Hypothetical Fusion Propulsion Rocket Vehicle

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A specific fusion propulsion rocket design is investigated, on the assumption that a stable self-sustained ^{3}He-D plasma will be achieved in present controlled-fusion research programs. The status of pertinent plasma physics is reviewed, together with an analysis of the requirements for a successful controlled-fusion system. This system must include a ^{3}He-D self-sustained fusion plasma confined in a magnetic trap produced by superconducting electromagnets. A self-consistent set of plasma physics parameters is assumed, and the resulting engineering parameters are itemized. The major components of the hypothetical fusion rocket are described. Special attention is given to estimations of neutron heating and the cryogenic system design.

Introduction

Fusion propulsion for space vehicles has recently been made conceptually feasible by development of hard superconductors, high-energy neutral injection, and Lorentz trapping. Actual hardware engineering must, of course, be preceded by a successful solution of plasma stability problems. This paper presents a preliminary engineering design of a fusion propulsion rocket system designed for ignition at low-earth orbit and for delivery of 25–100 Mw of continuous exhaust power for several years. These studies were made to answer realistically the many questions of engineering feasibility. No attempt has been made to predict the probability of achieving a stable ^{3}He-D self-sustained plasma, but in order to make these studies, it was necessary to assume that such a plasma could be achieved.

Early speculation of fusion space propulsion systems assumed that plasma containment would be achieved by the magnetic field produced by flowing a large current through the plasma (such as the pinch discharge), but long and expensive experimentation has proved that these systems are inherently unstable. Therefore, present serious planning must be based on a stable, or semistable, externally confined plasma.

Since the spring of 1961, considerable attention has been given to the discovery made by J. E. Kunzler, et al., concerning the superconductive characteristics of the intermetallic compound of niobium and tin.[1] Current densities of 150,000 amp/cm^2 in magnetic fields of greater than 200,000 gauss are reported to be achievable. This accomplishment opens avenues to solving the problem of maintaining the magnetic field necessary for plasma confinement, while eliminating the requirements for the conventional heavy coils, large radiators, and structural parts and the large amount of power normally required to maintain high magnetic fields. Further, the high magnetic fields made conceptually feasible by superconductivity permit use of the ^{3}He-D fusion reaction, which greatly reduces the untenable neutron production that results from the more conventional fusion reactions.[2] The rocket design presented here was used to normalize equations defining the operation of fusion rockets in general. These equations have been developed and reported in another paper.[3] The specific rocket reported does not, however, represent the optimum system that can now be visualized. (The other paper[3] indicates system changes that result in power-to-weight ratios superior to those reported here by more than a factor of 3.)

All engineering parameters in these studies, such as working temperatures, stress levels, available materials, etc., have been taken either at measured values or within the definitely foreseeable state of the art. The details of other parts of the over-all Aerojet studies are given in other reports.[4–6]

In principle, fusion can be achieved by confining a hot plasma of two light elements in a stable magnetic bottle. Of all the possible fusion reactions, only three appear to have practical application. These are the deuterium-deuterium (D-D), the deuterium-tritium (D-T), and the helium-3-deuterium (^{3}He-D) reactions (see Formula 1).

$$\mathrm{D} + \mathrm{D} \xrightarrow{(50\%)} {}^3\mathrm{He}\ (0.82\ \mathrm{Mev}) + \mathrm{n}\ (2.45\ \mathrm{Mev})$$

$$\mathrm{D} + \mathrm{D} \xrightarrow{(50\%)} \mathrm{T}\ (1.01\ \mathrm{Mev}) + \mathrm{H}\ (3.02\ \mathrm{Mev})$$

$$\mathrm{D} + \mathrm{T} \rightarrow {}^4\mathrm{He}\ (3.5\ \mathrm{Mev}) + \mathrm{n}\ (14.1\ \mathrm{Mev})$$

$$\mathrm{D} + {}^3\mathrm{He} \rightarrow {}^4\mathrm{He}\ (3.6\ \mathrm{Mev}) + \mathrm{H}\ (14.7\ \mathrm{Mev})$$

