg-27	9.46 min.	0	1.11×10^4	0	1.37
Co-62	13.9 min.	5.62×10^1	0	3.81×10^1	0.33
Mn-52M	21.1 min.	1.14×10^1	3.70×10^{-1}	8.16×10^0	0.04
K-44	22.0 min.	0	6.78×10^1	0	0.02
Cl-38	37.3 min.	0	6.41×10^1	0	0.02
Cr-49	41.9 min.	6.78×10^1	0	5.17×10^1	0.09
Sc-49	57.5 min.	0	3.27×10^2	0	6×10^{-5}
A-41	1.83 hour	0	2.63×10^2	0	0.06
Ni-65	2.56 hour	5.41×10^2	0	5.34×10^0	0.31
Mn-56	2.58 hour	5.60×10^3	2.24×10^3	2.38×10^2	9.76
Si-31	2.62 hour	1.10×10^1	3.84×10^4	5.34×10^{-1}	0.002
K-42	12.4 hour	0	4.60×10^3	0	2.59
Na-24	15.0 hour	0	1.49×10^5	0	702
K-43	22.4 hour	0	2.28×10^1	0	0.001
Ni-57	36.0 hour	1.06×10^3	0	7.18×10^2	3.57
Ca-47	4.54 day	0	1.65×10^0	0	0.0006
Mn-52	5.60 day	1.76×10^0	5.96×10^{-2}	1.27×10^0	0.008
Fe-59	45.6 day	7.44×10^2	2.13×10^2	4.23×10^1	1.42
Co-58	71.3 day	2.73×10^4	0	1.57×10^4	91.3
Co-57	270 day	3.08×10^6	0	2.18×10^6	0.05
Mn-54	303 day	1.29×10^4	4.56×10^2	8.82×10^3	17.1
Na-22	2.62 year	0	1.59×10^2	0	0.04
Co-60	5.26 year	3.48×10^2	0	2.23×10^2	1.42
Al-26	7.4×10^5 year	0	1.22×10^{-4}	0	4×10^{-8}
K-40	1.26×10^9 year	0	1.95×10^{-4}	0	3×10^{-9}
				Total:	968

Fig. D-8. Location of detectors within reactor room. Location A is at a z-position at the center of the room and therefore "sees" into the machine. Locations 1 through 7 are 1 m above the floor.

Fig. D-10. Dose rates in rem/h 30 days following shutdown following one year of reactor operation.

Fig. D-9. Dose rates in rem/h immediately after shutdown following one year of reactor operation.

of the contribution from inside the reactor. Figure D-11 illustrates the time dependence of the dose rate at point one.

CONCLUSIONS

The shield that has been analyzed in this report is an "ideal" shield; that is, it exists only on paper and does not include all the structural components that will be in a "real" shield. In general, the real shield will not be as good as the one that is analyzed. The drawings for FERF can be used to illustrate this

Fig. D-11. Dose rate or time in days at detector position no. 1 after shutdown following one year of reactor operation.

point. For example, the need to disassemble the reactor for repair or replacement of the first wall has led to a design in which the magnets can be pulled apart. The joint in the shield that is therefore necessary now has a gap through which neutrons may stream directly into the magnet. The heating from these neutrons may be as much as that from the neutrons which must pass through the shield to get to the magnet. It is understood, of course, that this joint would never be made as illustrated; other design items are adopted which will greatly increase the effectiveness of the shield.

On the other hand, very little work has been done to make this an optimum shield from the calculational standpoint alone. Many man-years of work and many machine hours of calculation will undoubtedly

enable the construction of a much better shield than that described in this report. It therefore seems entirely possible within the existing state-of-the-art to construct a shield for the present design which will hold the heating of the magnet and its case to 5 KW.

In arriving at the present design, four different shield designs were calculated. These are illustrated, drawn to the same scale, in Fig. D-12. (The first used a Baseball magnet.) After approximate normalization to the same quantity, and with heating in a steel

magnet liner included, the relative heating in the magnet for each design is included on the figure. Only one of the designs has a higher heating than the present design.

The high dose rates in the reactor room after operation may be controlled by appropriate design efforts. However, the calculated activities will exist somewhere in the system, and it is necessary to determine where by making a design judgment as to the philosophy desired in containing the radiation.

Fig. D-12. Shield designs, showing relative magnet heating (all drawn to same scale).

REFERENCES

- D1. The MORSE Code—A Multigroup Neutron and Gamma Ray Monte Carlo Transport Code, ORNL Rept. 4585 (1970).
- D2. W. W. Engle, Jr., A Users Manual for ANISN, Union Carbide Corporation, Rept. K-1963 (1967).
- D3. R. J. Howerton, et al., An Integrated System for Production of Neutronics and Photonics Calculational Constants — Volume 4; Evaluated Nuclear Cross Section Library, Lawrence Livermore Laboratory, Rept. UCRL-50400, (1971).
- D4. E. F. Plechaty, Photon Cross Sections, 1 KeV to 100 MeV, Lawrence Livermore Laboratory, Rept. UCRL-50400, Volume 6, (1968).
- D5. C. Wong, et al., Livermore Pulsed Sphere Program: Program Summary through July 1971, Lawrence Livermore Laboratory, Rept. UCRL-51144, Rev. 1 (1972).
- D6. W. E. Alley and R. M. Lessler, Semiempirical Neutron — Induced Reaction Cross Sections, Lawrence Livermore Laboratory, Rept. UCRL-50484, Rev. 1, (1972).
- D7. C. M. Lederer, J. M. Hollander, and I. Perlman, Table of Isotopes — Sixth Edition, (John Wiley & Sons, New York, 1967).

Appendix E - Remote Assembly / Disassembly Procedures*

OBJECTIVES

The procedures for remote assembly and disassembly of the FERG reactor assembly and associated equipment were formulated with the following objectives in mind:

- Completely remote operation (i.e., no personnel in the vault);
- All devices to operate in a fail-safe manner;
- Operations to be automated if possible;
- Redundant backup systems to be provided where feasible; and
- Lifting by single-point suspension to be avoided if possible.

GENERAL REQUIREMENTS FOR HANDLING SYSTEMS

The same equipment can usually be used for both assembly and disassembly. The handling systems fall into two general categories: primarily fixed or rigid systems; and portable or flexible systems with universal application. The fixed systems consist of supporting dollies permanently attached to the large reactor subassemblies which operate on tracks imbedded in the floor of the vault. Subassemblies such as the expander tanks, first- and second-wall assemblies, and half tanks are mounted on dollies which support the entire reactor assembly.

Reactor system components that either cannot be supported by permanent dollies or that cannot be mounted by pure translational movement can be handled by a combination of lifting devices, portable dollies, and special-purpose temporary fixtures. Such components include the superconducting magnet coils, C-clamps for the magnetic coils, and some of the injector clusters and plasma collector plate units. The positioning and clamping of the mating surfaces of individual components prior to pumpdown can be similar for all reactor subassemblies.

The initial concept drawings of the reactor vault indicate a vault approximately 60 feet high and 55 feet wide. A slot or trench down the centerline of the floor of the vault reduces the height of the assembled reactor above the floor by permitting projecting portions of the half tank and one expander tank to extend below the floor level. The trench separating the major subassemblies permits access to the inner components. However, if it is found that the trench inconveniently limits the distance and direction that the major subassemblies can be moved away, the problem can be avoided by mounting the entire reactor structure above the floor level. Two sets of parallel tracks along each wall of the vault and appropriate switching mechanisms will permit the components from the inner core to be placed on dollies which could then be shunted around the subassemblies that had been moved away. Mounting the entire assembly above the floor will simplify mounting and dismounting the components beneath the reactor structure.

All operations in the assembly and disassembly will be monitored and conducted from a central control room outside the vault. About 10 fixed and portable remotely operated TV monitors equipped with zoom lenses will permit viewing of all areas within the vault. The control room is included within a section of the reactor control room. A console within the control room contains all of the required system operating controls, TV monitoring screens, and instrumentation readouts.

Because the atmosphere within the reactor vault is nitrogen, all equipment and instrumentation within the vault must operate in nitrogen. Personnel entering for any reason must use a self-contained breathing apparatus.

TRANSLATIONAL MOVEMENT USING INTEGRAL DOLLIES

Dollies will be integral parts of the major reactor subassemblies. Major structural components (e.g., the half tanks, expander tanks, and first- and second-wall assemblies) can then be assembled and disassembled by pure translational movement back and forth along a set of tracks. The driving mechanisms for the

*Acknowledgement is given to Miles G. Patrick and Scott Kelley of Battelle-Pacific Northwest Laboratories who assisted in preparation of this Appendix.

dollies can be a redundant set of electrical induction motors driving one or more wheels of each dolly. The motors can brake automatically when activated by a set of limit switches on each end of the travel. For example, during assembly the units can be automatically stopped about 1 foot from the final position, and the motors can then be manually jogged to achieve final position. Power to the motors can be provided by either suspended or trailing electrical cables mounted on constant-tension reels to take up slack. Alternately, those dollies supporting the major subassemblies which move back and forth in a straight line can obtain power from an energized insulated strip of wire (similar to the overhead wires used for electric cars) mounted between the tracks.

The positioning of the dollies during final assembly must be exact to permit accurate connection of ancillary equipment such as coolant piping and vacuum exhaust ducts. All dollies will therefore need an electrically activated mechanical brake and a set of positioning stops on the tracks (to maintain position during assembly). A backup, optical-alignment system would verify the correct position.

Assembly of the large and heavy components of the inner core (e.g., the magnetic coils and C-clamps) will be greatly facilitated by splitting the half-tank assemblies in a horizontal plane. The bottom half of the half-tank assembly can be locked in final position and the coils, C-clamps, and portions of the first-wall assembly set into the half tank. The extreme weight of the C-clamps can be accommodated during assembly by separating each clamp into at least four sections and fastening the sections together with nuts and bolts. The nuts can be captive on the assembly and installed by remotely operated impact wrenches suspended from the crane or mounted on remotely controlled dollies.

The configuration of the slot between the superconducting magnet coils precludes withdrawing the first-wall assembly through the assembled coil by sliding it away from the center of the reactor; therefore, a parting seam is required between the coils on the first-wall assembly.

Because of the composite weight of the reactor components, a single set of tracks may not be adequate for support and movement of the dollies. This can be overcome by nesting the dollies and using two

sets of different-gage tracks. Then the dollies supporting the half tanks would ride on both sets of rails, and the dollies under the first- and second-wall assemblies and the expander tanks would be mounted only on the outer tracks.

USE OF AN OVERHEAD CRANE

A travelling overhead crane with lifting capacity of approximately 60 tons will handle the reactor components during assembly and disassembly, providing the extremely heavy C-clamps are divided. During disassembly, inner components such as the C-clamps and magnetic coils would be stored on the floor in jigs which provided storage in the correct position for assembly. Lifting lugs would be permanent where possible, where not, removable pad eyes would be screwed into the units.

A portable machine with two manipulator arms could perform some operations in the assembly/disassembly procedure such as installation or removal of components and ancillary equipment located beneath horizontal projections of the reactor structural subassemblies and therefore not accessible to the hook of the crane. Considerable design effort should be spent on methods to preclude the use of such a machine because, first, it would be very expensive and complex, and second, operation of remotely operated portable manipulators cannot normally be made fail-safe.

Smaller components and ancillary equipment such as vacuum lines, cooling water lines, sample exposure tubes, plasma collector plate, and injector clusters can be mounted and dismounted on an individual basis using a combination of portable dollies, the overhead crane, and temporary jigs and fixtures. Assembly/disassembly will be greatly simplified if movement is restricted to a single translational motion or to pivoting around a single axis.

If the support assembly for the injector clusters on the vertical half tank is split in a horizontal plane, the injector clusters can be simply lowered or raised for assembly onto the top and bottom of the vertical half tank. The injector clusters mounting into the horizontal half tank are attached to dollies that ride on tracks parallel to the centerline of the injector cluster unit. Assembly/disassembly then requires only pure translational movement to and from the reactor structure.

The plasma collector plate units are mounted onto the horizontal expander tank by translational movement. However, the plasma collector plate units which are mounted on the vertical expander tank cannot readily be assembled or disassembled by translational movement. The lower plasma collector plate unit will require a dolly with an elevatable platform. Elevation would be along an axis perpendicular to the plane of the mating surfaces rather than vertical to the floor. The upper plasma collector plate on the vertical expander tank will be mounted and dismounted using a combination horizontal and vertical movement with the overhead crane.

The sample tubes and piping for the cooling water will be connected and disconnected using a sliding manifold that simultaneously locks and seals all tubes. The sample tubes will be difficult to seal if cooling is required because the tubes may then become annuli and require a double seal. The manifolds will be moved by electrically driven lead screws which are attached either to a rigid structure such as the reactor vault floor or to portions of the reactor structure.

The vacuum exhaust ducts and other attached ancillary hardware will be assembled and disassembled either by attaching them to electrically powered pivot arms that swing the ducts out of the way or (possibly) by a ram that moves the ducts back and forth in translational movement. The flanged joints will be slightly beveled from the horizontal (see Fig. E-1) so that the assembled units will be wedged tightly after assembly.

An alternative assembly/disassembly method for the coolant piping, sample exposure tubes, and exhaust ducts would be to provide a telescoping section. This section would have to retract enough to permit clearance between joints and also clearance to move other components past the remaining piping.

REMOTE POSITIONING AND CLAMPING

The joints formed by mating surfaces of the major reactor subassemblies to smaller system components such as injector clusters and plasma collector plate units require an initial alignment during each step in the assembly followed by a simultaneous clamping pressure on all joints of the assembled reactor prior to pumpdown. The system cannot be evacuated unless

Fig. E-1. Schematic of tapered flanges.

the assembly is complete. Thus, the joints must be clamped together while reactor subassemblies are being assembled or disassembled, particularly if gravity would cause the component would fall off the reactor when the reactor core is not evacuated.

All mating surfaces forming joints between subassemblies or components are assumed to be planar. It is further assumed that metallic O-ring seals will be used at each joint interface and the mating surfaces will, therefore, consist essentially of a pair of flanges. The flanges must be wide enough to permit an adequate radius in the metallic O-ring at all corners of rectangular cross sections. A 3-inch flange width is the minimum acceptable width to provide room for an adequate (4-inch-minimum) radius.

The alignment between all mating surfaces will be established by a set of tapered guide pins (Fig. E-2) on the flange of the subassembly on the component being moved. The pins will enter tapered holes on the flange of the subassembly to which the moving component is being fitted. The size and number of guide pins required per joint will depend on the weight of the component being moved into position and on any existing pre-alignment. For example, the joint between half tanks will require only two alignment guide pins on opposing flanges because both subassemblies will be mounted on dollies which serve to prealign the joint, and assembly requires only pure translational movement of one

Fig. E-2. Schematic of guide pins.

or both of the units. Positioning of the injector clusters and plasma collector plate units, however, will require three or more guide pins because of little or no prealignment capability and because the mating will require movement in two planes.

To permit initial engagement, one of the guide pins on components moved in two places will be longer than the remaining pins. The assembly will then be jogged to facilitate engagement of the remaining pins. The jogging will be done by suspending each component from a bridle and lifting one of them with the overhead crane. If components require precise axial alignment during assembly, a second set of smaller guide pins and tapered holes could be provided that will engage after the mating surfaces are almost in contact.

The devices for clamping and holding the joints together are shown in Fig. E-3. The clamping devices will be used on all mating surfaces or joints which have a rectangular cross section. Joints with a circular cross section (such as the pipes from the roughing pumps) will be clamped with a split-ring, wedge-type clamp (Fig. E-4) which is actuated by rotating lead screws on the top and bottom. All clamping devices

Fig. E-3. Clamping device.

Fig. E-4. Split-ring wedge clamp.

will be designed fail-safe in that loss of power will result in the clamp remaining in the closed position.

The clamping devices for flanges which have rectangular cross sections will be spaced as required along each edge. The maximum distance between clamps will be about 2 to 3 feet. The clamps will be designed to be strong enough to hold the component in place on the reactor structure against all forces (e.g., gravity) tending to break the joint. Upon evacuation, all components and subassemblies will be held together by the external forces induced by the reduced pressure.

The clamps are operated by a two-way electrical solenoid. Because a spring will keep the plunger extended when the power is off, the solenoids will not normally be energized to keep the plunger extended. The concept of clamping forces provided by a wheel moving up a ramp (see Fig. E-3) provides two advant-

ages: it reduces the forces required for engagement (rolling versus sliding friction), and it permits ready visual checking to ascertain that the clamp is secure. The clamping procedure would be complete only after the wheel had climbed up the ramp and traveled 1 inch or more across a flat area behind the ramp.

The electrically powered solenoids to power the clamps may hamper the assembly and disassembly operations because the electrical leads will be dragged as the units are rolled or swung back and forth. If the leads from the solenoids do cause trouble, all of the solenoids at a joint could be energized by a simple electrical lead remotely connected to the joint after the unit is in place. However, at this time the series of cables dragging across the floor is preferable. Constant tension reels will be provided to take up slack in each cable.

Appendix F - Safety Analysis

PHILOSOPHY

A major goal of every facility engineering design effort is to assure that any potential hazards to personnel working in the facility, to personnel around the facility, and to the general public are reduced to the lowest practicable level. To this end, facility design must provide protection under both operational and potential accident conditions so that exposures will be kept well within applicable federal, state, and regional regulations and guidelines.

It is the intent of this section

- To identify the most significant potential safety problem areas associated with FERF, and
- To discuss them in sufficient detail to determine which can be dealt with satisfactorily using normal good safety practices and which will require special design features and technology.

SOURCE OF HAZARDS

The primary safety problems associated with FERF come from the specific operational requirements of a pilot fusion reactor. These may be summarized as follows:

- The use of large quantities of deuterium and tritium as the fuel source for the fusion reaction.
- The fast-neutron flux associated with the DT burn. The flux results in high radiation levels during operation and in the production of large amounts of beta and gamma-emitting activation products.
- The use of large amounts of electrical energy to provide the necessary electromagnetic fields for plasma control.
- The large heat loads that result from the developed electromagnetic fields and the DT burn.

TRITIUM USE

Inventory

As indicated in Appendix G of this report, about 400 g (66.6 mol) of tritium gas and about 800 g (200 mol) of deuterium gas will be employed in the operation of the FERF facility. This represents the maximum amount of material that will be present in

the facility at any one time. More significant, however, is the distribution of the material during operations. At the end of each cycle, the total tritium in the four injector modules is estimated to be about 60 g. The cryogenic distillation columns which are part of the isotopic separation process are expected to contain another 270 g. The remaining tritium will be distributed throughout the recycling system, with a portion kept in storage as reserve supply.