Formula 1

As seen, the D-D and the D-T reactions release, respectively, about one-half and three-fourths of their total reaction energy in neutrons. Although for terrestrial use these neutron-producing reactions may have important applications, for a system operating in space where the massive weight of neutron shielding and thermal energy conversion equipment is intolerable, these D-D and D-T reactions cannot be competitively utilized.[7] Associated with the energy loss (i.e., adverse effect on the power balance) these neutrons

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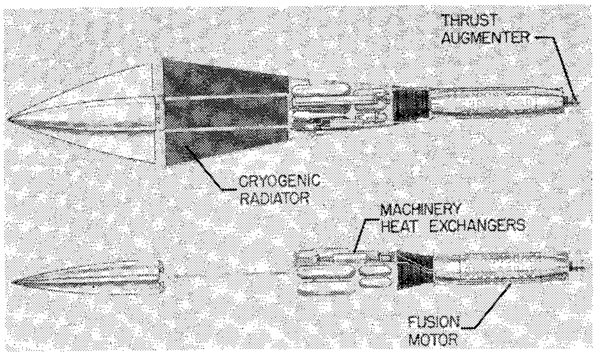

Fig. 1 Hypothetical fusion propulsion system.

produce an unacceptably large amount of neutron heating in the required cryogenically cooled superconducting magnet coils. Therefore, the ^{3}He-D reaction, for which the products are both charged particles and are therefore confinable by magnetic fields, is preferred for a space propulsion system. There are always unavoidable side reactions of the D-D and D-T types, due to the interactions of the deuterium, which is required for the primary ^{3}He-D reaction with itself and also with its daughter product, tritium These side reactions, however, can be held to a small fraction of the total power; in the system reported here, they produce less than $1\frac{1}{2}\%$ of the total system power in neutrons.

In order to achieve the desirable hot ^{3}He-D plasma that would result in a self-sustaining thermonuclear reaction, it is necessary to find a magnetic confinement geometry that is capable of overcoming the numerous plasma instabilities known to exist. The cusp geometry seems to be immune to the most disastrous MHD or outward flute class of instabilities. The simple cusp geometry, however, has fundamental drawbacks of its own and cannot be expected to provide the long confinement time that is required. Ioffe[8] of the USSR has experimentally combined the cusp and the regular mirror geometry into a multipolar (or mirror-cusp) system. This system shows signs of having both the needed resistance to instabilities and a long adiabatic confinement time. These results appear to be confirmed by theoretical[9] and small-scale experiments in the United States and England. This mirror-cusp magnetic geometry, built with superconducting coils, is assumed for the study in this paper.

To achieve ignition of the original fusioning plasma, it will be necessary to have a method of obtaining high-energy particles trapped near the center of a magnetic geometry.§ Because of the high magnetic field required to confine the energetic daughter proton from the ^{3}He-D reacting plasma, high-energy neutral injection appears to be the ideal injection system. Recent work has shown that, with intense magnetic fields, the neutral particles injected into the bottle can be successfully ionized and trapped by the Lorentz or $\mathbf{v} \times \mathbf{B}$ force when the neutrals are initially excited to principal quantum states of greater than 5 or 6; in a stable magnetic geometry, this would lead to ignition.

Once ignition of a self-sustaining fusion reaction has been achieved, the injector and trapping systems can be turned off. The needed fuel can simply be fed into the reacting plasma region at low energy, where it would automatically be ionized and heated to the required temperature.

As the fusion reaction continues, each of the ions in the plasma will finally be scattered into the escape cone of the confining mirror-type field. By adjusting the magnetic strength of the aft or exhaust mirror coil so that it is slightly weaker than the forward mirror, essentially all of the plasma will be expelled in the aft direction. This directed high-energy beam can provide the thrust of a space vehicle directly, if a high I_{sp} is desired, or mixed with additional cold expellant in a thrust augmenter to reduce the I_{sp} to the lower levels.