Note that during recycling of the tritium inventory, the tritium pressure will be less than ambient and, in fact, the system will generally be under a hard vacuum. Thus, potential leaks will be kept to a minimum.

Another tritium source is the borated water planned for use in the cooling water for the magnet shield. Calculations indicate that about 140 Ci of tritium as T_2O will be produced each year as a result of neutron interaction with boron-10. Provisions must be made for periodic replacement of this water and its subsequent disposal.

Toxicity

The toxicity of tritium is rather well known and has been documented in a number of publications. Tritium is considered a moderately toxic radioisotope and has been assigned recommended concentration guides (RCG) for personnel exposure (see Table F-1). If continuously present, these concentrations will result in exposures to an individual of approximately 5 rem/year for the controlled area (40 h/week) and 0.5 rem/year for the uncontrolled area (168 h/week).

Table F-1. Recommended concentration guides for personnel exposure.

Isotope	Controlled area ($\mu\text{Ci}/\text{m}^3$)	Uncontrolled area ($\mu\text{Ci}/\text{m}^3$)
^3H (HTO)	5×10^{-6}	2×10^{-7}
H (HT)	2×10^{-3}	4×10^{-5}

To place these numbers in perspective, however, one must factor in the rather large amounts of tritium involved in the FERF facility. For example,

exposure of an individual to a concentration of $10 \mu\text{Ci}/\text{m}^3$ of tritium as HTO for a period of 10 minutes will result in a whole-body dose of 480 rem. (If the tritium form were HT the exposure would be 300 mrem.) Assuming static condition in a room such as the reactor vault which is $50 \times 150 \times 60$ feet (about $450,000 \text{ ft}^3$), this concentration would be attained through the release of 15 g of tritium as HTO.

To achieve the RCG value of $5 \times 10^{-6} \mu\text{Ci}/\text{m}^3$ in the same room, again assuming static conditions, a release of only $7.5 \times 10^{-2} \mu\text{Ci}$ or about $7.5 \times 10^{-6} \text{ g}$ of tritium would be required; obviously, in either case, this is a small fraction of the total tritium available. Thus, to achieve the required measure of control, the tritium containment system must be designed as leak-proof as possible. Much of the required basic tritium containment technology exists today; however, where in use, it is on a small scale and mainly at several AEC facilities (including LLL, LASL, and the Savannah River Project).

Special provisions must also be made in the design and operation of FERF to minimize personnel exposures to tritium during disassembly and maintenance operations. This will be especially true for any surfaces that may contain tritium due to permeation and that may outgas during exposure to the air. Special precautions that may be employed include pressurized suits and local close-capture hoods or enclosures. Plastic suits are now available that reduce exposure levels by 10^5 .

Environmental Effects

Accidental release of gram quantities of tritium to the environment is not considered credible because the proposed design provides for at least double containment of the tritium system. Since the integrity of the containment system is essential to minimum release of tritium, the quality assurance program developed for the project must insure continuous operation of the containment system throughout all postulated design-basis accidents.

It is certain, however, that small amounts of tritium will be released routinely and continuously from the facility due to permeation of the metal wall and transfers of equipment and personnel through air locks. Maximum probable losses for these potential release points are listed in Table F-2.

Table F-2. Maximum probable routine tritium losses.

Release point	Loss (Ci/year)
Permeation through metal wall:	20
Three small air locks, assuming 10 trips/day:	900
Two large air locks, assuming 2 trips/year:	1100
Total:	2020 (or ~2000)

If it is assumed that these releases occur at LLL and the appropriate diffusion parameters for average annual meteorological conditions are applied, the total man-rem dose to the population within a 50-mile radius would be less than 100 man-rem per year. Since the total population affected is 4.6 million, the average dose would be about $2 \times 10^{-2} \text{ mrem}/\text{man-year}$.

The potential impact of an accidental release of tritium was assessed by assuming loss of 1000 Ci of tritium as HTO over a 24-hour period. The dose due to inhalation skin absorption to an individual at the site boundary was found to be less than 0.3 mrem, well within the guidance of Reference F1. This excludes the possibility of local rainfall.

RADIATION LEVELS

During Operation

During normal operation of the facility, the DT reaction in the plasma is expected to yield about $3 \times 10^{18} \text{ n/s}$. This will require the use of rather large amounts of shielding to provide not only personnel protection but also radiation-damage protection for the magnetic and other critical parts of the fusion reactor. In the current design, the magnet shield consists of steel with borated water for cooling; however, engineering and physics requirements may call for other materials. Concrete is used for the biological shield. Radiation levels during operation of FERF will be reduced to acceptable levels through the appropriate use of shielding. The technology of calculating and employing suitable shielding is well understood.

After Shutdown

The primary radiation problems associated with FERF will occur as a result of neutron activation of materials used for shielding and containment. Activation of other parts of the system (such as corrosion products in the coolant water) was considered and found to be insignificant. Tritium produced in the borated water will not add to the external radiation levels.

Probable radiation levels in the area surrounding the plasma containment vessel following shutdown of the plasma were calculated assuming the materials of construction include stainless steel and other metals commonly used for containment. The dose rate immediately following shutdown was found to be approximately 1000 R/h at 600 cm from the center of the reactor vessel.

Any maintenance operations or modifications that must be performed on the magnet and the containment vessel will require the use of extensive personnel shielding and/or remote handling equipment. Allowing time for cooling following shutdown will help little due to the long half lives of the activation products. The technology developed at reactors, fuel processing plants, and isotope laboratories can be applied directly.

ACCIDENT ANALYSIS

Accidents affecting the facility operation may be divided into two categories: those caused by acts of nature and peculiar to the site location (e.g., earthquakes, tornadoes, floods, lightning), and those usually caused by the human element (e.g., operational error or equipment failure due to improper design or maintenance). The facility must be designed so that, if any one of the accidents considered credible for the facility should occur, those features and systems essential to safe shutdown will continue to function and no loss of containment will occur.

The principal accidents that have been considered in the design of this facility are discussed briefly below.

Collapse of the Vacuum Vessel

Collapse of the inner vacuum vessel that contains the plasma is considered highly improbable because

the void surrounding the vessel is maintained at a pressure only slightly above the 10^{-6} Torr. Even if first-wall failure should occur, a second shell thick enough to contain the pressure vessel surrounds the inner vessel.

First-Wall Failure

The first wall is cooled by high-pressure, fast-flowing cold water carried in thin-walled tubes. These first-wall tubes will receive massive neutron doses and high heat loads. They must withstand severe sputtering, several kinds of radiation damage, embrittlement, and thermal stresses. A tube might fail, although this seems highly unlikely because two layers of essentially parallel coolant lines are provided. As a consequence, the plasma would immediately go out and the plasma vacuum chamber would partially fill with water. The water in the coolant loop would therefore become tritium-contaminated. The coolant water will not run directly to a cooling tower but will first go through a water-water heat exchanger which would be located within the biological tritium shield. Thus, should the coolant water become contaminated, it can be removed and disposed of safely.

The same considerations apply to the water that would leak into the main chamber. The volume of water would be less, but its tritium content would be higher. This water might be drained from the vessel, pumped directly onto molecular sieves, or evaporated and trapped by the main tritium recovery system.

Any local cleanup would be extremely difficult due to the high radiation levels caused by induced radioactivity. Special suits designed to reduce tritium exposure would be used.

Chemical Explosion of the Fuel

Deuterium and tritium are used in sufficient quantities to constitute an explosion hazard should the gases leak and become mixed with air. This potential problem has been eliminated by replacing the atmosphere surrounding all deuterium-tritium bearing lines and vessels with an inert gas such as nitrogen.

Magnet Shield Failure

Present plans call for borated water to be used in

the magnet shield to provide neutron shielding and cooling. As indicated earlier, the water will become contaminated with tritium due to neutron interaction with the boron. Any leakage of this liquid will result in the same problems indicated for first-wall failure.

Vacuum System Failure Outside the Vacuum Vessel

Vacuum system failure outside the vacuum vessel would result in the leakage of nitrogen into the vacuum lines. Some tritium would leak out into the main room, but this could be cleaned up with the main deuterium-tritium recovery system. Line repair and cleanup would again require the use of protective suits.

Tritium Distillation System Failure

The distillation columns are the only components of the entire fuel-handling system that run above atmospheric pressure. They receive no neutron radiation damage, and the chance of their failing is extremely remote. There is a possibility of valve failure, but this too is considered remote due to the quality assurance program that will be applied to this system.

The still and other cryogenic components will be protected by a vacuum jacket and therefore are also doubly contained. Loss of vacuum in the jacket could result from nitrogen leaking in from the outside or from DT leaking in from the column. Although loss of vacuum in the jacket will automatically transfer operation to the other still, it will not necessarily result in a tritium release. Nitrogen leaking in from the outside would be of little consequence other than requiring some repair work. It would result in rapid boiling of the entire still inventory. A pressure surge could result which would either be contained by the inherent strength of the system or, more likely, be relieved by rupturing an all-welded rupture disk which would abort the system to a large reservoir. Filling the vacuum jacket with DT would cause loss of vacuum with the same consequences. The equipment would require disassembly for repair, but it could probably be done with relative ease. If tritium did escape, it would be into the nitrogen atmosphere being processed by the main recovery system. Potential problems with the tritium distillation system can be controlled using existing technology.

Magnet Failure

Super-conducting magnets usually operate during charging and discharging at very low voltages, in the order of 2-5 V. However, should the magnet lose its superconductivity due to rise in temperature or other problems, it has been designed to carry the load safely. Since loss of superconductivity could cause conversion of the liquid helium to the gaseous state, the helium lines must be protected with overpressure relief valves. The basic design of superconducting magnets, plus special safety features included in the design of the FERF magnet, eliminate melt-down as a credible accident.

APPLICABLE REGULATIONS

Safety Analysis

The design and construction of a facility such as FERF is subject to critical review by a variety of state and federal agencies. A key point, yet to be settled, is whether FERF is to be treated on the same basis as a fission reactor. Based on recent AEC policy with respect to reactor fuel reprocessing and plutonium handling facilities, it seems likely that most of the regulations written for power reactors will be applied to FERF. Given that this is true, the principal regulations to which FERF will be subject are contained in Title 10 of the Code of Federal Regulations^{F1}, especially Part 20 - "Standards for protection against radiation," Part 50 - "Licensing of production and utilization facilities," and Part 100 - "Reactor site criteria," plus appropriate appendices. If the project is developed by an AEC contractor, then the design and operation become subject to the various AEC Manual chapters.

AEC Manual, Appendix 6101, Annex C, dated April 8, 1971,^{F2} requires the preparation of both preliminary and final safety analysis reports as part of the design of any major facility that has the potential for significant environmental impact. Contents of the documents may vary somewhat depending on the specific characteristics of the proposed facility, but in general they must follow the format recommended in "Standard Format and Content of Safety Analysis Reports for Nuclear Power Plants" published by the Regulatory Staff in October 1972. Requirements for safety analysis reports are also indicated in Section

50.34 of Reference F1.

Principal features of safety analysis reports include a detailed description of the proposed site, a presentation and discussion of "design basis accidents," an identification of those structures, systems, and components essential to the safe operation of the facility, and a detailed presentation of the measures that will be taken to assure that those systems will continue to function properly through the postulated design basis accidents.

ENVIRONMENTAL ASSESSMENT

AEC IAD 0510-29^{F3} requires environmental assessments for all proposed line items, major general plant projects, major equipment, and other major activities. The assessment must include the following sections:

- Description of the proposed action and its anticipated benefits.
- Potential environmental impact.
- Alternatives and their environmental impact.
- Cumulative and long-term effects of the proposed action.
- Irreversible and irretrievable commitments of resources.
- Potential conflict with federal, state, or local governments.
- Cost-benefit analysis.

On the basis of this seven-point document, the AEC, Division of Operational Safety, determines the possible

need for an environmental impact statement as required by the National Environmental Policy Act of 1969. It seems likely that such a document must be prepared for this facility and reviewed prior to construction.

CONCLUSIONS

The FERF described in this report will be designed to ensure the safety of operating personnel and the general public at all times during normal operations and during possible accident conditions. The several possible accidents briefly discussed in this Appendix represent those considered to be of greatest concern in the design and operation of the facility. As indicated, the most significant safety challenges will be presented by problems associated with containment of the tritium and the high radiation levels due to neutron activation. Both of these areas, however, can be successfully treated with the current level of technology employed by several of the AEC contractors.

REFERENCES

- F1. Code of Federal Regulations, Title 10, Atomic Energy, GSA (Rev. January 1, 1973).
- F2. AEC Manual, Appendix 6101, Annex C, AEC (April 8, 1971).
- F3. AEC Manual, Immediate Action Directive, AEC, IAD No. 0510-32 (November 13, 1973).

Appendix G - Tritium

INTRODUCTION

The FERF would be the first high-power, continuously operating reactor in the world. Since tritium will be used, a major class of tritium handling and safety problems must be solved.

Most of the topics which will be discussed in this Appendix fall logically into one of two categories: fuel recycling and fuel confinement. The former includes all the operations and processes which must be performed to maintain the fueling of FERF in a reliable, steady-state condition for periods of several months. The latter, confinement, includes all the measures necessary to assure that no one inside or outside the facility will ever, even in an accident situation, be exposed to radiation dosages of unacceptably high levels.

FUEL CYCLE

General

A very schematic diagram of the fueling system is shown in Fig. G-1. Note that almost the entire gas-handling system will be operating below atmospheric pressure. In fact, much of the equipment will run under conditions usually described as hard vacuum. Therefore, there is little reason to believe that a leak anywhere in the system would automatically cause a major release of tritium. This will be true even though there is a relatively large tritium inventory (400 g) in the facility. (Leakage in air poses a separate set of problems.)

Vacuum Pumping the FERF Reactor

The FERF reactor is pumped in two principal regions: in the injector modules and in the tank receiving the mirror end losses. Because of the high gas loads and space limitations, the most demanding requirements are found in the injector region. From the standpoint of minimizing the tritium inventory, we would prefer to use mercury diffusion pumps; however, because of the spatial restraints, cryopumps must be used in the injector modules. At any given moment, three of the injector modules will be operat-

Fig. G-1. Fuel system flow diagram.

ing and one will be undergoing regeneration of its cryopump. The planned cycle time for each injector module with its respective pump is about 30 minutes. Under these operating conditions, the injector-cryopump modules will have a combined tritium inventory of about 60 g at the end of each cycle.

The possibility of using one injector module for tritium and the other three for deuterium was considered. (To optimize plasma fueling, the deuterium and tritium are injected at different energies.) If in each injector module one could use an array of small cryopumps which would be sequentially isolated from the machine and regenerated, then the total tritium inventory could drop by a factor of three.

Only part of this inventory reduction would result from a reduced quantity in the cryopumps; the rest would come about because a smaller inventory could be maintained in the isotopic separation plant. By keeping the two isotopes separate during pumping operations, the unused gas could be directly recycled to the injectors without first passing through the isotopic separation equipment. However, as with the diffusion pumps this option became unavailable because of the demands for unusually high pumping speed. A large number of smaller pumps could not be accommodated in the space available.

In the region where the mirror end losses are removed, diffusion pumps might be used: the choice will be on the basis of convenience rather than tritium inventory. Because the gas loads are so much lower in this region, the tritium inventory associated with cryopumps can be small compared to that in the injector region.

Palladium Diffusers

Several impurities may accumulate in the fuel. The most obvious one is helium, which will be produced by tritium radioactive decay ($\sim 2 \times 10^{-7}$ g-mol/s) and by DT burn in the plasma ($\sim 2 \times 10^{-6}$ g-mol/s). In addition to helium, during startup there will be some residual air, small vacuum leaks in the system will admit air (or nitrogen), and surfaces will give off water both by normal outgassing and by hydrogen reduction of oxide films. However, simple calculations show that the bulk of the impurity should be helium.

All impurities must be removed from the fuel. Helium must be removed to minimize blistering of metal surfaces which would otherwise be caused by implantation of energetic alpha particles. Oxygen and nitrogen, if allowed to accumulate, would eventually freeze out and cause plugging in the cryogenic distillation apparatus. Also, frozen oxygen in liquid DT could be hazardous. Therefore, all the fuel leaving the FERF reactor for recycling is permeated through hot (400°C) palladium-silver membranes before going to the distillation columns.

The physical arrangement for removing impurities from the fuel has already been developed here, both experimentally and theoretically.^{G1} Computer codes exist which accurately predict observed performance.

The FERF purifier uses seven 6.35-mm-diameter X 0.127-mm-wall palladium-silver alloy tubes in parallel hookup. The impure gas at 69 kPa (520 Torr) flows on the inside of the tube; a reduced pressure, 1.32 kPa (10 Torr), is maintained on the outside so that permeation can occur. After flowing down 7.62 m of tubing, 90% of the DT has been permeated. The remainder of the gas inside the tube is combined by manifolding and passes through another 7.62-m-long, 6.35-mm-diameter tube with the same operating conditions. The gas is then routed through a final 7.62-m-long tube which is only 1.27 mm in diameter, but here the pressure outside the tube is held at 0.132 Pa (1 μ m) rather than at 1.32 kPa. (The pumping capacity required at 0.132 Pa is only 1 l/s.) The gas exiting from the end of this final tube is 99.9998% helium and other nonpermeable gases.^{G2} The helium can be collected and disposed of as low-level radioactive waste. Redundancy is provided so that if a single, small component should fail the entire facility needn't be shut down. A schematic diagram of the separation system is shown in Fig. G-2, with the calculated flow rates which correspond to a nominal injector gas flow equivalent to 3000 A of diatomic molecules and a nominal fusion power of 5 MW. The final reactor design has a fusion power of 3.38 MW.

Isotopic Separation of Deuterium and Tritium

After leaving the palladium diffusers, the purified fuel is compressed to 186 kPa (1400 Torr) and sent to the cryogenic distillation columns. This compression may be done by cryopumping so that no impurities are reintroduced to the fuel. One might also consider using a gallium-filled Sprengle pump which would reduce the tritium inventory and, because of gallium's very low vapor pressure, maintain clean gas.

During the initial design study for distillation equipment, we thought isotopic separation could be maintained for the gas being pumped in the injector region. Therefore, the design was based upon a gas load equal to that which entered the plasma region either by injection or streaming. The gas entering the column was expected to consist of the equilibrium composition at high temperature, namely, 0.64 D₂ and 0.04 T₂. The actual gas load is 12 times larger than that, and the

Fig. G-2. Fuel purification system.

expected composition of the gas also has changed slightly.

The original design was based on a rather complete parametric study of the multistage separation process in a packed column, with a goal of minimizing the tritium inventory. The required distillate and raffinate streams must be at least 90% isotopically pure for proper functioning of the fuel injector system. For the reasons given above, the principal results of the original design study were scaled by a factor of 12 and are given in Fig. G-3. The column was 25.4-mm in diameter and the original tritium inventory was estimated to be 18 g. Refrigeration requirements for the upper column were 40 W at 25 K. After scaling to the new requirements, these values

became 270 g tritium and 500 W. It is possible that an injection system can be designed for combined D and T operation, thus greatly reducing the size of the columns and the tritium inventory. The new column has a 90-mm internal diameter and is 1.5-m tall. Note that a catalytic isotope equilibrator is used on part of the feed to the lower column. This is necessary because the deuterium and tritium present as DT molecules cannot be separated by distillation alone. Two identical distillation systems will be supplied to provide redundant capacity in the event of equipment malfunction.

Immediately after isotopic separation, the fuel will be returned to the injectors before significant helium-3 can accumulate by radioactive decay.

Fig. G-3. Fuel isotopic separation system.

Fuel Storage

Although not necessarily a part of the fuel system proper, it is clear that provision must be made for safe fuel storage. There should also be a reserve supply to act as a ballast in the event of transient perturbation in the fuel-handling system. Storage capacity will be provided for the entire facility inventory so that all radioactive gases can be held in a safe configuration during periods of maintenance. If we assume a 400-g (66.6-mol) inventory of tritium, and three times that many moles of deuterium, the storage volume needed is about 4500 l at standard conditions. This will be easily provided by tanks isolated from the rest of the facility in a sealed vault so that a large accidental release would not be possible.

TRITIUM IN MAGNET SHIELDS

Shielding requirements to protect the magnets from radiation are covered in Appendix D. It appears highly desirable to include within the shields a substantial mass of boron because of its high neutron capture cross section and because no gamma rays are produced. However, although the shields are highly effective in capturing neutrons, they do so in part by reactions which produce tritium. These reactions are

The boron may be present either as an aqueous solution of boric acid or as a boron-rich solid material. To date, neutron calculations of shielding performance have been based on aqueous solutions. In what follows, we assume a solution will be used and that it will also function as a heat transfer fluid to remove the heat produced (4 MW) by the neutrons.

For 5 MW of fusion power, it is estimated from calculations that 142 Ci/yr will be produced in the borated water.^{G4} This relatively low level of tritium production can be maintained by regularly removing the lithium produced in the second reaction listed above. Lithium is removed by regular replacement of the solution with fresh. It must be borne in mind that tritiated water is several hundred times more toxic radiologically than tritium gas.^{G5} Provision will have to be made for changing the water and disposing of the contaminated solution.

By withdrawing contaminated solution during regularly scheduled maintenance periods and replacing it with fresh solution, the activity level can be kept to levels that are well within the range where present technology and work practices are sufficient to cope with them. A nearly identical situation prevails in heavy-water-moderated nuclear reactors such as the NRX and NRU research reactors at the Chalk River Nuclear Laboratories where the steady-state tritium activity runs from 10 to 50 Ci/l.^{G6} With properly trained personnel, good equipment, and well-planned procedures, these operations can be performed safely. The waste liquid can be disposed of either by injecting it into deep wells or by storing it for a long time under surveillance.^{G7,G8}

CONTAINMENT OF TRITIUM

General

Tritium containment is difficult. We must protect the people working within the facility at all times, and we must be able to guarantee with a high degree of certainty that people off-site will never be exposed to more radiation than is acceptable. The standards to be applied to FERF are not known, but it seems reasonable to assume that present licensing requirements for commercial power reactors might be appropriate. No one living on the site boundary would then be permitted a total additional radiation dose

exceeding 5 mrem per year.

Tritium can escape either into the air or into the cooling water, but the former release mode is the more difficult to control. In all present tritium-handling facilities in the United States, the conventional practice is to supply large quantities of fresh-air ventilation on a once-through basis, exhausting the effluent air out a tall stack. In an accident situation, however, the entire spill is released to the atmosphere within a few minutes.

Off-Site Dose Calculations

Before deciding how to assure environmental acceptability, we must examine the possible consequences of various tritium releases. This has been done using advanced numerical modeling techniques. The interaction of air contaminated with tritiated water exhausting from a 30-m stack with no effective plume rise was examined for several atmospheric conditions. The site chosen was our own laboratory, which is situated near the eastern end of a small inland valley. Sheep and cattle graze on hills within 1 km of the eastern boundary of the site.

One case treated postulated the release of 10-g of tritium as tritiated water during 900 s (15 minutes). This represents a release slightly smaller than one which occurred accidentally in Livermore several years ago, which with once-through ventilation is postulated as a credible accident.^{G9} The other problem treated postulates the release of 0.1 g of HTO during 86.4-ks (24-h) period which we believe might simulate conditions during major facility maintenance operations. The most pessimistic conditions were found during the dry season.^{G10}

The results of calculations based on the 0.1-g release during average dry-weather conditions are given in Fig. G-4. We see that there is a 95% probability that no one would receive doses greater than 1.0 mrem. However, these same contours can be scaled by a factor of 300 to represent the 10-g release, where time, quantity, and time-averaged wind direction are taken into account. Clearly, if we impose a 5-mrem maximum acceptable dose increment, this latter situation would represent an unacceptably high risk.

Two pathways into the body were considered: the direct inhalation/skin route and the vegetation-to-

Fig. G-4. Dry-season inhalation-skin absorption dose (mrem); -5% probability of being exceeded. Calculations assume release of 0.1 g of HTO in 24 h.

meat-to-man route. The vegetation-to-meat-to-man sequence gave much higher dose estimates, and it would have been necessary to monitor the tritium content of the farm animals after either type of release, regardless of weather conditions. Localized rain during the wet season could give higher tritium concentrations in some areas.

The general philosophy followed in this study, therefore, has been to follow a total-containment approach in which very little tritium can escape to the outside world.

Permeation by Tritium

Hot Equipment Walls

Like other hydrogen isotopes, tritium permeates all metals to a certain extent. Usually, permeation is of little concern except when the equipment operates at high temperature. In FERF, we would expect high-temperature components in only a few places: the injector sources, which are surrounded by a vacuum; the palladium diffusers, which will be secondarily contained; and some diffusion pumps, which also can be secondarily contained. (Secondary containment systems will be described later.) Thus, the usually encountered type of permeation can be dealt with in a straightforward fashion.

Cooled First Wall

In the FERF reactor, there will be an additional and unusual type of permeation, i.e., permeation of implanted high-energy particles resulting from stray losses in the injected beam, from the uncaptured beam, from particles scattered out of the plasma, and from mirror end losses. When these particles are imbedded in the metal walls, they will tend to diffuse away from their stopping position toward the vacuum and coolant surfaces of the walls.

The tendency of the implanted ions to diffuse in both directions is independent of the material or its temperature. At steady state, the fraction of tritium diffusing out the coolant surface to that out the vacuum surface equals the ratio of implantation depth to wall thickness. This, in turn, is the concentration-gradient ratio shown in Fig. G-5. This simple argument assumes that we do not encounter blistering or peeling of the first wall and that the diffusion coefficient is independent of position.

The estimated total energetic tritium current to all the walls is 200 A, or 6×10^{-3} g/s. If the implantation depth is $1 \mu\text{m}$, the tritium permeating to the coolant would be 1.2×10^{-5} g/s, or more than 380 g each operating year. This would be unacceptable from at least two points of view: tritium is costly, and contaminated coolant would be an additional hazard.

Fig. G-5. Permeation of single-material first wall.

A composite metal wall should take care of this problem. For the metal exposed to the vacuum, we would choose a fast permeator such as palladium or LaNi₅ alloy. It would cover the outer surface of the coolant tubes, in turn made of a slow permeator such as aluminum. The amount of tritium escaping to the coolant would then be strongly material-dependent.

In a composite-tube wall such as this, the isothermal concentration profile of tritium would be something like that shown in Fig. G-6. The solubility and diffusivity of hydrogen is much higher in palladium than in aluminum. The bulk of the palladium is therefore loaded to the tritium concentration, $C_{\max}$, which is determined by the particle flux and diffusion coefficient of tritium in palladium. To calculate $C_{\max}$, we assume 70 W/mm² heat load at 90 keV consisting of deuterium and tritium. This corresponds to 1.07×10^{-7} mol/cm²·s. Then, if the implantation depth, δ , is 1 μ m, and the diffusion coefficient is 5.79×10^{-6} cm²·s,^{G11}

$$C_{\max} \approx (\text{flux}) (\delta)/D = \frac{1.07 \times 10^{-7} (10^{-4})}{5.79 \times 10^{-6}}$$

$$= 1.84 \times 10^{-6} \text{ mol/cm}^3$$

This corresponds to 1.63×10^{-5} H/Pd atom ratio. For hydrogen in alpha-phase palladium,^{G12}

$$\log P_{\text{H}_2}^{1/2} = \log \left(\frac{H}{Pd} \right) + 4.2 - \frac{522}{T} - \frac{926}{T-215} \left(\frac{H}{Pd} \right)$$

so that

$$P_{\text{atm}}^{1/2} = 3.02 \times 10^{-4}$$

In these calculations, it was assumed that the wall temperature was 350 K. The actual temperature of the palladium will drop as the palladium erodes because its thermal conductivity is so much below that of aluminum.

Knowing the equivalent equilibrium pressure of hydrogen in the palladium, we invoke thermodynamic equilibrium across the Al-Pd boundary. This is what gives us such a large drop in tritium content even through the chemical activity (effective partial pres-

Fig. G-6. Permeation of laminated Al-Pd first wall.

sure) is the same. For aluminum, the permeability, ϕ , is given by^{G13}

$$\phi = 1.1 \exp \left(-\frac{26,000}{1.987 (T)} \right) \text{ ml/cm}^2 \cdot \text{s} \cdot \text{atm}^{1/2}$$

Therefore, the permeation rate of tritium through a first-wall surface of 10 m² which is receiving the fluxes of energy and ions mentioned above is

$$\text{Loss} = \frac{\phi_{Al} P_{\text{atm}}^{1/2} A_{\text{wall}}}{t_{\text{tube}}}$$

$$= 1.1 \exp \left(-\frac{26,000}{1.987 (350)} \right) 3.02 \times 10^{-4}$$

$$\times (10^5)/0.0254$$

$$= 7.6 \times 10^{-14} \text{ ml/s}$$

In one operating year, at 2.625 Ci/ml, the tritium loss = 6.27×10^{-6} Ci for the first-wall system. This should be compared with 3.8×10^6 Ci for either aluminum or palladium when used alone in the isothermal approximation. Losses for the composite case can be ignored.

If this behavior is verified experimentally, the coolant system will not receive a significant amount of tritium.

Outgassing

Just as every metal will permeate tritium to some extent, every metal which has been exposed to tritium will also outgas tritium when exposed to air. Metals with large solubilities or which have been implanted with tritium can be especially bad in this respect. When the FERF reactor or other major system components are disassembled, we should expect tritium to be released to the atmosphere surrounding the equipment. The amount of tritium released at this time could be substantially more than that escaping during normal operation.

The amount of tritium outgassing can be estimated by assuming the release of a 100-monolayer equivalent from the metal surfaces. This is probably too high an estimate for metal surfaces that have not been bombarded with energetic particles, but it is probably low for those that have been. If we assume that the metal surface exposed is 120 m^2 , then 1.2 g ($12,000 \text{ Ci}$) escapes. The main hall would become tritium-contaminated.

We can reduce this problem by well over 90% by intentionally outgassing the equipment before disassembly.^{G14} For the FERF reactor, this might be done by shutting off the injectors and running a hot fluid through the first-wall coolant tubes while maintaining a good vacuum. For the fuel-system piping and fuel-processing equipment, electric heaters can be attached to the exterior of the components while they are vacuum-pumped. After this, the equipment could be disassembled. On the basis of our (incomplete) present knowledge, we would predict a release of less than 0.1 g tritium (1000 Ci).

Secondary Containment

The rather large tritium inventory on the cryo-pumping surfaces could be a problem if there were a severe failure of the vacuum system. Such a failure is not expected, but because of the extraordinary neutron fluences parts of the machine will receive, a sudden mechanical failure is not impossible. A large air leak into the FERF reactor would cause the condensed deuterium and tritium to evaporate quickly.

Explosive mixtures could form and the hot injector parts could cause detonation.^{G15} To preclude the occurrence of this accident situation, the room in which the machine is located will be backfilled with nitrogen. A large leak cannot create an explosive mixture.

This technique will be applied to all areas containing components having a large tritium inventory. The three areas in this category, the large hall in which the FERF reactor is located, a much smaller room containing the fuel-reprocessing equipment, and the fuel-storage vault, all will have nitrogen atmosphere, and the walls will be steel-lined and leak-proof. In the event of a large tritium release, the nitrogen will become contaminated, but no explosion or fire can occur and no tritium can escape.

Everywhere in a room, also metal-lined but isolated from the others, is equipment for removing tritium from the nitrogen. This general scheme is shown in Fig. G-7 and an example of a secondarily contained wall penetration between adjacent rooms is shown in Fig. G-8. This room constitutes the main tritium recovery system (TRS). Because it will usually contain small inventories of tritium at low concentrations, its atmosphere is air.

An accurate estimation of the design of the TRS is not yet possible. Although the use of small systems for special applications has been common practice for many years, no one has built a TRS which would have the capacity and removal efficiency required of this one. The main TRS will probably consist of activated carbon beds to trap organic vapors and protect the catalysts, heat exchangers (heaters), hot catalyst beds, more heat exchangers (coolers), molecular sieve beds to remove tritiated water formed in the catalyst beds, water injectors, and more molecular sieve beds to redry the nitrogen. All of the molecular sieve beds should be regenerable, and equipment to accomplish this must be included in the TRS room. These particular operations are presently a little beyond the state of the art. They are also the largest processing operations in the entire facility.

Cost estimates for a TRS are based on very long extrapolations of what we think we can do today. For example, a system with a $0.028\text{-m}^3/\text{s}$ (60-cfm) capacity was recently installed at Monsanto Research Corporation's Mound Laboratories at a cost of

Fig. G-7. Secondary containment of tritium using blanketing atmospheres.

Fig. G-8. Scheme for providing secondary containment for inter-room fuel line.

slightly over a quarter million dollars.^{G16} A system being constructed for Sandia Livermore Laboratories will have a $0.08\text{-m}^3/\text{s}$ capacity and should reduce tritium content to $500\text{ }\mu\text{Ci}/\text{m}^3$. Its cost (including instrumentation and control), has been estimated at between \$400,000 and \$1,000,000.^{G17} The capacity needed to process the gas in the FERF's nitrogen-blanketed rooms should be about $50\text{ m}^3/\text{s}$. The diameter of each of the molecular sieve beds is 4 m, and a total of 20 would be required. A schematic of the system needed for FERF is shown in Fig. G-9.

For two reasons, a small TRS in another isolated room is provided to maintain the air quality in the main TRS room. First, the main TRS may need total shutdown for major repair or modification. It then couldn't keep the tritium content at safe levels in its own room to permit repair operations, even if the workers wore special protective clothing. Also, it would be convenient to get into the TRS room to do minor repairs while it is operating.

Some high-energy neutrons will escape from the reactor and pass through the nitrogen blanket. Some of the neutrons in the nitrogen may form tritium through the $^{14}\text{N}(n,T)^{12}\text{C}$ reaction. The amount of tritium gas produced this way would be $20\text{ m}\ell/\text{yr}$ ($50\text{ Ci}/\text{yr}$), an amount easily handled by the main TRS.^{G18}

Accidents

Failure of First Wall

The first wall is cooled with high-pressure, fast-flowing cold water contained in thin-walled tubs. These first-wall tubes will receive massive neutron doses, will undergo severe sputtering, will experience several kinds of radiation damage along with helium embrittlement from (n, α) reactions, and will have thermal stresses imposed on them. A tube might fail, the plasma would immediately go out, and the plasma vacuum chamber would partially fill with water. Because the water would become tritium-contaminated, some tritiated water would enter the coolant loop. Therefore, coolant water cannot be circulated directly to a cooling tower, but must first go through a water-water heat exchanger located within the biological tritium shield. Thus, if the coolant water should become contaminated, it can be removed and disposed of safely.

Fig. G-9. Tritium recovery system module (10 modules required).

The same considerations apply to the water that would squirt into the main chamber if a first-wall tube should fail. The volume of water would be much less, but its tritium content would be higher. This water might be drained from the reactor and either pumped directly on to molecular sieves or evaporated and trapped by the main TRS drying beds.

Failure of Magnet Shield

Borated water will probably be used in the neutron shield and as coolant. By removing contaminated solution and adding fresh, its tritium content can be kept below 50 Ci/l. As implied earlier, a leak in the coolant circulation system could release hazardous quantities of radioactivity to the main hall in the form of tritiated water. As with failure in the first wall, a substantial water spill could be handled by the main TRS. Additional cleanup and repair would require trained workers in protective clothing, with due consideration for other forms of radioactivity that might be present. Special suits which provide an effective 10^5 reduction in tritium exposure have been developed recently at CEN/Saclay.^{G19}

Failure of Vacuum System in FERF Reactor

Failure of the vacuum system implies leakage of nitrogen and some oxygen into the system; however, no explosion or fire could occur. The palladium diffusers could be used to recover some (but not all) of the fuel which became contaminated. Similarly, any tritium that leaked into the nitrogen blanket surrounding the equipment would be removed by the main TRS. However, this cleanup would not produce a significant increase in the amount of waste already being produced, but the radioactivity level of the waste thus produced would be higher than normal.

Failure of Distillation System

The distillation columns are the only components of the entire fuel-handling system that run above atmospheric pressure. They receive no neutron radiation damage, and the chance that this will either burst or suffer weld failure is extremely remote. There is a possibility of a valve failure, but this can be dealt with.

The still and other cryogenic components will be protected by a vacuum jacket and therefore have

built-in secondary containment. Loss of vacuum in the jacket could result either from nitrogen leaking in from the outside or DT leaking out from the column interior. The former case would be of little consequence other than causing some repair work because it would result in rapid boiling of the entire distillation apparatus inventory. A pressure surge could result which would either be contained by the inherent strength of the system or, more likely, be relieved by rupturing an all-welded rupture disk which would abort the system to a large reservoir. Filling the vacuum jacket with DT would cause loss of vacuum with the same consequences; the equipment would require disassembly for repair.