§ The magnetic geometry must be approximately magnetically adiabatic for at least several Larmor diameters in cross section, and with a length consistent with the adiabatic criteria for mirror-type confinement.

Preliminary Design Considered

Figure 1 shows the relative size and relationship of system components in the fusion propulsion system considered for a space mission initiated from low-earth orbit. The thrust augmenter is that region in which high specific impulse exhaust of the motor can be mixed with additional cool expellant to obtain any desired I_{sp} from 500,000 sec down to perhaps 5000 sec. To the left of the fusion motor is the lithium hydride neutron scattering shield, which reduces the neutron radiation from side reactions to a tolerance 2 mr/hr in the crew compartment located at the far left. Next to the neutron shield is the machinery and recuperator of the cryogenic or refrigeration system, which uses an all-gaseous helium cycle with two stages each of compression and expansion. The tankage shown in this section of the vehicle would contain only a limited amount of expellant for use in reducing the specific impulse but would be adequate for all the ^{3}He-D fuel needed for a powered spaceflight of several years. The cryogenic radiator is the heat sink necessary for the cryogenic or refrigeration system. The rejection temperature optimizes for this design at approximately 1000° K, so the system is not materials limited.

Let us now limit the discussion to the fusion motor. As seen in Fig. 2, the innermost structural part of the motor is the louvered heat shield called the bremsstrahlung shield. This immediately surrounds the volume in which the fusion reaction would take place. Surrounding the bremsstrahlung shield are the superconducting coils which provide the magnetic field that confines the hot reacting plasma. The entire assembly is surrounded by a rigid support structure required to hold the magnet coils in place. Each section or component of this fusion propulsion system was studied in enough detail to get a realistic weight estimate and to see that no major systematic flaws exist.

The preliminary design presented here depends upon the arbitrary, non-optimized, but consistent, set of plasma-physics parameters given in Table 1. Some of the system components (e.g., the fuel ratio, method of cooling the bremsstrahlung shield, and auxiliary power sources) were selected more for illustrative purposes than for an optimum integrated system.

The engineering parameters calculated on the basis of the equations given in the earlier paper[3] are shown in Table 2. The most conservative electron temperature prediction was assumed. (If the lower prediction of electron temperature was used, the ultimate net power would have been approximately four times larger for the same weight and size motor.)

Motor Components

Bremsstrahlung Shield

The bremsstrahlung and gyromagnetic radiations constitute the significant power losses from the fusioning plasma. The shield reflects much of the radiation back into the plasma and absorbs the 10–30% of the energy which is ultimately

Table 1 Assumed plasma-physics parameters

Average ion temperature	T_i = 110 kev
Magnetic field at mirrors	B_m = 150 kgauss
Mirror ratio	R_m = 3
Beta[a]	β = 0.34
Length-to-diameter ratio	5.4
Total power	P_{total} = 33.4 Mw

[a] Ratio of the hot, expanding plasma pressure to the confining magnetic-field pressure.

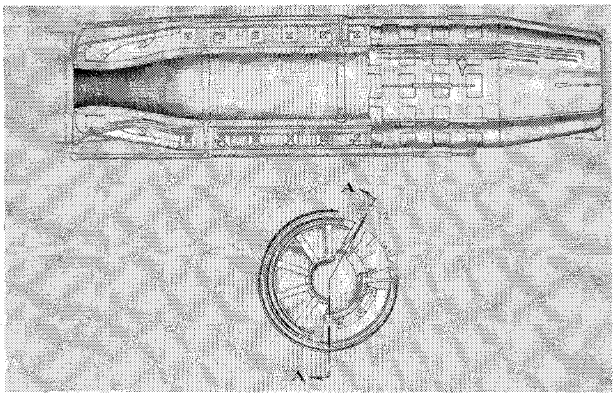

Fig. 2 Fusion motor assembly.

lost. For the power levels of this design, the shield could be self-radiating and not require an auxiliary cooling system. However, a liquid-metal coolant loop could permit operation at even higher power levels. If the heat is removed by a heat-transfer fluid, the heat can be used to supply shaft power for the cryogenic system through a thermodynamic power-generation system¶ or, as used here, a thermionic radiator. Vacuum pumping around the plasma is provided through the louvered pumpout openings at the side of the shield. These openings connect the reaction region to the vacuum of space, so that neutral atoms of gas can exit radially with a minimum of two scattering incidences.** For an externally cooled shield, a series of square tubes running longitudinally can be used. Although only small normal pressure drops would result, the magnetic field effect on the moving liquid-metal conductor in the circumferential headers could be excessive. Reduction in flow rate in the headers and their location outside the high field area is practical and could result in an acceptable value for liquid-metal pumping power. The support structure must allow for about 6 in. of longitudinal expansion.

Radiation Shielding

The radiation shielding is designed to protect the crew compartment and the machinery area from thermonuclear radiation. It consists of four lithium hydride disks, 2 ft apart. They are 8–12 ft in diameter and 1 ft thick and are canned with stainless steel. Stainless-steel spacers are used to hold the four radiation shields together in a complete assembly and to provide an adequate thermal radiation area for dissipation of neutral heat. Both the lithium and helium systems, as well as the electrical lines, pass around the outside of the shield from the machinery section aft to the motor assembly (an optimized system could reduce the weight of this shield by a factor of approximately 2.5).

Superconducting Coils

The prime considerations in motor design are how to support, fabricate, and cool the superconductive electromagnetic coils. This preliminary design is based on the tested characteristics of vanadium-gallium as the superconductor. It has been demonstrated that a mixture of vanadium and gallium ($V_{2.95}Ga$) superconducts in strong magnetic fields as high as several hundred kilogauss,[10] which is ample for optimum plasma physics requirements. For the fusion propulsion reactor, vanadium-gallium is superior to Nb-Zr, Nb_3Sn, and all other known superconductors, not only in critical magnetic fields at the higher temperatures, but also in weight per unit of current carried and in neutron cross sections.

The superconductive magnet coils (see Fig. 3), operating at cryogenic temperatures, must be rigidly supported to withstand the extreme compressive and radial loads imposed by the magnetic fields associated with them. Should the cryogenically cooled conductors contact any noncooled support structure, a serious heat load could be produced. Since it is difficult to have effective thermal insulation while supporting high compressive loads, it was assumed that the entire support structure must be cooled. Thin insulation interfaces are used to reduce the heat flow from the structure to the superconducting coils, so that the support structure

¶ The heat deposited on the shield could be used to generate auxiliary power by a conventional thermal cycle. The AGN study showed, however, that an MHD-type system produces the most electrical power for the least total extra weight.

** Other means of shielding have been considered, including surrounding the ^{3}He-D plasma with a cold cylindrical expellant plasma that would absorb the radiation emitted by the ^{3}He-D plasma particles. This system suffers from the disadvantage that the cold plasma would unavoidably diffuse into the ^{3}He-D region and extinguish the reaction. Also, calculations show that, in order to maintain a power balance, much of the cyclotron radiation must be reflected back into the ^{3}He-D plasma for reabsorption.