Loss of vacuum in the jacket of one still will result in automatic transfer of operation to the other still, but will not necessarily result in a tritium release. If tritium did escape, it would be into the nitrogen atmosphere being processed by the main TRS.

Anticipated Tritium Releases

In a facility that is nominally totally enclosed, there are only two ways for tritium to escape. One is permeation through the stainless steel room liners followed by escape to the atmosphere through cracks or seams in the building. The other is during deliberate flushing of air locks to allow people to move out of the radioactive materials areas.

Tritium should not escape through coolants because of the secondary loops used for isolation. Also, all heat exchangers will operate at quite low temperatures, will be constructed of low-permeability copper alloys, and will have relatively small surface areas.

If we attain state-of-the-art performance with the main TRS, the partial pressure of tritium in the nitrogen will be around 10^{-8} atm. Using the steady-state permeability equation^{G20}

$$\phi = 2.9 \times 10^{-3} \exp \frac{-12,000}{RT} \frac{\text{m}\ell}{\text{cm} \cdot \text{s} \cdot \text{atm}^{1/2}}$$

and assuming $4 \times 10^4 \text{ m}^2$ of stainless steel wall liner with a 1-cm thickness, the tritium loss by permeation is calculated to be 2 mg/yr (20 Ci/yr).

Larger releases will come from flushing personnel and equipment locks. If we assume that three small air locks of 10 m^3 each are used three times per day (once per shift) as people move out of the tritium handling area, then $33,000 \text{ m}^3$ of contaminated gas would be released per year from the source. Suppose the volume of the large air lock used for moving major pieces of equipment is 3000 m^3 , and that it is used three times per year. The total contaminated volume of gas released from this source would be about $42,000 \text{ m}^3$. With a contamination level of 10^{-8} tritium, 420 mℓ (100 Ci) of tritium would be released per year.

MONITORING AND PERSONNEL PROTECTION

Two kinds of tritium monitoring will be appropriate for the FERF. The first measures the tritium contents of the effluents from the building stack. These gases would normally include only air from the offices, control room, work shops, and air locks. During periods of planned shut-down, when all the tritium is safely stored in the vault, it might also include air from the radioactive materials area if the recirculating nitrogen is replaced by once-through air ventilation. Our atmospheric modeling does not make this option attractive unless outgassing prior to disassembly is very effective.

The monitoring equipment will consist of ion chambers with readouts for stack-effluent concentrations and integrated total release. Such systems, coupled to appropriate alarm systems, already exist.^{G21} Monitors at locations throughout the facility will give people working in the area information on their local air quality. These self-contained monitors will give instantaneous local tritium concentrations and will have alarm levels at two tritium concentrations. These individual monitors will be interconnected to a master control and alarm panel in the control room. Systems such as this are also presently in use and are found to be quite satisfactory.

Personnel protection includes much of what has been discussed so far; however, in the following context it is restricted to protective clothing. Protective clothing must be worn whenever a large tritium inventory is present in the system components. This is self-evident for those areas where nitrogen blanketing

is used and breathing apparatus must be supplied. It is mandatory also in the main TRS room where air is used. With a totally contained atmosphere, a release would quickly contaminate the entire space, and even with the rather extraordinary TRS processing proposed, the atmosphere would be very toxic. It is probable that an unprotected man in a building this large could not escape quickly enough, even if he didn't have to go through an air lock. Therefore, full protection in the form of special suits will normally be required.

Self-contained breathing apparatus, ventilated hoods, and ventilated suits are in use today in this country. Other kinds of protective suits have been studied and used at the Canadian Chalk River Laboratories.^{G6} As mentioned earlier, very effective ventilated suits have recently been developed in the CEN Laboratories at Saclay, France.

CONCLUSIONS

All aspects of tritium handling and safety can be controlled. Most of the techniques and hardware requirements are known today. Many of the costs can be estimated with good accuracy, but the largest cost item, the main TRS system, may be uncertain by as much as a factor of two. It must be the subject of a major design study.

REFERENCES

- G1. F. J. Ackerman and G. J. Koskinas, Ind. Eng. Chem. Fund. 11, 332 (1972).
- G2. F. J. Ackerman, Lawrence Livermore Laboratory, private communication (1974).
- G3. W. R. Wilkes, Monsanto Research Corporation, Mound Laboratories, private communication (1973).
- G4. T. P. Wilcox, Lawrence Livermore Laboratory, private communication (1974).
- G5. A. W. Klement, C. A. Miller, R. P. Monx, and B. Shleien, Estimates of Ionizing Radiation Doses in the United States: 1960-2000, United States Environmental Protection Agency, Rept. ORP/CSD 72-1 (1972).
- G6. W. R. Bush, Assessing and Controlling the Hazard from Tritiated Water, Chalk River Nuclear Laboratories, Chalk River, Ontario, Canada, Rept. AECL-4150 (1972).
- G7. W. D. Arnold, R. Salmon, K. H. Lin, and W. DeLaguna, Preliminary Evaluation of Methods for the Disposal of Tritiated Water from Nuclearly Stimulated Natural Gas Wells, Oak Ridge National Laboratory, Rept. ORNL-TM-4024 (1973).
- G8. W. Hild, H. Krause, K. Sauer, W. Kessler, and W. Fischak, Deep-Well Disposal of Tritium Containing Liquid Effluents, Gesellschaft für Kernforschung M.B.H., Karlsruhe, Rept. KFK 1744 (1972).
- G9. D. S. Myers, J. F. Tinney, and P. H. Gudiksen, Health Physics Aspects of LLL Tritium Release, Lawrence Radiation Laboratory, Rept. UCRL-73310 (1971). See also USAEC Rept. CONF-710809.
- G10. K. P. Petersen, Lawrence Livermore Laboratory, private communication (1974).
- G11. F. A. Lewis, The Palladium-Hydrogen System (academic Press, New York, 1967), p. 102.
- G12. W. M. Mueller, J. P. Blackledge, and G. G. Libowitz, Metal Hydrides, (Academic Press, New York, 1968), p. 637.
- G13. C. M. Cochran, J. Electrochem. Soc. 108, 317 (1961).
- G14. N. W. Robinson, The Physical Principles of Ultra-high Vacuum Systems and Equipment (Chapman and Hall Ltd., London, 1968), p. 237.
- G15. H. F. Coward and G. W. Jones, Limits of Flammability of Gases and Vapors, U.S. Bureau of Mines Bulletin 503 (1952).
- G16. E. A. Mershad, Monsanto Research Corp., Mound Laboratories, private communication (1972).
- G17. W. A. Swansiger, Sandia Laboratories, Livermore, private communication (1973).
- G18. T. P. Wilcox, Lawrence Livermore Laboratory, private communication (1974).
- G19. Y. LeBourdonnec, P. Colonna, and R. Renard, Essai d'un Betement Pressurise pour Travail en Ambiance Fortement Contaminee en Tritium, Centre d' Etudes Nucleaires de Saclay, Saclay, France, Note CEA-N-167 (1973).

G20. J. J. Collins and J. C. Turnbull, Vacuum 11,
114 (1961).

G21. T. B. Rhinehammer and P. H. Tamberger,
Tritium Control Technology, U.S. AEC Rept.
WASH-1269 (1973).

Appendix H - Magnet Design

The coil design described here is not optimized. However, it is believed that relatively minor variations in shape and current distribution are required to optimize the design.

WINDING CONFIGURATION

Introduction

The magnet for FERF is more akin to a magnet for a fusion reactor than to magnets which have been designed for current plasma physics experiments. The FERF plasma, at thermonuclear density and temperature, necessitates placing the magnet conductors outside of thick shielding. The plasma occupies only a small fraction of the magnetic field volume, and it is for this small volume that the conductor geometry must be tailored.

The basic requirement is for a minimum- $|B|$ mirror field throughout a plasma volume with a circular cross-section of 25-cm radius at the midplane. The mirror ratio as measured along the z -axis is nominally 2.0.

Two choices of central induction were considered: The low-field design, with $B(z=0) = 3.75$ T; and the high-field design, with $B(z=0) = 5.00$ T. The low-field coil will use all niobium-titanium superconductor. The high-field coil will be wound with Nb_3Sn throughout at least one third of its cross-sectional area, in the highest field region.

For both the high- and low-field coils, the maximum field at the conductor would be 2.4 times the central field. Maximum conductor surface fields would be kept near the values of 9 and 12 T, respectively.

A Yin-Yang coil was chosen over a Baseball coil design because it affords superior access to the plasma region for fuel injection. An initial rough layout showed that Yin-Yang coils having major arcs with included angles of 140 degrees to 150 degrees would be required to allow for fuel injection over a broad angular spread.

The initial design was for a set of coils with major inner radii of 100 cm and minor inner radii of 30 cm. The magnet was of constant cross section. The resulting induction at the turn-around (the region of space just inside the arc of minor radius) was 14.8 T vs the

12 T desired for the high-field case.

At first, the turn-around field was reduced by thickening the conductor cross section in a direction perpendicular to the local field. This approach was tried because the conductor can be thickened during actual construction by varying the angle swept by the major radius at each winding layer and interposing straight-line sections between the major radius and minor radius arc sections. Centers of curvature of the major arcs need not be displaced. It was found, however, that if the thickening did not encroach on desired fuel injection regions, the mirror ratio dropped to 1.7 for coils centered on the machine center. Separating the coils sufficiently to achieve a mirror ratio of 2.0 destroyed the minimum-B geometry of the field. The loss of minimum-B occurred over a large enough region that average minimum-B criteria could not be met.

The final design approach then involved a lengthening of the cross section at the turn-arounds in a direction parallel to the local field. This has a first-order effect on the surface field since the sheet current density varies inversely with the dimension parallel to the field. The coil winding will be somewhat complicated, since the major arcs in each winding layer will have displaced centers. Thus, tapered fillers will be required between layers.

Considerations for shielding, conductance for pumping, and injector beam line clearance dictated a final design with inner major and minor radii of 143.1 and 55.0 cm, respectively. The included angle of the major radius arcs is 140 degrees.

Figure H-1 shows a section in the z, x plane of the final magnet configuration. The design is suitable for both low- and high-field cases, requiring only a scaling of current densities. Materials and current densities are indicated, with high-field specifications in parenthesis.

Magnetic Field Calculations

The magnetic induction, B , was calculated using the computer code MAFCO. Each half of the Yin-Yang pair was simulated by 46 filaments, 28 in the low- j region and 18 in the high- j region. The field parameters are listed in Table H-1.

Fig. H-1. Cross section of magnet.

Table H-1. Parameters for low- and high-field magnets

Parameter	Low-field (T)	High-field (T)
B (z=0)	3.75	5.00
B _{max} (axial)	7.62	10.15
R (dimensionless)	2.03	2.03
B at conductor (max)		
Crossover	9.20	12.27
Turnaround	9.61	12.81

The maximum conductor fields, 9.61 and 12.81 T are greater than the target values of 9 and 12 T by 6.7%. The average current density in the high-field magnet is $2.31 \text{ kA} \cdot \text{cm}^{-2}$. Minor adjustments of conductor location should achieve the desired field values in a more detailed design.

The radial well depth (the ratio of B at the outermost line threading plasma to B at the center) is 1.007 in the z=0 plane. This is its lowest value; the field is minimum-B everywhere between the mirrors.

A plot of the field lines which envelope the plasma is shown in Fig. H-2. The surface which envelopes a circle of 25-cm radius at z=0 at the mirror limit becomes approximately an ellipse in cross section.

The ellipse has 75.33-cm major radius and 3.84-cm

Fig. H-2. Envelope of outermost field lines surrounding plasma.

minor radius.

The forces on the magnet were calculated by simulating one of the coils with a single filament at the current-weighted averaged locus of the original 46 filaments. This filament was broken at several places, and the induction at the breaks was calculated with MAFCO. The cross-product, $\vec{T} \times \vec{B}$, was then taken to get the force per meter length. The distribution of forces in the x-, y-, and z- directions on one-fourth of a coil is shown in Fig. H-3.

STRUCTURAL DESIGN

Forces

The structure for the FERF magnet system is designed for the high-field or 5.0 T central-field case. In the following calculations, no attempt is made to

Fig. H-3. Forces on conductor bundle.

optimize the design. The solution proposed is not the only approach to the problem and may not be the best. It is clear that the complex three-dimensional structural problem of this coil structure requires extensive analysis before a final design can be made. These calculations are intended only to demonstrate feasibility within the confines of space available for magnet structure. It may be desirable to increase the magnet size somewhat to reduce the unit stress in some areas and simplify the structure problem. In Fig. H-3, only the total net force on one quarter of the current bundle is shown for the high field or 5.0-T central-field situation. For design purposes, the forces that load both the small- and large-radius turn must be known. These forces are given in Table H-2.

Table H-2. Forces on magnet.

Force	Total force ($\times 10^6$ lb)	Force on large- radius turns ($\times 10^6$ lb)	Force on small- radius turns ($\times 10^6$ lb)
F_x	39.09	14.52	24.57
F_y	105.92	62.1	43.82
F_z	-5.33	10.87	-16.2

The above tabulated values must be doubled to get the total forces on the entire coil.

Throughout the following calculations, the magnetic loads are assumed to be transmitted to the magnet case by compression, and the superconducting winding itself is not loaded in tension. In reality, some of these loads may be taken by the superconductor if the winding was placed with the proper amount of preload and if some movement was possible before the winding contacted the case. Because these conditions are difficult to achieve in a complex magnet of this design, the more conservative approach is used.

Required Structure

First consider the forces in the x direction that place a bending moment on the magnet case. If all the load were taken by the rectangular cross section of the coil case that lies in the y, z plane, the wall thickness would be excessive and both plasma volume and space available for beam injection would be reduced. Therefore, the magnetic load is divided be-

Fig. H-4. Cross section of coil in x, z plane.

tween the coil case and a semielliptical beam structure on each side of both Yin-Yang coils. The detailed analysis of a structure of this type is dependent upon the deflection of the various components and can involve a rather complex solution. For simplicity, it is assumed that the load is equally divided between the two support members.

The moment due to F_x on the large radius is

$$M_L = 14.52 \times 10^6 \times \frac{110 \text{ cm}}{2.54} = 628.8 \times 10^6 \text{ in. lb.}$$

The moment arm of 110 cm is determined by force distribution on the large-radius turn. The arm is 170 cm from the neutral axis for the small radius.

$$M_S = 24.57 \times 10^6 \left(\frac{170}{2.54} \right) = 1644.5 \times 10^6 \text{ in. lb.}$$

Total moment is 2273.3×10^6 in. lb.

$$\text{Average moment arm} = \frac{2273.3 \times 10^6}{39.09} = 58.1 \text{ in. or } 148 \text{ cm.}$$

This is shown as A in Fig. H-4.

The cross section of the coil case in the y, z plane is assumed rectangular and has the dimensions shown in Fig. H-5.

Fig. H-5. Cross section of the coil case.

Stress, in psi, is bending moment divided by the section modulus or M/IC^{-1} . Therefore, for one half the total moment and $M/IC^{-1} = 10^5$ psi,

$$I/C = \frac{2273.3 \times 10^6}{2 \times 10^5} = 11,366 \text{ in.}^3,$$

and $I/C = 1/6bd^2$ for a rectangular section where b is the width and d is the depth of the beam. In this case, the beam is hollow and

$$I/C = \frac{1}{6} [(b + 2t)(d + 2t)^2 - bd^2]$$

where t is the uniform wall thickness. Setting the above equation equal to 11,366 in.³ with b = 45 cm or 17.7 in. and d = 150 cm or 59 in., a value of t can be determined.

$$I/C = 11,366 \text{ in.}^3 = \frac{1}{6} [(17.7 + 2t)(59 + 2t)^2 - (17.7)(59)^2]$$

Solving for t: $t \approx 5 \text{ in. or } 12.7 \text{ cm.}$

In a curved beam, the centroidal axis does not coincide with the neutral axis of the x section as it does in a straight beam. Instead, it moves toward the concave side of the beam. This results in a higher stress on the inner surface and less on the outside of the curve. To balance this stress, the inner surface of the box beam would be thicker than the 5 inches calculated by ordinary flexure formula and thinner on the outer surface. In this case, the correction is about 23%.

Fig. H-6. Cross section of magnet in the x, y plane.

A 12.7-cm wall thickness is somewhat greater than that indicated on some drawings included in this report; however, the wall of the magnet case can be increased with a small reduction in the thickness of the shielding material.

Next, consider the load on the curved beam that lies in the x, y plane and carries the remaining one half of the ΣF_X or $39.09 \times 10^6/2 = 19.5 \times 10^6 \text{ lb.}$ Figure H-6 shows the magnet cross section in the x, y plane. The beam shown must be curved to clear the injectors. Again for simplicity applying the ordinary flexure formula, the maximum bending moment in the curved beam is

$$M_B = F \times A = 19.5 \times 10^6 \left(\frac{38 \text{ cm}}{2.54} \right) = 292 \times 10^6 \text{ in. lb.}$$

and the section modulus for a stress of 10^5 psi is

$$I/C = M_B = \frac{292 \times 10^6}{10^5} = 2920 \text{ in.}^3$$

The most efficient beam for this purpose would be a fabricated box or I-beam; however, the deflection would have to match that of the magnet case at the maximum stress condition. To roughly size this beam, assume it measures (see Fig. H-7) 100 cm between the inner and outer curved surfaces and is 50-cm wide. For the I/C value of 2920 in.³ and $I = 2920 \times 19.7 = 57,524 \text{ in.}^4$, the thickness of the flange can be calculated. The moment of inertia for any plane area is

$$I = \int r^2 da,$$

Fig. H-7. I-beam dimensions.

or, in this case,

$$I = 2b \int r^2 dr \text{ or } I = \frac{2b}{3} [r_2^3 - r_1^3]$$

Therefore,

$$(r_1 = 14.8 \text{ inch, and } t = r_2 - r_1 = 4.9 \text{ inch.})$$

Due to the curvature, the centroid has moved toward the inner surface of the beam, and the inner flange would be thicker than the outside flange if the surface stress in each flange were the same. The best solution to this stress problem may be to eliminate the curved I-beam and increase the wall thickness of the case in the areas of maximum stress. This would require an increase in radius of the small-radius turns on each Yin-Yang coil, which in turn would increase the space between the large-radius turns. Some changes in the magnet would be necessary to maintain the required magnetic field profile.

Forces in the y direction on the large-radius turns are restrained by the large C-clamp structures that lie in the x, y plane (see Fig. H-8). The $\Sigma F_y = 62.1 \times 10^6 \text{ lb}$ and is nearly uniform over an area of $9,642 \text{ in.}^2$, i.e., over the area in one quadrant. The unit pressure, P, is then

Fig. H-8. C-clamp supporting coil.

$$P = \frac{62.1 \times 10^6}{9,642} = 6,440 \text{ psi}$$

A simple approximate check on this pressure can be made by a direct calculation of magnetic field "pressure":

$$P = \frac{B^2}{1.72}$$

where pressure is in psi and B is in kG. The field between the large-radius turns for a mirror radio of 2 is $50 \text{ kG} \times 2 + 100 \text{ kG}$ is 100 kG . Therefore,

$$P = \frac{100^2}{1.72} = 5,813 \text{ psi.}$$

Consider a tapered cantilever that is 1-in. thick and has a span equal to that of the largest C-clamp, i.e., 134 in. or 3.4 m (see Fig. H-9).

Then the bending moment is

$$M = \frac{F\ell^2}{2} = \frac{6440 (134^2)}{2} = 57.8 \times 10^6 \text{ in. lb.}$$

and the moment of inertia for a stress of 10^5 psi is

Fig. H-9. Tapered cantilever.

$$I/C = M = \frac{57.8 \times 10^6}{10^5} = 578 \text{ in.}^3.$$

For the rectangular section 1-in. wide, the depth of the beams, d , can be determined

$$I/C = (1/6) b d^2 = (1/6) (1) (d^2)$$

so that

$$d = 2\sqrt{(6)(578)} = 58.9 \text{ in.}$$

This would be the maximum depth of C-clamps. As the span decreases so would the depth d .

The vector sum of the forces in the $-z$ direction plus the forces in the y direction on the small-radius turns place a hoop stress on the semicircular cap (Fig. H-10). Since the forces on each side in the y direction cancel one another, only the z forces are considered.

$$\Sigma F_z = 16.2 \times 10^6 \times 2 = \frac{2(55 \text{ cm})}{2.54^2} (225) (P_z)$$

$$P_z = 8,433 \text{ psi.}$$

(The magnetic field "pressure" of 12T is 8,370 psi.) Consider this as a hydraulic pressure in a cylinder, and solve for the wall thickness for a stress of 10^5 psi.

$$\sigma = \frac{PD}{2t}$$

Fig. H-10. Stress on semicircular cap.

where σ in the stress P is pressure in psi, D is the i.d. of the winding, and t is the thickness of the case in inches.

Then

$$D = \frac{(55)(2)}{2.54} = 43.3 \text{ in.}$$

and

$$t = \frac{PD}{2 \times \sigma} = \frac{(8433)(43.3)}{2 \times 10^5} = 1.8 \text{ in.}$$

The thickness, 1.8 in., is the minimum needed to retain the z forces on the small-radius turn. The wall thickness must be increased in the x, z plane so that it will also take the loads in the y direction.

If the load

$$F_y = 43.8 \times 10^6 \text{ lb}$$

is distributed throughout the cross section of the coil case in the x, z plane, the thickness would be

$$t = \frac{F_y}{(\text{perimeter})(\sigma)} = \frac{(43.8 \times 10^6)(2.54)}{2(225 + 45) \times 10^5} = 2 \text{ in.}$$

This wall thickness must be added to the 1.8-in. thickness for a total cap thickness of 3.8 in. Actually, some of the load on the outer cap would be transmitted to the side wall of the cap and the coil and the unit stress would be reduced.

The sum of the forces in the z direction on the large-radius turn is 10.87×10^6 lb. The load is applied by compression to the curved surface of the coil cover, but since the coil width in the x direction is only 45 cm, it will be distributed to the coil cover that lies in the x, z plane and the total area of the coil from in the x, y plane. The cross section in Fig. H-11 is the box section of the coil case at the plane of transition between the small and large turns. The total area of this cross section is

$$A_{xy} = [45 + 2(12.7)] (225 + 11 + 14)$$

$$= (45)(225) = 7,475 \text{ cm}^2 = 1,159 \text{ in.}^2$$

Therefore, the unit stress on this area due to the F_z of 10.87×10^6 would be

$$\sigma = \frac{10.87 \times 10^6}{1159} = 9,378 \text{ psi.}$$

The forces in the z direction on the large-radius turns also place a bending moment on the cross section of the coil case in the y, z plane. This moment is opposite in sign to that imposed by the F_x on this same cross section and would reduce the stress in this area.

The net attraction force between the two Yin-Yang coils is a compressive load on the interface when the coils are separated. An estimate of the cross-sectional area limits the unit stress to 5,000-psi maximum.

Materials for Magnet Structure

High strength, toughness, and low magnetic permeability are necessary properties of the magnet structural material. Several candidates are listed in Table H-3. Of these, the Armco 21-6-9 stainless steel appears to be a good choice. The yield and tensile strengths are very high, yet the material exhibits good toughness at cryogenic temperatures. The plane strain fracture toughness has been measured to be $108 \text{ ksi} \cdot (\text{in.})^{1/2}$,

Fig. H-11. Cross section of the coil case.

which is comparable to mild steel at room temperature. Further cryogenic data for this alloy is listed in Table H-4 and the composition is given in Table H-5. The alloy is fusion weldable, but for good ductility a high nickel filler is suggested in large weldments.

Table H-3. Properties of various metals.

Material ¹	At room temperature		At 4 K		Area reduction or elongation at 4.2 K (%)	Permeability	Comments
	Yield strength (ksi)	Tensile strength (ksi)	Yield strength (ksi)	Tensile strength (ksi)			
304 SS	30	80	40-80	235	55	1.01-10	Precipitates martensite at low temperature; ferrites in weld.
316 SS CF-8M	35	90	40-80	215	60	1.01	$K_{IC} = 79 \text{ ksi}/\sqrt{\text{in.}}$; ferrites in weld.
Kromare 55	33	69	81	128	30	1.002	Fully austenitic.
21-6-9 SS	55	95	196	245	20	1.002	$K_{IC} = 108 \text{ ksi}/\sqrt{\text{in.}}$; fully austenitic.
7039 T64 Al	55	65	65	53	16	1.000	
310 SS	38	82	105	188	55	low	
Cold-worked MP35N	252	274	332	349	38	low	Cold worked 60%.

¹Reference to a company or product name does not imply approval or recommendation of the product by the University of California or the U.S. Atomic Energy Commission to the exclusion of others that may be suitable.

Table H-4. Typical properties of 21-6-9 stainless steel at cryogenic temperatures^{a,b,c}

Test temperature (°F)	Ultimate tensile strength (ksi)	0.2% yield strength (ksi)	Elongation in 1" (%)	Reduction of area (%)
75	103	58	50	70
-110	134	87	59	71
-320	203	150	—	24
-423	245	196	15	20.5

^a Armco product data^b Tested in transverse direction^c Annealed 4-3/4-in. thick slab.

Another attractive material is MP35N Multiphase because it has extremely high strength with good ductility. The alloy (composition in Table H-5) has a face-centered cubic matrix of cobalt and nickel in which the chromium and molybdenum are soluble at elevated temperatures. A face-centered cubic structure will persist upon cooling, but cold working forms small platelets of a hexagonal, close-packed structure. These small platelets are responsible for the high strength of the worked material because they impede further deformation. The large amount of cold work required limits the size of fabricated structures and also precludes welding. This probably makes the material impractical for the main coil housing. However, braces and fixtures up to 2-in. thick would be fabricated. By judicious use of these materials, design stresses of 100 ksi in the magnet structure may be permitted.

FABRICATION OF WINDINGS

The magnets will be wound on a three-axis winder which has a vertical and one horizontal axis capable of complete rotation. The remaining horizontal axis will partially rotate. Because of the cross-sectional dimensions of the magnets and the large radius swing during winding, technicians will be positioned on a mobile platform in the winding area. To accommodate increasing radial swing and conductor-layer build, tensioning and conductor pay-off equipment would be mounted on a platform capable of sidewise translation and advancing elevation adjustment.

Solder joints will be fabricated to provide maximum shear area as well as resistance to peel when under load. (This was done with the Baseball II magnet by jacketing the lapped conductor in the joint area.)

Insulation material will be a polyimide or polyimide-glass laminate. This is the most radiation-resistant organic material presently available. Interlayer insulation will consist of slotted laminate sheets, and inter-turn insulation will consist of spaced, laminated dots captured by film strip.

The variable winding space between turns will be maintained by machined or laminated copper or polyimide wedges and rectangular bars. The cross-section of the conductor is considerably larger than that required for imbedment of the superconductor filament. Therefore, the filaments could be imbedded in a corner of the conductor so that minimum strain

Table H-5. Composition of 21-6-9 stainless steel and MP35N multiphase.

21-6-9 SS		MP35N Multiphase	
Element	Composition (%)	Element	Composition (%)
Carbon	0.04-0.08	Nickel	35
Manganese	8.00-10.00	Cobalt	35
Phosphorus	0.060 max.	Chromium	20
Sulfur	0.030 max.	Molybdenum	10
Silicon	1.00 max.		
Chromium	18.00-21.00		
Nickel	5.50-7.50		
Nitrogen	0.15-0.40

Fig. H-12. Expanded view of magnet.

would result on the filaments during winding.

Layers and turns will be arranged as indicated in Fig. H-12. The inner winding surface will consist of stackable stainless steel rings which will remain part

of the structure. Similarly, rings provided at the outer periphery of the windings will provide for subsequent load transfer to the covers.

MAGNET POWER SUPPLY

The total magnetic energy is 1400 MJ. The operating current in each winding is about 10,000 A.

Figure H-13 is a simplified schematic of a suitable magnet power supply. To reduce the insulation requirements, each winding is divided into four identical sections. This also permits rapid emergency discharge of the magnet. During normal operation, the $0.06\text{-}\Omega$ discharge resistor is short-circuited by the parallel thyristor so that essentially no power is dissipated. However, under fault conditions the following sequence

Fig. H-13. Schematic of magnet power supply.

occurs:

- The magnet power supply is de-energized;
- The commutator thyristor is fired, thereby opening the parallel thyristor and allowing the magnet energy to be dissipated in the resistor.

This sequence discharges the magnet in 60s, a short time compared with the 2000s required for charging.

The rapid discharge requires that the magnet voltage per coil rise from the normal 12.5 V to 600 V and that the magnet insulation be appropriate for this voltage. Also, each of the eight discharge resistors must be capable of dissipating 175 MJ during the 60s discharge.

As shown in Fig. H-13, a typical, two-loop, current-regulator circuit actuates the conducting period of

the thyristor contactor. The a.c. loop coming from the "B dot" coil must be switched in electronically after the current has reached the operating value and the regulator preamplifier comes out of saturation. Using a standard technique in the art of magnet regulation, this second loop improves the regulator performance by making the time rate of change of field zero.

The presence of a thyristor contactor as the regular actuating element permits the use of small, water-cooled transformer rectifier units. Because these units can be located close to the magnet windings, it is possible to reduce the length of the low-voltage, high-current, d.c. bussing to the magnet.

To accommodate computer control, the regulator reference uses digital voltage selection.

Appendix I - Vacuum Pumping Requirements

Cryogenic pumps will be needed within the FERF neutral beam injectors and in the mirror chambers. Mechanical pumps will be used during degassing cycles.

INJECTOR PUMPING SYSTEM

FERF will have four neutral beam injectors, any three of which will supply D^0T^0 beams of adequate capacity to sustain the plasma. To keep the load on the plasma within tolerable limits, a gas load of $180 \text{ Torr}\cdot\ell\cdot\text{s}^{-1}$ must be pumped for each of the four injectors (see Table B-2). The space available for pumps and the size of the load precludes any method of pumping other than cryopumps.

Each injector will have two cryopumping volumes separated by a tubular impedance which is transparent to the D^0T^0 beam. This arrangement allows 90% of the gas to be pumped in the first volume at a pressure 12 times higher than the pressure in the volume adjacent to the plasma. To keep neutral beam reionization within acceptable limits, the volume adjacent to the plasma must be held below $3 \times 10^{-5} \text{ Torr}$. Pumping the full gas load at the lower pressure would require an impractically large cryopumping system.

Because of lack of space, the conventional chevron-shielded cryopump arrangement cannot be used on the injector pumps. The injector pumping system therefore consists of liquid-helium-cooled cryopanel which are shielded from the warm vacuum walls by liquid-nitrogen-cooled surfaces. The liquid nitrogen fills the need for cooling the neutralizer tubes and the gas-flow impedance tubes as well as shadow-shielding the opening to the plasma chamber. Cooling the internal elements of the beam line with liquid nitrogen permits maximum gas conductance to the cryopanel.

The characteristics of the injector pumps are listed in Table I-1.

The gas load on the plasma from gas diffusing from the injectors and streaming from the sources amounts to about 7 A of diffuse gas and 26 A of streaming gas for a total of 33 molecular amperes for all four injectors.

Table I-1. Injector pumping system.

Item	Capability
Total cryopanel area for one injector (one side only):	10^5 cm^2
Total cryopanel pumping speed for one injector:	$9 \times 10^5 \ell\cdot\text{s}^{-1}$
Pressure in the low-pressure pump space:	$3 \times 10^{-5} \text{ Torr}$
Pressure in the high-pressure pump space:	$3.7 \times 10^{-4} \text{ Torr}$

MIRROR CHAMBER PUMPS

The gas loads emerging from the mirrors in the form of high-energy D^+ and T^+ ions must also be pumped. The ion current escaping from each mirror is approximately 373 A of D^+ and T^+ , which amounts to a gas load of $32 \text{ Torr}\cdot\ell\cdot\text{s}^{-1}$. The ions will be dumped on water-cooled plates and subsequently pumped on cryopanel cooled to 4.2 K. The pressure that must be held in the mirror chambers is determined by the amount of back-diffusing gas that can be tolerated by the plasma. This amount has been estimated at 5 A of D_2^0 per mirror of $0.85 \text{ Torr}\cdot\ell\cdot\text{s}^{-1}$. The required pressure, considering the gas conductance through the mirror duct, is $2 \times 10^{-5} \text{ Torr}$. This requires a pumping speed of $1.6 \times 10^6 \ell\cdot\text{s}^{-1}$ in the pumping chamber.

To allow continuous operation of the machine, mirror chamber pumps must be designed in modules that can be separately isolated and degassed. There will be 10 to 12 modules per mirror chamber. The characteristics of the mirror chamber pumps are listed in Table I-2.

The mirror chamber can possibly operate at higher pressure (up to 10^{-4} Torr), greatly reducing the required pump capacity. This would result from the ionization of the back diffusing gas by the ion current exiting the mirrors. The effect would be a large reduction in the gas conductance from the mirror chamber to the plasma chamber.

Table I-2. Mirror chamber pumps.

Item	Capability
Required cryopanel area for one mirror chamber (one side only):	$4 \times 10^5 \text{ cm}^2$
Total pumping speed, including the effects of liquid-nitrogen-cooled entrance baffles, per mirror chamber:	$1.6 \times 10^6 \text{ l s}^{-1}$
Operating pressure:	$2 \times 10^{-5} \text{ Torr}$

RECYCLING PUMP

Because the plasma can be sustained by three of the four injectors, each injector can be shut down in sequence for degassing the cryopanel. After 1 h of operation, there will be approximately 10^5 monolayers of gas (gas $\sim 30\text{-}\mu\text{m}$ thick) on the cryopanel. By closing a valve between the injector and the plasma

volume, the cryopanel can be warmed to about 15 K, thereby converting all of the condensed deuterium and tritium back to the gas phase at an equilibrium pressure near 66 Torr. The injector will then be evacuated by an 1800-l s^{-1} mechanical pump to 10^{-2} Torr; the cryopanel would then be recooled to 4.2 K in preparation for reactivating the injector. The degassing cycle would require about 2 to 5 minutes. Time between degassing operations can be varied between a few minutes to about 1 h, depending on acceptable tritium holdup.

A 25-inch-diameter manifold will collect gas from all the cryopumps during the sequenced degassing cycles. This operation will require two 1800-l s^{-1} compound blower-piston mechanical pumps and isolation valves for all pumps. The pumped gas will be processed to remove helium and to isotopically separate deuterium and tritium. This portion of the system is described in Appendix G.

Appendix J - Heat Flux Capability of the First Wall

DEFINITION OF THE PROBLEM

Because of the directionality of the injectors and because the reactor must pass through the startup and plasma-buildup phase where the unattenuated neutrals hit the wall, the maximum thermal flux from these neutrals may range from values as high as $\sim 3300 \text{ W/cm}^2$ for short times to values of $\sim 1000 \text{ W/cm}^2$ for steady state (see Appendix C). Figure 1 (page 5) shows the region within the reactor where the flux is incident.

For FERF, two additional considerations arise in addition to the energy removal:

- The metallurgical effects of neutrals and neutrons should be separated to make analysis less difficult. To do this, the short-range neutrals (those having a 1- to 10- μm burial depth in metals) are intercepted by a sacrificial first wall or first-wall shield. The effects of the neutrons can then be evaluated in a test region immediately on the other side of this wall.

- The cooling of the first wall in FERF, compared to that in a commercial reactor, is not (economically) limited in pressure drop or pumping power. The wall may also be changed several times a year.

To remove the very large energy flux from the first wall, the following methods are among those possible:

- Transpiration cooling.
- Film cooling.
- Film evaporation.
- Ablation cooling.
- Radiation cooling.
- High-velocity, forced, convective cooling with liquids in the nonboiling and subcooled boiling regimes.

The first three methods have in common the use of a liquid coolant film on the surface of the wall facing the plasma. Plasma-wall interactions with this arrangement would probably quench the plasma because liquids have high sputtering coefficients. Ablative cooling involves the physical removal of wall material, and this probably would also quench the plasma. Radiation cooling in this geometry does not solve the problem but merely transfers it to another

surface. We have, therefore, focused our attention on the last listed method, the use of high-velocity, forced, convective cooling with liquids in the non-boiling and subcooled boiling regimes. This method has the additional advantage of producing low wall temperatures and, consequently, higher allowable stress.

Also, if the walls facing the plasma are cool, aluminum alloys may be considered for their construction. These materials produce relatively short half-life species after neutron irradiation, making the handling, disposal, and replacement of first-wall structures less of a problem.

In forced convective cooling, the maximum heat flux may be limited by any one of several engineering constraints, including:

- Allowable structural stresses.
- Maximum allowable structural temperature.
- Coolant pumping power requirements.
- Coolant pressure drops.
- Critical heat flux in boiling liquids.
- Practical tube diameters, thicknesses, and lengths.

These constraints are examined in the following sections.