Table 2 Calculated propulsion system parameters

Power	
Total	33.4 Mw
Available for thrust	21.5 Mw
Neutrons	0.26 Mw
Heat shield losses	11.6 Mw
Fraction of total from D-D reaction	2%
Ion density in plasma	1.45×10^{14} ions/cm^3
Neutrons in plasma	
D-D (2.45 Mev)	2.8×10^{10} n/cm^3
D-T (14.1 Mev)	5.22×10^9 n/cm^3
Total (2.45 Mev)	3.9×10^{17} n/sec
Total (14.1 Mev)	6.8×10^{16} n/sec
Average flux at coils	
2.45 Mev neutrons	$\sim 3.57 \times 10^{11}$ n/cm^2-sec
14.1 Mev neutrons	$\sim 1.09 \times 10^{11}$ n/cm^2-sec
Size	
Plasma volume	1.3×10^7 cm^3
Plasma radius	73 cm
Plasma length	780 cm
Intermediate magnet diameter	223 cm
Mirror magnet diameter	128 cm
Length between mirror magnets	1150 cm
Shield diameter in throat	84 cm
Magnetic fields	
Mirror coils	150 kgauss
Intermediate coils	62 kgauss
Bar fields at plasma surface	3 kgauss
Fuel and Expellant	
Fuel	60% ^{3}He, 40% D
Expellant	D_2, H_2
Fuel consumed at full power	0.38 lb/day
Expellant added to exhaust (the same for any rocket of similar I_{sp} and power); for I_{sp} 5000 sec	2700 lb/day
Thrust	
At I_{sp} 4.7×10^5	=1.84 lb
At I_{sp} 5000 sec	=150 lb
System weights	
Reactor (less coils)	31,500 lb
Coils	16,800 lb
Cryogenic system	10,000 lb
Space radiator	2700 lb
Thrust and I_{sp} control	5000 lb
Biological shield	17,500 lb
Total	83,500 lb
Power-to-weight ratio	
(Exhaust jet power to total system weight)	Approx. 4 lb/kw

can be maintained at a slightly higher temperature than the coils.

The cryogenic cooling requirements are calculated from the heat introduced into the cooled sections by absorption of nuclear radiation and conductive heating. Since primary gamma rays are not produced in a fusion plasma, coil heating by gamma energy (from neutron interactions in the structural members) will be negligible.

In this preliminary engineering design, the cryogenic heat load is about 18.5 kw absorbed at temperatures of from 8 to 20°K (approximately 16.5 kw from neutron heating and approximately 2 kw from conduction or radiative transfer).

Thermal Insulation

For the low-temperature range this design calls for a superinsulation consisting of many fine layers of reflecting metal, alternated with layers of radiation-resistant, fibrous, poorly conducting material. Such insulations can give thermal conductivities near 4×10^{-5} Btu/hr-ft^2-°F/ft over the 8° to 300°K range. For higher temperatures the new Linde insulation ($k \simeq 2 \times 10^{-4}$ Btu/hr-ft^2-°F/ft) can be used. These k values vary with the insulation compressive load and the temperature difference.

Heat can enter the superconductor coils by conduction or radiation through the thermal insulation and by nuclear heating. The heat flux due to radiation and conduction can be reduced by increasing the thickness of insulation, but at the same time additional heat is generated within the increasing mass of insulation due to interactions with the neutron flux. A careful study was required to arrive at an optimum insulation thickness.

The relatively long mean free path of the high-energy neutrons in shielding material makes it unfeasible to attempt to reduce the neutron flux by the interposition of shielding materials. Therefore, for a fixed neutron flux, the neutron energy deposited in the coils can be decreased only by decreasing the amounts of materials in the coils, or by better material selection for the smallest fast-neutron cross section. In this design there has been a preliminary attempt to do the latter. Further, gaseous helium coolant is used throughout because the presence of liquid helium (a relatively good neutron moderator) would reduce the energy of the neutron flux and increase the neutron heating.

Neutron Heating

Neutron heating results from the following neutron-nuclei interactions: elastic scattering (only neutron heating resulting from the first interaction of each neutron is significant here); inelastic collision (where the effect of heating is calculated as the sum of the heating due to the recoiling compound nucleus and the heating due to the absorption of the de-excitation gamma); and activation (where the heating due to the secondary absorption of all radiations from the compound nucleus is considered).