FIRST-WALL CONCEPT

The concept selected for this study involves an array of closely packed tubes or thin, corrugated sheets joined to form a wall of flow channels. This highly cooled wall can be used as the first-wall shield and placed in front of the containment vessel so as to shield it from erosion. To be an attractive concept for FERF, the first-wall shield should be substantially easier and less costly to fabricate, install, maintain, and replace than the first wall or the front part of the containment vessel itself, particularly if frequent replacement proves to be necessary.

THERMAL STRESS LIMITATIONS

Because of the high heat fluxes expected in FERF, the design of the tube wall is strongly influenced by thermal stress. The thermal stress, σ_{th} , in a thin-walled tube is:

$$\sigma_{th} = \left(\frac{\alpha E}{2(1-\nu)} \right) \Delta T_w \quad (J1)$$

where ΔT_w = temperature across the tube wall, α = coefficient of thermal expansion, E = modulus of elasticity, and ν = Poisson ratio. The surface heat flux q and the conduction equation determine ΔT_w so that

$$\sigma_{th} = \left(\frac{\alpha E}{2(1-\nu)k} \right) q\delta \quad (J2)$$

where q = local heat flux, k = thermal conductivity, and δ = tube wall thickness. The quantity in parentheses is material- and temperature-dependent and is a useful guide for material selection.

Actually, the total stress is complicated by the details of specific designs but is not less than the sum of the hoop stress and the thermal stress. Therefore,

$$\sigma_{total} \geq \frac{pD}{2\delta} + \left(\frac{\alpha E}{2(1-\nu)k} \right) q\delta \quad (J3)$$

where p = pressure in the tube, and D = tube diameter. However, as an aid for preliminary material selection we will assume that thermal stress dominates and plot two different criteria relative to thermal stresses.

Using Eq. (J2) with property data taken at room temperature yields Fig. J-1, a plot of thermal stress vs thermal flux for representative materials. This plot serves to divide the materials into two categories: those that are clearly thermal-stress limited at flux levels of interest and therefore unacceptable, and those materials that are acceptable.

We see that stainless steel, vanadium, and titanium all have a sharp sensitivity to thermal flux and are not likely first-wall materials. The acceptable alloys are those of tantalum, molybdenum, niobium, aluminum, and the special aluminum alloy SAP. However, the use of aluminum alloys is uncertain, not because of high thermal stresses, but simply due to low allowable working temperature. For niobium, molybdenum, and tantalum, the stresses are well below the yield point at 1000 W/cm².

Fig. J-1. Thermal stress vs thermal flux for some materials of interest in FERF first-wall application.

An even more comprehensive material property parameter, M^* , can be used to compare potential first-wall materials

$$M^* = \left(\frac{2\sigma_y k(1-\nu)}{\alpha E} \right) = \frac{\sigma_y}{\sigma_{th}} q\delta \quad (J4)$$

where σ_y = short-time tensile yield stress. This parameter is most useful for the case of very high heat fluxes in thin-walled tubes where the thermal stresses dominate all other stresses including the hoop stresses. Under these conditions, the ratio (σ_y/σ_{th}) is essentially equal to the structural safety factor. If we use the same safety factor for all materials and the same tube-wall thickness, δ , then M^* is directly proportional to the maximum heat flux the material can safely handle.

Reliable and comparable values of the material properties (particularly the tensile yield stress) for the various materials as a function of temperature are difficult to obtain. We have tried to be as consistent as possible in the choice of the data used to calculate the M^* values plotted in Fig. J-2, but some uncertainties remain. Nonetheless, the qualitative trends

Fig. J-2. Comparison between various promising first-wall shield materials in terms of a material property parameter related to thermal stresses in thin-walled tubes.

shown in Fig. J-2 should be correct. In particular, the high yield stresses of Ta-10W and Mo-0.5Ti, coupled with favorable property data (as measured by the low slopes of Fig. J-1), result in the highest heat-flux capabilities. The niobium and aluminum alloys form an intermediate group of materials which are still possible choices, particularly if the wall temperatures can be kept low. The alloys of titanium and stainless steel and commercially pure vanadium are the least attractive of the materials investigated for these high heat-flux applications.

TRANSIENT BEHAVIOR

In addition to problems of thermal stress, the choice of wall materials is influenced by the transient (startup) behavior of the reactor. Thermal fluxes on the wall during this time interval, or during a time interval when the plasma is lost but fuel injection continues, are as high as 3300 W/cm^2 . It will be assumed that with this high flux the critical heat flux for convective heat transfer may be exceeded (although, as

will be shown later in this appendix, this does not appear to be the case if the Rousar correlation is correct). The critical heat flux is the point at which there is a sharp reduction of the local heat transfer coefficient caused by replacement of the liquid by vapor at the heated surface. Critical heat flux sets the upper limit for convective heat transfer, and burnout (tube melting) can occur.

We are concerned with the time-temperature history of the tube wall, particularly the outer surface of the tube facing the plasma. There are two parameters that may be used to gage the temperature of the tube wall. The first parameter is

$$\overline{\Delta T_\infty} \triangleq [T(\theta) - T(0)]_\infty = \frac{q\theta}{\rho C_p \delta}, \quad (\text{J5})$$

where θ is time, ρ is density, and C_p is the specific heat. This is the exact solution of the time history of the temperature of a thin plate of infinite thermal conductivity without heat removal. The second parameter, the Fourier number, is useful for the cases where the thermal conductivity is finite.

$$F = \alpha' \frac{\theta}{\delta^2} = \frac{k}{\rho C_p} \frac{\theta}{\delta^2}, \quad (\text{J6})$$

which includes α' , the thermal diffusivity. It can be shown that for finite thermal conductivity at the point x into the tube wall,

$$T(\theta) - T(0) = f(F, \overline{\Delta T_\infty}, x/\delta).$$

This is plotted in Fig. J-3.

Table J-1 sums up the $\overline{\Delta T_\infty}$ values and the Fourier numbers for a representative tube-wall thickness, δ , of 1 mm and a heat flux of $q = 3300 \text{ W/cm}^2$.

By using these values of F and $\overline{\Delta T_\infty}$ for different times, θ , the temperatures of the front face T_{ff} can be determined from Fig. J-3 by noting that the $x/\delta = 0$ line refers to the front face, the dashed line is for $\overline{\Delta T_\infty}$, and the ratio of their two intercepts on the ordinate is the factor by which $\overline{\Delta T_\infty}$ is to be multiplied to obtain the temperature of the front face.

The front-face temperature rise as a function of time is plotted in Fig. J-4. We see that the aluminum

Fig. J-3. Temperature response of finite-thickness plate ($0 \leq x \leq \delta$) with insulated rear face $x = \delta$ after sudden exposure to constant heat input at $x = 0$.

alloys have only about 48 ms before the front face starts to melt (the melting point of aluminum is 660°C) while the tantalum alloys have over 216 ms. The principal point to be made from these results is that it is more reasonable to provide as much time for transient plasma buildup or loss as possible by choosing materials with high melting points, such as tantalum, in preference to aluminum. This is particularly true for very high heat-flux cases, since there is considerable uncertainty about the critical heat flux under these conditions.

PRESSURE DROP AND PUMPING POWER CONSTRAINTS

For the steady-state flux, it is desirable to keep the temperature of the first-wall shield low to maximize

allowable stress and thus maximize heat flux capability. The decrease in the allowable stress with temperature is illustrated in Fig. J-2 since it is the dominant cause of the decrease of M^* . The maximum temperature of the outer surface of the tube at the exit is given by:

$$T_{w(\max)} \triangleq T_{wo(\text{ex})} = T_{B(\text{in})} + \Delta T_B + \Delta T_{\text{film}} + \Delta T_w \quad (\text{J7})$$

where $\Delta T_B = T_{B(\text{ex})} - T_{B(\text{in})}$ = fluid bulk temperature rise, inlet to exit and $\Delta T_{\text{film}} = (T_b - T_{wi})_{\text{ex}}$ = temperature rise from the bulk fluid to inner tube wall and $\Delta T_w = (T_{wo} - T_{wi})_{\text{ex}}$ = temperature rise across the wall. These temperature variations are shown in Fig. J-5. We have no control over the ΔT_w for a given material and wall thickness since it is proportional to the heat flux. Consequently, to minimize $T_{w(\max)}$, we must minimize $T_{B(\text{in})}$, ΔT_B , and/or ΔT_{film} .

For this design, liquid water has been selected as the coolant, and the lowest possible value of $T_{b(\text{in})}$, about 0°C , has been used. To keep the ΔT_B and ΔT_{film} low, the high flow velocities illustrated in Fig. J-6 are required. This leads to high pressure drops, Δp , and high pumping power. The pressure drop, $\Delta p = \eta g^2/2$ Dip is plotted versus the flow rate parameter, $G \Delta \rho$, in Fig. J-7. Converting these pressure drops to pumping power required per square meter of the surface area of the first-wall shield which faces the plasma gives

$$\frac{P_{\text{pump}}}{A_{\text{fw}}} = \frac{\pi D_i}{4L} \frac{G \Delta p}{\rho} = \frac{\pi f}{8} \frac{G^3}{\rho^2} \quad (\text{J8})$$

Table J-1. Values of the Fourier number and tube-wall temperature as a function of time for material selected materials (time, θ , is in seconds)

Item	SS 316	Nb FS85	Mo Mo-0.5Ti	Al 6061	Ti	Ta	V
Fourier No. F	4.840	27.00	43.00	70.00	7.300	29.600	12.00
ΔT_{∞}	44000	145000	129000	135000	142000	138500	112000

Fig. J-4. Surface temperature of the tube wall vs time for constant flux input of 3000 W/cm^2 without heat removal.

Fig. J-5. Variation of the fluid and wall temperature along a tube with uniform axial heat input.

DESIGN FOR FERF

For the reference design calculations, we have chosen $D = 10 \text{ mm}$, $\delta = 0.5 \text{ mm}$, and $L = 1 \text{ m}$ for a representative coolant tube. An inlet water pressure of 280 psi was chosen because it is an even multiple of one of the pressures used in the critical heat flux correlations which will be discussed later in this appendix. To illustrate the calculational procedure in arriv-

Fig. J-6. Reduction of bulk and film temperatures with increasing coolant mass flow rate.

ing at the maximum allowable heat flux, data for Nb-1 Zr have been used in the following figures.

Figure J-8 shows how the total stress (thermal plus hoop stress) varies with heat flux for three different tube-wall thicknesses. The hoop stress, which is the intercept at $q = 0$, increases as the tube-wall thickness decreases. However, the total stress is lowest for the 0.25-mm tube wall for heat fluxes above $\sim 600 \text{ W/cm}^2$. We chose a $\delta = 0.5 \text{ mm}$ because tubes of this diameter are easy to fabricate and to allow for the erosion caused by sputtering.

In Fig. J-9, we have replotted these data for $\delta = 0.5 \text{ mm}$ as a function of the maximum wall temperature at the tube exit for three typical values of the bulk temperature rise. If we superimpose the allowable stress ($2/3 \sigma_y$) vs temperature curve on this plot, we obtain the locus of possible maximum operating points.

For each bulk temperature rise, there is a particular value of the mass flow parameter, G , and the allowable

Fig. J-7. Pressure drop in round tubes using water coolant in the nonboiling flow regime.

Fig. J-8. Total stress as a function of thermal flux for three different tube thicknesses.

heat flux, q , at each intersection point. These values are replotted in Fig. J-10. This stress-limit curve forms the upper boundary for the maximum allowable heat flux for the material being investigated.

Fig. J-9. Total stress and allowable stress variations with temperature of Nb-1Zr tube wall. The intersection points define the maximum operating conditions for each case.

Several other constraints can be shown on this type of "performance map." For example, a second constraint is that on the fractional pressure drop. The upper limit on the fractional pressure drop is $\Delta p/p_{in} = 1.0$, which occurs at $G \approx 64,000 \text{ kg/m}^2\text{s}$ for the conditions of Fig. J-10. This is shown along with a limit boundary for $\Delta p/p_{in} = 0.5$ (at $G \approx 43,500 \text{ kg/m}^2\text{s}$). The latter value is a more practical engineering upper limit because it allows for additional pressure drops in the bends, headers, and other piping necessary to connect the tubes in the first-wall shield to the water pump. The Δp constraint forms the right-hand boundary of the permissible operating regime of Fig. J-10.

CRITICAL HEAT FLUX

A third engineering constraint for liquid coolants is that of the critical heat flux for dryout or burnout. Data are limited for critical heat flux at the very high values of mass flow rate ($G > 10,000 \text{ kg/m}^2\text{s}$) in which we are interested. However, Rousar¹¹ developed some correlations using Aerojet data on critical flux when water is used as a coolant in this high mass flow

Fig. J-10. Typical performance map showing the permissible operating regime for the reference first-wall design using Nb-1 Zr tubes.

regime. The best fit to these data is shown in Fig. J-11. The dramatic difference between Rousar's correlations and others such as MacBeth's^{J2} is quite evident. However, the data on which the MacBeth and other similar correlations are based seem to be for values of $G \leq 7000\text{--}8000 \text{ kg/m}^2\text{-s}$ while the Aerojet data is at the higher mass flows.

On that basis, we have tentatively accepted the higher q_{crit} values based on the Aerojet data as valid for our application. These q_{crit} values are plotted on the performance map of Fig. J-10 and form the minimum mass flow boundary of the permissible operating regime for the reference design.

It can be seen that the maximum allowable heat flux for Nb-1Zr is at the intersection of the stress constraint curve and the pressure drop curve. The water is in the nonboiling regime at this point, far below the critical heat flux curve. The maximum heat flux is $\sim 2 \text{ kW/cm}^2$ with Nb-1Zr tubes. At this condition, the pumping power is $\sim 380 \text{ kW/m}^2$ of first-wall surface area.

Note that the stress-limit curve in Fig. J-10 is quite flat for Nb-1Zr because the allowable stress is a weak function of temperature in this temperature region. As a result, it is possible to reduce the heat flux

Fig. J-11. Critical heat-flux correlations for uniform heat fluxes on round tubes with water coolant.

slightly and save a substantial amount on the pumping power. For example, at $G \approx 29,000 \text{ kg/m}^2\text{-s}$, the maximum allowable q is still about 1.9 kW/cm^2 , while $\Delta p/p$ is reduced to 0.25. This cuts the pumping power required to 120 kW/m^2 , about one-third of the previous value.

Note that as we reduce the mass flow, the subcooled boiling regime is reached. This regime is where most industrial applications operate to obtain the low fractional pressure drops and pumping powers required for good plant efficiency. However, for an experimental fusion facility, we are not strongly constrained in pumping power, and consequently we can operate at the higher values of G in the nonboiling regime up to some economic limit on the pumping system costs and/or on the electric power available to run the pumps.

COMPARISON OF MATERIALS

The maximum allowable heat flux capabilities for all the materials investigated are shown in the bar chart of Fig. J-12. The basic equations and the materials data used are given in Annex 1 to this Appendix, the solution method in Annex 2, and a numerical example in Annex 3. Clearly, Ta-10W and Mo-0.5Ti are superior.

The upper limit for heat flux for each material is based on short-time tensile property data where we use two thirds of the yield stress. For long-time operation, we have insufficient data on neutron damage and other factors affecting the allowable working stresses to permit choosing a design safety factor. As a rough guess, we suggest that one third the allowable yield stress be used.

All of these maximum heat flux values are well below the critical heat flux curve except for Ta-10W. Hence, all materials except Ta-10W are operating in the nonboiling regime at this maximum heat flux condition, provided that the Roussar correlation is valid.

Note that the maximum tube-wall temperatures for all the materials were below the values where creep effects become dominant. For example, the highest temperature for the Ta-10W was $\sim 1000^\circ\text{C}$; for the niobium alloys, the maximum temperatures were $< 400^\circ\text{C}$. For the aluminum materials, the maximum exit temperature was constrained to be about 120°C .

The aluminum materials were originally included in this study specifically because of their low induced activity. However, it appears that the rate of diffusion of the neutron-produced vacancies to the grain boundaries and the subsequent void formation is in inverse proportion to the melting point of the material.¹³ Consequently, the useful lifetime of structures made of the lower melting point materials may be shorter than for high melting point materials.

EFFECT OF PARAMETER VARIATIONS

The parameters chosen for the reference design ($L = 1$ m, $D = 10$ mm, $\delta = 0.5$ mm, $p_{in} = 280$ psi) are not optimized; they were simply chosen as reasonable first guesses for first-wall panel dimensions in the highest heat flux areas.

Fig. J-12. Estimates of the short-time and long-time heat-flux capability of various tube-wall materials for the reference case with water coolant ($\Delta p/p$ constrained to be 0.5 for all cases).

While no full optimization study has been performed, several calculations were made in which the tube dimensions and the inlet water pressure were varied. For example, thinner tube walls increase the heat flux capability as shown in Fig. J-8 because the thermal stresses always dominate at the high heat fluxes. Longer tube lengths, larger tube diameters, and higher water pressures were found to decrease the maximum heat flux capability somewhat for a fixed fractional pressure drop of 0.5. Further studies to find optimized designs for specific applications are certainly warranted.

CONCLUSIONS

A tubular wall of refractory metal alloys such as Ta-10W can probably handle energy fluxes from the

plasma as high as 6.4 kW/cm^2 if high-velocity water is used as the coolant. This value is for a specific geometry ($L = 1 \text{ m}$, $D = 10 \text{ mm}$, $\delta = 0.5 \text{ mm}$) and inlet water pressure (280 psi). For Mo-0.5Ti under the same conditions, the maximum value is about 5 kW/cm^2 . The niobium alloys and aluminum materials have maximum values of about 2 kW/cm^2 . Other materials, vanadium, the titanium alloys, and the stainless steels had heat flux capabilities below 1 kW/cm^2 for this application and geometry.

When the fractional pressure drop in the tubes is held at 0.50, the pumping power is about 380 kW/m^2 of first-wall area subjected to the high heat fluxes. Substantially lower values of pumping power can be obtained by a relatively small reduction in the maximum allowable heat flux. For example, the pumping power can be reduced by a factor of three with only a 5–10% reduction in the allowable heat flux.

It should be noted that there exist some important uncertainties in the analysis, e.g., the critical heat flux under these conditions and the neutron damage effects which require experimental studies.

Even though these maximum heat flux results are not optimized values, they are sufficiently high to indicate that water-cooled first walls for FERF and other early experimental fusion machines can probably be designed with some margin of safety for the unexpected.

ANNEX 1 – EQUATIONS USED IN THIS STUDY

The allowable working stress of the various materials was approximated by

$$\sigma_{\text{allow}} = f_{\text{allow}} \sigma_y = f_{\text{allow}} (A_1 - A_2 T_{\text{wo(ex)}}), \quad (J9)$$

$$\text{where } \sigma_y = A_1 - A_2 T_{\text{wo(ex)}},$$

and f_{allow} is the inverse of the safety factor.

The coefficients A_1 and A_2 were chosen to give a good fit to the tensile yield stress data near $T_{\text{wo(ex)}}$, the tube-wall temperature at the hottest point of the outer surface at the tube exit. (This is the hottest temperature of the tube material for the simplified case of uniform axial heat flux assumed in this study

as shown in Fig. J-5. Work is now in progress on a generalized study which includes axial heat-flux variations.)

The total stress in the tube wall at the exit has been estimated as the sum of the hoop stress and thermal stress; that is,

$$\sigma_{\text{tot}} \approx \sigma_{\text{hoop}} + \sigma_{\text{th}}, \quad (J10)$$

$$\text{where } \sigma_{\text{hoop}} \approx \frac{P_{\text{ex}}(D)}{2\delta},$$

$$\text{and } \sigma_{\text{th}} \approx \frac{\alpha E}{2(1-\nu)} \Delta T_w. \quad (J11)$$

These relations are valid for thin-walled tubes where $\delta/D \ll 1$. The material property data required to calculate σ_{th} were evaluated at the estimated value of $T_{\text{wo(ex)}}$ for the maximum allowable heat flux for each material. These property data are listed in Table J-2.

The temperature drop across the tube wall has been calculated for the peak value of heat flux at the point on the circumference of the tube facing the incoming energy flux, $q_{\text{peak}} = q(\Theta = 0^\circ)$, as

$$\Delta T_w = T_{\text{wo(ex)}} - T_{\text{wi(ex)}} \approx \frac{q_{\text{peak}} \delta}{k_w}. \quad (J12)$$

Note: As mentioned previously, the heat flux has been assumed to be uniform along the length of the tube, L , in these calculations. As shown in Fig. J-13, it is assumed to vary only circumferentially. As a result, ΔT_w at a given Θ is constant along the length of the tube.

Fig. J-13. Circumferential heat-flux distribution.

Table I-2. Materials property data.

Alloy	Approximate operating temperature (°C)	Coefficient of thermal expansion, α (°C ⁻¹)	Modulus of elasticity, ϵ (N/m ²)	Thermal conductivity, k_w (W/m·°C)	Approximate allowable stress, $\sigma_y(T)$ (psi)
SAP 895	120	2.2×10^{-5}	6.7×10^{10}	150	27,000
Al 6061	120	2.4×10^{-5}	8.0×10^{10}	170	35,000
Nb-1Zr	360	7.5×10^{-6}	1.1×10^{11}	50	30,000
Fe-85	400	6.8×10^{-6}	1.4×10^{11}	40	41,000
Ti-70	300	9.4×10^{-6}	8.3×10^{10}	14	30,000
316 SS	250	1.5×10^{-5}	2.0×10^{11}	16	34,000
Mo-0.5 Ti	500	5.4×10^{-6}	3.0×10^{11}	107	80,000
Ta-10W	500	7×10^{-6}	1.0×10^{11}	62	120,000

The bulk temperature rise of the water coolant has been calculated using the following assumptions: that there is no nucleate boiling (this assumption is then checked for the calculated exit flow conditions), and that the first-wall shield is made up of an array of closely packed tubes.

$$\Delta T_B = T_{B(ex)} - T_{B(in)} = \frac{\bar{q} \times (\pi D_i L)}{m C_p} \quad (J13)$$

where

$$\bar{q} = \frac{1}{2\pi} \int_0^{2\pi} q(\Theta) d\Theta \cong \frac{q_{peak}}{\pi} \quad (J14)$$

Also,

$$m = \frac{G \pi D^2}{4} = \text{constant},$$

where

$$G = \rho u.$$

The film temperature drop is a constant along the tube length of a given Θ . At $\Theta = 0^\circ$,

$$\Delta T_{film} = T_{wi(ex)} - T_{B(ex)} = C_{asym} \frac{q_{peak}}{h} \quad (J15)$$

where the convective heat transfer coefficient, h , has been evaluated using the usual correlation for non-boiling fluids. In the form sometimes called Reynold's analogy,

$$h = GC_p \frac{f}{8} Pr^{-0.6} \quad (Pr \gtrsim 0.5), \quad (J16)$$

where

$$Pr \triangleq \frac{\mu C_p}{k},$$

$$\text{and } f = 0.184 Re_e^{-0.2} \quad [2000 \lesssim Re_e \lesssim 10^6], \quad (J17)$$

$$\text{and where } Re_e \triangleq \frac{GD}{\mu}.$$

Since Reynolds numbers above 10^6 are often encountered for the high coolant flow rates of this study, a better correlation for f should be used in future work. The C_{asym} factor is a correction for the nonuniform circumferential heat flux distribution. As a first approximation based on the analytical model of Reynolds,¹⁴ a value of 1.3 has been used in this study. Further work is in progress to improve this correction factor.

The pressure drop across the tube of length L has been calculated from

$$\Delta p = p_{in} - p_{ex} = f \frac{L}{D_i} \frac{G^2}{2\rho g_c} \quad (J18)$$

where f is given by Eq. (J17).

ANNEX 2 - METHOD OF SOLUTION FOR THE MAXIMUM ALLOWABLE HEAT FLUX

This set of equations, Eqs. (J9) through (J18), has been solved for the special case of greatest immediate interest, the maximum allowable peak heat flux, q_{max} , by equating the total stress to the allowable stress; i.e. (see Fig. J-14),

$$\sigma_{tot} = \sigma_{allow} \quad (J19)$$

Using Eqs. (J9) through (J12), we obtain one equation relating $T_{wo(ex)}$ to the maximum allowable peak heat flux, q_{peak} , so that

$$B_1 + B_2 q_{peak} = f_{allow} (A_1 - A_2 \times T_{wo(ex)}) \quad (J20)$$

where the constants are functions of material properties, tube dimensions, and p_{ex} .

Fig. J-14. Allowable stress variations with peak tube-wall temperature.

The exit pressure, p_{ex} , can be eliminated as an unknown by specifying $(\Delta p/p_{in}) \triangleq (p_{in} - p_{ex})/p_{in}$. (Typically, a value of 0.5 has been used for the fractional pressure drop.) Therefore,

$$p_{ex} = p_{in} \left(1 - \frac{\Delta p}{p_{in}} \right) \quad (J21)$$

Also, G is evaluated from the specified values of Δp and p_{in} using Eq. (J18).

A second equation relating $T_{wo(ex)}$ to the peak heat flux, q_{peak} , is obtained using Eqs. (J12) to (J17) so that

$$\begin{aligned} T_{wo(ex)} &= T_{B(in)} + \Delta T_B + \Delta T_{film} + \Delta T_w \\ &= C_3 + (C_4 + C_5 + C_6) q_{peak}, \end{aligned} \quad (J22)$$

where the C 's = $f(L, D_i, C_{asym}, C_p, G, P_r f, k_w, \delta \text{ and } T_{b(in)})$

Eqs. (J20) and (J22) are two equations in the two unknowns, $T_{wo(ex)}$ and q_{max} . Solving first for q_{max} gives

Maximum allowable $q_{peak} \triangleq q_{max}$

$$= \frac{C_1 - C_2 C_3 - B_1}{B_2 + C_2 (C_4 + C_5 + C_6)}, \quad (J23)$$

where $C_1 \triangleq f_{allow} A_1$,

and $C_2 \triangleq f_{allow} A_2$

Then the corresponding $T_{wo(ex)}$ can be solved for using either Eq. (J20) or Eq. (J22).

Note: No attempt was made to choose an optimum operating pressure, p_{in} , or tube-wall thickness, δ , in this initial study. Work is now in progress on this refinement.

To assure that $T_{b(ex)}$ was below the temperature for the onset of nucleate boiling, T_{onb} , an empirical equation for T_{onb} from Tong¹⁵ was used:

$$T_{onb(ex)} \approx T_{sat(p_{ex})} + \Delta T_{J\&L(ex)} - \Delta T_{film}, \quad (J24)$$

$$\text{where } \Delta T_{J\&L(ex)} = 60 e^{-p_{ex}/900} \left(\frac{q_{max}}{10^6} \right)^{1/4} \quad (J25)$$

and

$$[p] = \text{psia}$$

$$[q] = \frac{\text{Btu}}{\text{h-ft}^2}$$

$$[\Delta T_{I\&L}] = ^\circ F.$$

$T_{b(ex)}$ was then calculated from Eq. (J13) and compared to T_{onb} to verify that

$$T_{b(ex)} \leq T_{onb(ex)}$$

A second check was then performed to verify that q_{max} was less than the critical (burnout) heat flux, q_{crit} . An empirical correlation for q_{crit} developed by Rousar¹¹ was used because his correlation is based on Aerojet data for water at high heat-flux levels and high mass flow rates very near the flow regime in this present study. At the tube exit,

$$q_{crit} = D_1 + D_2 u \Delta T_{sub(ex)} \quad (J26)$$

which is valid for $u \Delta T_{sub} \gg 5000 \text{ ft}^\circ F/s$,

where

$$\Delta T_{SUB(ex)} \triangleq T_{SAT(ex)} - T_{B(ex)}$$

The values of D_1 and D_2 in Eq. (J26) for water are then

$$D_1 = 5.1 \frac{\text{Btu}}{\text{in.}^2 \cdot s}$$

$$D_2 = 0.000860 \frac{(\text{Btu}/\text{in.}^2 \cdot s)}{(\text{ft}^\circ F/s)}$$

ANNEX 3 - EXAMPLE CALCULATION

For an illustrative example, assume that we specify the following input data:

$$L = 1 \text{ m}, D_i = 10 \text{ mm}, \delta = 0.5 \text{ mm}$$

$$P_{in} = 280 \text{ psia}, \Delta p = 140 \text{ psia}$$

$$T_{b(in)} \approx 0^\circ C$$

Calculations then procede as follows:

Assume that we use Ta-10W alloy and choose a

conservative total operating stress

$$\sigma_{tot} = 1/5 \sigma_y \text{ (i.e., } f_{allow} = 1/5)$$

(This choice is also based on the fact that it yields a maximum heat flux of about 3 kW/cm^2 .)

Solve Eq. (J23) for the maximum allowable value of the peak heat flux for this stress limit.

$$q_{max} = 3.1 \text{ kW/cm}^2$$

From Eq. (J20), the maximum tube wall temperature at the exit is

$$T_{wo(ex)} = 467^\circ C (872^\circ F)$$

From Eq. (J9) the corresponding stress is

$$\sigma_{tot} = \sigma_{allow} \approx 23,900 \text{ psi} \approx 24,000 \text{ psi}$$

We find $\sigma_y \approx 120,000 \text{ psi} = 5 \sigma_{allow}$ at a temperature of $467^\circ C$.

The stress components given by Eqs. (J10) and (J11) are

$$\sigma_{hoop} = 1400 \text{ psi}$$

$$\sigma_{th} \approx 22,500 \text{ psi}$$

We see that the thermal stress tends to dominate the hoop stress for this situation where we are trying to maximize the allowable heat flux.

Using the pressure drop Eq. (J18) and eliminating the friction coefficient using Eq. (J17), solve next for the mass flow rate parameter, G :

$$G = 4.31 \times 10^4 \frac{\text{kg}}{\text{m}^2 \cdot s} \left(31.7 \times 10^6 \frac{\text{lbm}}{\text{h} \cdot \text{ft}^2} \right)$$

The corresponding values of the friction and heat transfer coefficients are

$$f \approx 0.0092$$

$$h = 3.07 \times 10^5 \frac{\text{W}}{\text{m}^2 \cdot ^\circ C}$$

where $R_e \approx 3.2 \times 10^6$ and the Prandtl number ≈ 1.0 for an estimated mean film temperature at the tube midpoint of about 180°C (360°F).

Next, calculate the corresponding temperature differences from Eqs. (J12), (J13) and (J15):

$$\Delta T_w = 250^\circ\text{C}$$

$$\Delta T_B = 22^\circ\text{C}$$

$$\Delta T_{\text{film}} = 195^\circ\text{C} \text{ (for } C_{\text{asym}} \approx 1.3)$$

Using these values of the temperature differences, calculate the following temperatures:

$$T_{B(\text{ex})} = 22^\circ\text{C} \text{ (} 71^\circ\text{F)}$$

$$T_{wi(\text{ex})} = 217^\circ\text{C} \text{ (} 422^\circ\text{F)}$$

$$T_{wo(\text{ex})} = 467^\circ\text{C} \text{ (} 872^\circ\text{F)} \text{ as given previously.}$$

In addition, calculate the mean film temperature (at the tube midpoint):

$$T_{F(\text{mean})} \triangleq \frac{T_{B(\text{mean})} + T_{wi(\text{mean})}{2}$$

$$\approx 109^\circ\text{C} \text{ (} 228^\circ\text{F)}$$

Since this value is lower than the earlier estimated value of 180°C (360°F), we should change the water property data and then iterate until we achieve agreement on $T_{F(\text{mean})}$. This will be done in the computer code now being developed, but it was not done in these initial hand calculations.

Next, calculate the temperature for the onset of fully developed nucleate boiling (T_{onb}) from Eq. (J24):

$$T_{(\text{onb})}(\text{ex}) = T_{\text{sat}}(\text{ex}) + \Delta T_{J\&L} - \Delta T_{\text{film}}(\text{ex})$$

$$= 179^\circ\text{C} + 50^\circ\text{C} - 195^\circ\text{C}$$

$$= 34^\circ\text{C} \text{ (} 93^\circ\text{F)}$$

Since $T_{B(\text{ex})} = 22^\circ\text{C}$, this approximate equation implies that there is not fully developed subcooled

nucleate boiling.

As an alternate method of checking, compare the inner tube-wall temperature to the saturation temperature plus $\Delta T_{J\&L}$:

$$T_{wi(\text{ex})} = 217^\circ\text{C} < T_{\text{sat}}(\text{ex}) + \Delta T_{J\&L}(\text{ex}) = 229^\circ\text{C}$$

This implies that we are below the onset of fully-developed subcooled nucleate boiling. However, note that

$$T_{wi(\text{ex})} = 217^\circ\text{C} > T_{\text{sat}}(\text{ex}) = 179^\circ\text{C}$$

This implies that there will be a small amount of partial nucleate boiling. Figure J-15^{J6} shows the typical qualitative relationship between surface heat flux and surface temperature. Our operating point is in the partial subcooled boiling regime between points B and E. This small amount of subcooled nucleate boiling should have little effect on the temperatures and Δp .

Fig. J-15. Typical qualitative curve describing the variation of surface heat flux with surface temperature for subcooled boiling (taken from Fig. 8, page 976, in Reference J6).

Note that the Jens and Lottes empirical equation may not be accurate for these flow conditions. Consequently, additional experimental work is necessary

to verify the $\Delta T_{J\&L}$ at high heat fluxes and subcooling.

Finally, check the critical (burnout) heat flux using the Rousar correlation (Eq. (J26)) which is valid because

$$u\Delta T_{\text{sub(ex)}} \approx 39,000 \gg 5000 \frac{\text{ft} \cdot ^\circ\text{F}}{\text{s}}$$

Thus,

$$q_{\text{crit}} \approx 7.2 \text{ kW/cm}^2$$

This is much greater than our calculated maximum allowable heat flux of about 3.1 kW/cm^2 for our chosen structural safety factor of 5.

REFERENCES

- J1. D. C. Rousar, Correlation of Burnout Heat Flux for Fluids at High Velocity and High Subcooling Conditions, M.S. Thesis, Univ. of California, Davis (1966)
- J2. W. M. Rohsenow and J. P. Hartnett, Eds. Handbook of Heat Transfer, (McGraw-Hill, 1973), Ch. 13.
- J3. A. Mukherjee, Dept. of Mechanical Engineering, Univ. of California, Davis, private communication.
- J4. W. C. Reynolds, Int. J. Heat Mass Transfer 6, 445 (1963).
- J5. L. S. Tong, Boiling Heat Transfer and Two-Phase Flow (Wiley, 1965).
- J6. J. G. Collier and G. B. Wallis, Two-Phase Flow and Heat Transfer, Vol. III, Summer Course Notes, Stanford, (1967).

Appendix K - Building Complex

The FERF reactor building is a highly specialized concrete structure. Its design is integrated with the design of the reactor systems, the specimen manufacturing facilities, the specimen examination facilities, and the tritium handling systems.

BUILDING FUNCTIONS

The building will provide the following:

- Shelter space for the reactor and allied equipment (including maintenance and operating equipment).
- Radioactive contamination control and protection of personnel from excessive amounts of radiation.
- A containment vault that will retain released radioactive materials and gases resulting from the reactor or its associated equipment.
- Facilities for the safe collection and disposal of tritium and contaminated wastes so that the hazard to personnel operating the facility will be

minimized.

- Work areas for fabrication of specimens, for examination of irradiated samples, for certain machining operations, and for storage.
- Tritium recovery and storage facilities.
- Utilities, office space, and shops as required to meet the minimum needs of operation and maintenance.

DESCRIPTIONS OF PRINCIPLE AREAS

Building plans, sections, and elevations are shown in Figs. K-1 through K-5.

Reactor Containment Vault

The reactor containment vault provides a housing for the reactor, biological shielding, and a gas-tight vessel for any radioactivity or particles released in the event of an accident. It is constructed of reinforced concrete, with walls and roof approximately 10-feet

Fig. K-1. Artist's conception of completed FERF.

Fig. K-2. Plan view of main floor.

thick. Inside, the vault is 152-feet long, 64-feet wide and 60-feet high. To assure gas-tight integrity, the inside of the vault is sheathed with 18-gauge stainless steel which is suitably sealed to all ducts which penetrate the vault. All cables or pipes within these ducts are sealed or otherwise closed in a positive manner to assure that the vault is leak-tight. The reactor vault is designed to withstand a positive pressure of 2 psi above atmospheric; however, it is normally operated at a negative pressure.

Two vertical, hydraulically operated, gas-tight shielding doors are provided for equipment access at either end of the vault. Personnel enter and leave the vault via an air lock which leads from a limited-access area.

A 50-ton bridge crane with a shielded control cab traverses the entire vault area and is used to assemble and disassemble the reactor. This crane can also be

remotely operated from the manipulator gallery and the operation viewed through shielding windows.

The reactor vault is operated at a negative pressure and contains an inert gas such as nitrogen. This inert atmosphere is continuously circulated through high-efficiency filters to remove radioactive contamination. A leak test is performed periodically to assure that there are neither leaks nor penetrations in the structure. For this test, the vault is pressurized to about 2 psi and the leak rate is determined over a 24-hour period using the absolute pressure-temperature method.