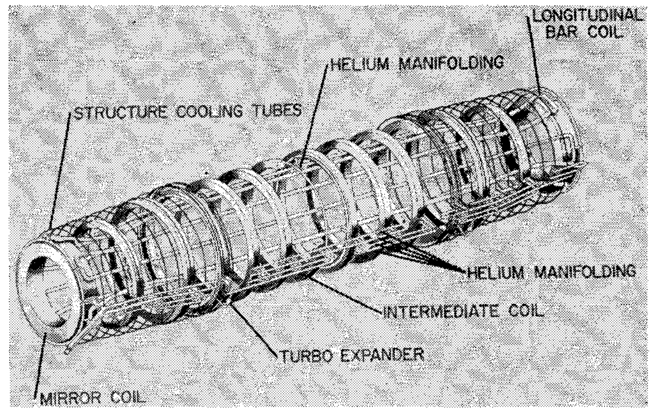

Fig. 3 Coil and helium manifolding assembly.

Table 3 lists the principal materials that must be cooled to cryogenic temperatures, their weights, and total and inelastic scatter cross sections for each of the neutron energies which would result from the fusioning plasma. The flow of the gaseous helium coolant can be seen by comparing the helium cooling section of the heat-transfer schematic diagram (Fig. 4) with the pictorial view of the coil and helium cooling system (Fig. 3).

The total heat generated by neutron interactions in the superconducting system was calculated to be about 16.5 kw for this design. It is felt that, within the present knowledge of fusion reactor configuration, this value is realistic and represents the best practical estimate. It is concluded that cryogenic coils can be designed for the heat-transfer requirements imposed by an operating fusion reactor with parameters in the ranges stated.

Design of Cryogenic System

The development problems involved in the design of a space vehicle cryogenic system capable of removing thermal energy continuously from a source maintained at temperatures in the range from 6° to 16°K are formidable. It is probably not feasible to store sufficient cryogenic fluids to remove heat loads greater than 1 kw at these temperatures for periods of more than one year. Therefore, it is necessary to provide for the steady-state operation of a shaft-power-driven cryogenic system and radiators to dissipate into space a quantity of heat much greater than that withdrawn from the cryogenic source. Because the capacity of space radiators to dissipate heat is more restricted than is usual with heat sinks available on earth, the temperatures of operation of the space radiators tend to be high. The higher the temperature at which heat is radiated, the smaller the space radiator becomes, but the larger the cryogenic power plant, so that a tradeoff is required.

The analysis of the cryogenic system was divided arbitrarily into two parts: a system analysis to show the dependence of

Table 3 Neutron heating in cryogenically cooled materials

		Cross section					
		2.45 Mev		14.1 Mev			
Material	Weight, g	Total	Non-elastic scattering	Total	Non-elastic scattering	Atomic weight, A	Total heat generation,[a] w
Fe	7.24×10^5	2.90	1.0	2.55	1.4	55.85	451
Ti	8.11×10^6	3.7	0.75	2.3	1.25	47.90	6600
V	8.40×10^4	3.0	1.65	2.4	1.4	50.95	60
Cr	1.79×10^5	3.0	1.1	2.4	1.2	52.01	126
Ga	2.70×10^4	3.1	1.75	3.19	1.52	59.72	13
Al	5.84×10^6	2.35	0.38	1.75	1.04	26.98	9104
Misc.	...	...	...	...	...	...	468
Total							16822

[a] For flux of $n_{2.45\text{ Mev}} \approx 3.57 \times 10^{11}$ n/cm^2 sec, and $n_{14.1\text{ Mev}} \approx 1.09 \times 10^{11}$ n/cm^2 sec.

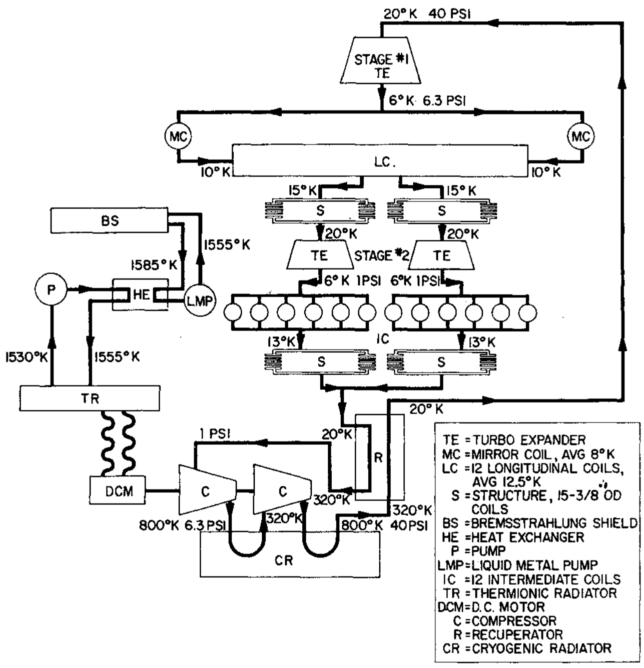

Fig. 4 Flow diagram.

the over-all thermodynamic performance on the operating characteristics of the individual components, and a preliminary design of the individual components to determine whether they could function as required within the cryogenic system. In addition, a computer program was used to determine the operating conditions for minimum over-all system weight.

Summary of Cryogenic System Findings

The weight of the cryogenic system is vested primarily in three main areas: 1) the cryogenic space radiators, 2) the powerplant that drives the cryogenic compressors, and 3) the cryogenic plant itself, particularly, compressors, turbines, and recuperative heat exchanger. The first two areas constitute more or less standard space design problems (weights were based on current design studies connected with SNAP programs), but the third is not.

It was found that a space cryogenic system, required to remove 5 kw from a cryogenic loop operating at 8°K, can be designed to weigh about 5000 lb, based on a weight for the powerplant required to drive the compressor of the cryogenic system of 7 lb/kw of compressor work. If a fusion-powered direct-conversion powerplant can be developed weighing 1.0 lb/kw of electrical energy generated, it will be possible to remove 20 kw from the cryogenic loop with a cryogenic system weighing approaching 10,000 lb.

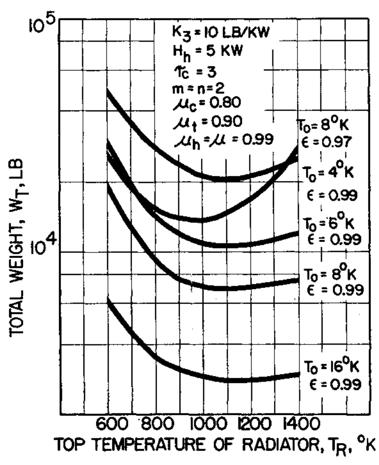

Fig. 5 Cryostat weight as a function of radiator top temperature.

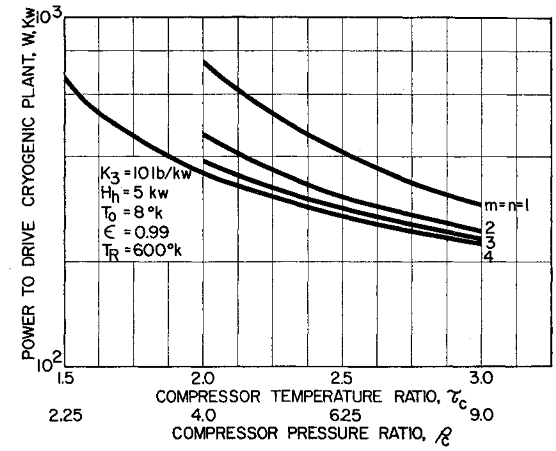

Fig. 6 Power to drive cryostat as a function of compressor performance.

The main analytical effort on the cryogenic system, because of the factors just outlined, was concentrated on 1) analysis of the dependence of the over-all performance on the performance of the components, 2) analysis of the performance of recuperative heat exchanges, 3) limited analysis of the performance of the helium compressors, and 4) an optimization of system weight based on 1), 2), and 3), and on SNAP experience. Some results of this optimization are shown in curve form in Figs. 5 through 8.