Reactor Assembly and Air Lock Area

A reactor staging and assembly area is located at one end of the reactor vault. Here, parts of the reactor can be preassembled before final assembly in the reactor vault. Preassembled components are transferred to the reactor vault through the shielding

Fig. K-3. Plan view of basement.

door on a transfer dolly or truck. The reactor assembly area is air-tight so that it can serve as an air lock when the shielding door to the reactor vault is open. A technician's shop and tool crib is located adjacent to the assembly area.

Reactor Hot Disassembly Area

At the opposite end of the reactor vault from the assembly area is a reinforced concrete extension which forms a shielded reactor-disassembly area. Radioactive reactor components are transferred through the shielding door by truck or dolly for disassembly, decontamination, and/or examination. A sliding, shielding door communicates with an air lock which provides the means for removing parts from the area without compromising the atmosphere of the disassembly area. A personnel air lock connects the disassembly area with the limited-access area.

Reactor Viewing Gallery

Reactor maintenance operations can be observed remotely from two peripheral viewing galleries which are situated on the second floor. Remote manipulators controlled from this area are used to insert specimens in the blind specimen tubes. The wall and window shielding is designed to permit continuous personnel occupancy.

Administration and Control Area

The administration and control wing is a single-story structure of approximately 18,250 ft² and contains the following:

- Reactor control room: provides space for operating consoles, computer terminals, and closed-circuit television monitors for viewing various parts of the reactor system.
- Computer room: houses computer and associated

Fig. K-4. Transverse cross-sectional view.

equipment

- Diagnostics room: houses the remote data collecting and counting equipment.
- Offices
- Library
- Auditorium
- Conference rooms
- Electronic maintenance shop: provides facilities for maintenance of the computer, control, and diagnostics equipment
- Toilet facilities
- Lobby

Target Manufacture and Assembly Areas

The target manufacture and assembly rooms are located in the basement of the administration and control wing. This area is used to prepare and instrument the samples and specimens that enter the through-reactor specimen tubes. These through tubes terminate on the wall of the target assembly room and lead directly to the reactor.

Shops

Small assembly and maintenance shops are located throughout the building. In addition, a second target-manufacturing shop is located on the first floor adjacent to the entrance to the limited-access area that leads to the reactor vault. Samples for the blind specimen tubes that terminate on the side of the reactor are prepared in this shop.

Power Supply Areas

Rooms on the first floor and basement level house the following electrical equipment and power supplies:

- Emergency generator.
- 400-Hz alternator for ion source filament.
- Power supplies for ion source.
- Distribution switchgear.

In general, all power supplies will be water-cooled to minimize the ventilation requirements in these areas.

Fig. K-5. Longitudinal cross-sectional view.

Mechanical Equipment Areas

There are two mechanical equipment areas. A conventional one houses equipment for building heating, ventilation, and reactor refrigeration. A second, limited-access area houses potentially radioactive equipment which includes the following:

- Mechanical vacuum pumps.
- Fluid heat exchangers and pumps for cooling specimens.
- Treatment facilities and pumps for the water coolant.
- Heating and ventilation equipment for the limited-access area.

Limited-Access Area

A limited-access area on the first floor and basement levels is provided for operations and equipment that are radioactive or potentially radioactive. Two change areas provide entry. One group of change rooms contains lockers and clothing bins, toilet and shower facilities, and personnel decontamination and monitoring facilities. Shielding and life-support suits are located in a second change area that provides entry to the reactor containment vault and the reactor hot-assembly area.

Much of the space in the limited-access area is used for the nitrogen filters and the tritium recovery and storage equipment described in Appendix G.

The through specimen tubes terminate in the target disassembly and examination room. An area next to this room contains shielded "hot" cells or compartments which have viewing windows and manipulators. Irradiated specimens are disassembled and examined in these shielded compartments. A monorail system handles casks containing radioactive samples. The irradiated specimens are analyzed in an analytical chemistry laboratory located near the target disassembly room.

Cooling Towers

The reactor power which has been dissipated in the heated, low-conductivity cooling water is transferred to the atmosphere. The water is passed through heat exchangers that transfer the heat to a cooling tower. Each cooling-tower/heat-exchanger combination is rated for 12.5 MW. Table K-1 lists the

important characteristics of each cooling tower.

The total installed capacity for the FERF facility is in excess of 150 MW.

Table K-1. Characteristics of a 12.5-MW cooling tower

Cooling tower cells:

Capacity each	42,500 Btu/h
Wet bulk	70 degrees
Approach	5 degrees
Range	15 degrees
Flow rate	5700 GPM
Fan motor horsepower	150 hp

Heat exchangers:

12.5 WM each	
Capacity each	42,500 Btu/h
TW temperature in/out	75°F/90°F
LCW temperature in/out	105°F/85°F
Mean temperature difference, Δt_m	12.5°F
Approximate tube surface	17,000 ft ²
Shell diameter	45 in.
Tube length	35 ft
TW on tube side/ LCW on shell side	

Tower water pumps:

Flow rate each	5700 GPM
Head	100 ft
Motor horsepower	200 hp

LCW pumps:

Flow rate each	4250 GPM
Head	275 ft
Motor horsepower	400 hp

Deionized water manufacturing:

Capacity	15 GPM
Equipment: filters, ion exchangers, deionized water storage tanks, etc.	

LCW polishing and deaeration:

Capacity	50 GPM
Equipment: filters, ion exchangers, vacuum deaerator, pumps, etc.	

Electrical Substation

The installed power capacity of the FERF facility

substation is 180 MVA. Table K-2 lists the detailed power requirements for the complete installation.

Two 230-kV lines come to the site on a single row of towers. These lines terminate on disconnect switches for isolation and switching. Two separate, three-phase transformer banks are provided, each having three 30-MVA single-phase transformers. One spare, standby transformer can be switched into service to replace any defective transformer. The voltage ratings of the transformers are 230 kV to 13.8 kV. Circuit breakers connect each set of transformers to a 13.8-kV bus. A normally open bus tie breaker connects the two 13.8-kV busses.

Outdoor distribution transformers rated 13.8 kV to 2.4 kV provide power to the large loads such as the refrigerators and water pumps. Other outdoor transformers rated 13.8 kV to 480 V supply power to 480-V switchgear which is housed within the building. The 480-V switchgear is located at approximate centers of load and serve major loads. Local, dry-type, indoor transformers feed panels to supply lighting, communications, alarm equipment, and control.

Two gas-turbine-driven generators provide emergency power. Batteries are provided for switchgear operation, d.c. lighting, d.c. instruments, d.c. safety circuits, and d.c.-to-a.c. inverters.

Table K-2. Facility installed-power requirements.

Item	Power requirement (kVA)
Building	2000
Reactor area air circulation; 2 100-hp plus miscellaneous	250
Cooling water system:	9000
12 tower cells at 750 kVA each (each requires 150-hp fan @ 150 kVA; 200-hp tower pump @ 200 kVA; 400-hp LCW pump @ 400 kVA)	
Total miscellaneous:	100
	9100
Magnet	550
Refrigeration	
Magnet	5000
Vacuum	50
	5050

Mechanical vacuum pumps		
2 - 60 hp	120	
Miscellaneous	30	
		150
D and T recovery		
2 refrigerators; 500 W @ 25 K each	200	
2 stack blowers; 20 hp each	40	
Furnaces	30	
Nitrogen gas preheater	5000	
Process pumps	30	
		5300
Ion source power supplies		
Each source (deuterium)		
Filament	150	
Arc	136	
Acceleration	1056	
Deceleration	26	
		98,496
72 deuterium sources @ 1368 kVA each		
Each source (tritium)		
Filament	150	
Arc	136	
Acceleration	1092	
Deceleration	49	
40 tritium sources @ 1427 kVA each		57,080
	Total:	177,976
	Install:	180,000
		(180 MVA)

SITE REQUIREMENTS

No specific site has been chosen for the FERF facility; however, the cost estimate (Appendix L) is based on a level site having the following requirements:

- Suitable substrata for massive structures.
- Large amount of available power.
- Adequate pure water supply.
- Easily accessible to supporting industry and commerce and transportation networks.
- Ideal weather conditions for dispersion of released low-level radioactive gas.
- Access roads, sidewalks, fences, and landscaping.

Appendix L - Details of Reactor Facilities

Details of the cost estimate and a discussion of the cost-estimating method are given in Tables L-1 through L-4. A summary is given in the main body of this report.

Table L-1. Reactor costs.
(Two superconducting coils, including structure, shielding, refrigeration, and power supplies.)

Item	Thousands of dollars
Coils	
Superconductor, NiSn, 7.5×10^8 Am @ \$0.015/Am	\$11,250
Insulation	300
Winding fixtures	100
Winding	2,000
Assembly and installation	1,000
Total coils:	\$14,650
Coil structure	
Coil tanks, 3.1×10^5 lbs @ \$5.20/lb (including fabrication)	1,610
C-clamps, 1.4×10^6 lbs @ \$3.70/lb (including fabrication)	5,180
Intercoil structure, 3.4×10^4 lbs @ \$5.20/lb (including fabrication)	180
Bolts, fittings, small parts	700
Assembly and installation	1,000
Total coil structure:	8,700
Coil radiation shielding	
Boral, 1/4 inch, 2000 ft ² @ \$30/ft (including fabrication)	60
Lead, 9.3×10^4 lbs @ \$2.00/lb (including fabrication)	190
Stainless steel, 6×10^5 lbs @ \$5.20/lb (including fabrication)	3,120
Bolts, fittings	300
Piping	50
Assembly and installation	500
Total coil shielding:	4,220
Coil thermal shield	
Nitrogen-cooled panels, 3.2×10^3 ft ² @ \$20/ft ²	65
Reflective heat shields, 9.6×10^3 ft ² @ \$4/ft ²	40
Fittings and small parts	25
Installation	40
Total coil thermal shield:	170
Coil refrigeration system (including cryopumping)	
3200 l/h helium refrigerator/liquifier (27,000 CFM — 8000 hp)	8,000
100,000 l liquid helium storage Dewar	300
Helium recovery system	2,500
Includes: 1500-hp, 1800-psi recovery compressors; 2×10^6 ft ³ , 1800-psi storage tanks; 3500-CFM @ 1800-psi helium purifier; 10^5 ft ³ gas bags, atmospheric; 3500-CMF and 1800-psi gas drier.	

Table L-1. (Continued).

Item	Thousands of dollars	
Coil refrigeration system (including cryopumping) (Continued)		
Liquid nitrogen storage Dewars (5×10^5 l)	\$	500
Installation costs		1,000
Includes: transfer lines, recovery lines, liquid nitrogen lines, etc.		
Total coil refrigeration system:		\$12,300
Coil power supplies		
90-KW power-supply module (8 required)		
Power supply (installed)	\$22,000	
SCR switching and registor	5,000	
Firing circuits	3,000	
Total	30,000	
Modules, 8 @ \$30,000 each	240	
SCR contactor	10	
Total coil power supplies:		250
Total superconducting coils:		40,290
Injectors (4)		
Ion sources ($4 \times 28 = 112$), 112 @ \$50,000 each		5,600
Ion source power supplies		
Filament 60-Hz to 400-Hz converters, 17 MW @ \$100/kW	1,700	
Filament transformers, 112 @ \$1500 each (installed)	170	
Arc power supplies, 136 kW, 112 @ \$27,000 each	3,020	
Deuterium source, acceleration power supply, 1056 kW, 72 @ \$95,000 each	6,840	
Deuterium source, deceleration power supply, 26 kW, 72 @ \$15,000 each	1,080	
Tritium source, acceleration power supply, 1092 kW, 40 @ \$98,000 each	3,920	
Tritium source deceleration power supply	720	
Total ion source power supplies:		17,450
Base plate and connector assemblies, 4 @ \$50,000	200	
Flapper valves and drives, 4 @ \$25,000	100	
Transition sections, 4 @ \$40,000	160	
Cooling water piping	20	
Assembly and installation	100	
Total injectors		23,630
First wall		
Inner layer, material and fabrication, 500 lb @ \$100/lb	50	
Outer layer, material and fabrication, 800 lb @ \$100/lb	80	
Vacuum joints	50	
Diaphragms	20	
Manifolds	20	

Table L-1. (Continued).

Item	Thousands of dollars	
First wall (Continued)		
Fittings	\$ 20	
Assembly and installation	100	
Total first wall:		\$ 340
Irradiation facilities		
Probes, 4 @ \$50,000 each	200	
Blind tubes, 80 @ \$5,000 each	400	
Through tubes, 40 @ \$20,000 each	800	
Sample carrier, 800 @ \$50 each	40	
Cables, fittings	40	
Manifolds and piping	50	
Installation	200	
Total irradiation facilities:		1,730
Plasma collector plates		
Panels, 64 @ \$3,000 each	190	
Supports	20	
Plates, 4 @ \$3,000 each	10	
Piping	10	
Assembly and installation	50	
Total plasma collector plates:		280
Outer tanks		
Main tanks, 2, 150,000 lb @ \$5.00/lb	750	
Intermediate tanks, 2, 9,000 lb @ \$5.00/lb	50	
Expander tanks, 2, 42,000 lb @ \$5.00/lb	210	
Flange clamps	200	
Miscellaneous parts	100	
Installation	200	
Total outer tanks:		1,510
Support structure		
Structure	200	
Running gear	300	
Drives	400	
Installation	200	
Total support structure and drives:		1,100
Diagnostics		
Probes, 4 @ \$50,000 each	200	
Laser and gear, 2 assemblies @ \$125,000 each	250	
Leakage-flux sensors, 12 @ \$10,000 each	120	
Miscellaneous instrumentation	150	
Installation	30	

Table L-1. (Continued).

Item	Thousands of dollars
Diagnostics (Continued)	
Total diagnostics:	\$ 750
Controls and instrumentation	
Controls	\$ 1,000
Computer	850
Total controls and instrumentation:	1,850
Vacuum system (refrigeration included in coil refrigeration)	
Mechanical pumps	50
Valves	270
Piping	30
Cryopumping panels	120
Nitrogen-cooled panels	60
Reflective heat shields	20
Miscellaneous parts	100
Installation	150
Total vacuum system:	800
Cooling-water distribution system	100
Models and tests	
Facility models	50
First-wall tests	200
Model magnet	50
Sample tube mockup	30
Remote handling mockups	100
Total models and tests:	430
Spares	
First wall (1) (100%)	220
Ion sources (28) (25%)	1,400
Ion source power supplies (10%)	1,740
Base plate and connector assembly (1) (25%)	50
Manipulators (25%)	200
Total spares:	3,610
TOTAL REACTOR CONSTRUCTION:	\$76,420

Table L-2. Costs of reactor facilities.

Item	Thousands of dollars	
Tritium system		
Tritium purification system	\$	1,670
Tritium-associated shielding costs		100
Containment		
Pre-outgassing heaters	\$	330
Main TRS		18,700
Secondary TRS		70
	Total containment:	19,100
Monitoring and personnel protection		630
Miscellaneous		280
	Total tritium system:	\$21,780
Ancillary equipment		
50-ton crane with shielded cab		100
Shielded vehicles with manipulators, 2 @ \$500,000		1,000
Reactor remote manipulators, 4 @ \$200,000		800
Hot cells, 4 @ \$150,000		600
Hot-cell manipulators, 8 @ \$15,000		120
Sample instrumentation		150
Radiation monitoring and control		140
Shop equipment		200
Viewing windows, 10 @ \$85,000		850
TV equipment		1,000
	Total ancillary equipment:	4,960
	Total reactor facilities construction:	\$26,740

Table L-3. Costs of building and utilities.

Item	Thousands of dollars	
Building		
Excavation	\$	50
Piling		290
Containment vault and hot disassembly		3,180
Stainless steel wall lining		310
Penetrations (200)		80
Low-bay areas 99602 ft ²		5,980
Hydraulic shielding doors, 2		500
		10,390
Electrical substation		\$10,390
230-kV switchgear		650

Table L-3. (Continued).

Item	Thousands of dollars	
Electrical substation (Continued)		
230- to 138-kV transformers, 200 MvA total	\$ 500	
13.8 kV switchgear and feeders	490	
13.8-kV to 480-V unit substations (66 total)	5,270	
	<hr/> 6,910	\$ 6,910
LCW cooling system		
Cooling towers and associated equipment, 150 MW	6,270	
Water treatment system	530	
Pumps and auxillary	300	
	<hr/> 7,100	7,100
Site utilities		200
Site improvements (landscaping, paving, roads, etc.)		300
		<hr/> 24,900
Architect, engineer, inspection (15%)		3,740
Contingency (15%)		4,300
		<hr/> 32,940
TOTAL BUILDING AND UTILITIES:		32,940
TOTAL PLANT:		\$226,370

Table L-4. Total plant cost

Item	Thousands of dollars ^a	
Total reactor construction (from Table L-1)	\$ 76,420	
Total reactor facilities construction (from Table L-2)	26,740	
Total reactor and reactor facilities construction	103,160	
Engineering, design, inspection, and administration (25% of \$103,160)	25,790	
Total construction and design	128,950	
Contingency (50% of \$128,950)	64,480	
Total reactor costs	193,430	\$193,430
Total building and utilities (from Table L-3)	32,940	32,940
TOTAL PLANT:		\$226,370

^aIn 1974 dollars

COST ESTIMATING METHODS

In making the cost estimate, we have used techniques developed at LLL and LBL during the past decade for estimating costs of large devices such as particle accelerators. Experience gained on these projects has enabled us to refine our procedures while establishing reliable planning factors and high quality standards. The actual construction costs for several accelerators have been compared with the respective estimates, and we now have confidence in the procedures.

The three major methods used in the FERF costs analysis were time-and-material, formula, and projection. A combination of all three was used to derive the cost estimates included here.

Time-and-Material Method

The time-and-material method for estimating costs is the most conventional and is used on items which are standard and for which a detailed design can be outlined. On items which are used in large quantities, a very accurate unit cost is required so that small

errors will not be magnified. For FERF, fixed-price survey quotations were solicited from a number of commercial firms and the average was used as the estimated cost.

Formula Method

In the formula method, the cost of a new device is derived by applying a suitable multiplying factor for extrapolation to the known unit cost of a previous, similar device. Adjustments are made for size, quantity, fabrication differences, and escalation. This method is very successful on items for which a conceptual design exists, e.g., vacuum systems, controls power supplies, etc.

Projection Method

In the projection method, an overall cost is obtained by estimating the time and size of the team required to complete the task. This technique lends itself to development projects and is accurate where the competency of the development projects and is accurate when the competence of the development group is known from previous assignments.