Development problems for the space cryogenic system

The design of a space cryogenic plant for the temperature range from 6° to 16°K is, to our knowledge, not currently standard design practice because the temperature range from cryogenic to space radiator conditions is greater than that covered for normal cryogenic requirements, and the compression ratios required for the helium compressors and turbines are large, and experience with the design of helium compressors and turbines is limited. The principal development problems are the regenerative heat exchanger (or recuperator) and the helium compressors.

As can be seen from Fig. 5, the effectiveness ϵ of the regenerative heat exchanger is very important in the over-all performance of the cryogenic system. In most engineering applications it is not economically justifiable to accomplish $\epsilon > 0.90$, because of the resulting size of the equipment required, but for the space cryogenic system, an ϵ near 0.99 may be needed. A regenerator to obtain $\epsilon = 0.99$ will have a length-to-diameter ratio roughly 10 times as large as a regenerator to obtain 0.9. Hence it becomes necessary, in order to reduce the over-all size, to consider heat-transfer passages with a very small hydraulic diameter. In the process it may well be required to design the regenerator to operate in the laminar flow region.

Because of the very high velocity of sound in helium, the pressure ratio per stage which can be accomplished by a helium compressor is relatively low; perhaps seven times as many

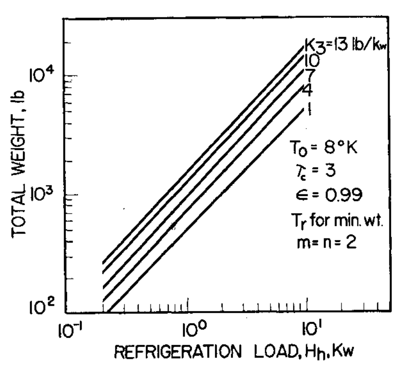

Fig. 7 Cryostat weight as a function of heat load.

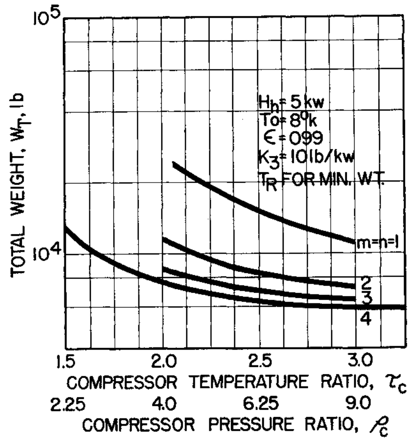

Fig. 8 Cryostat weight as a function of compressor performance.

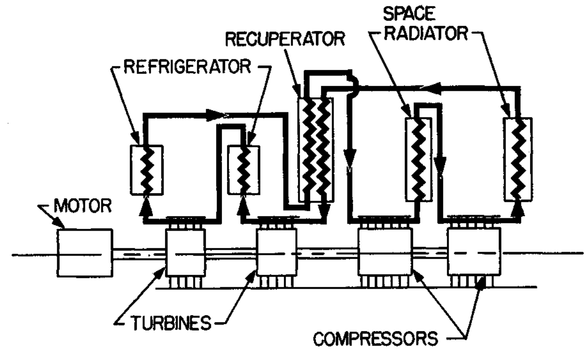

Fig. 10 Cryogenic system.

stages will be needed, compared to an equivalent air compressor. The estimated performance does improve, of course, as pressure ratio is increased (Fig. 8). A helium compressor having a larger pressure ratio per stage would require large gas velocities (large Mach numbers), which, in turn, would require high blade velocities and hence high centrifugal stresses. It is possible that the development of a modified centrifugal compressor might be considered to circumvent these problems.

Analysis of cryogenic system

The system considered is shown schematically in Figs. 9 and 10. The cryogenic heat load is absorbed in m stages of heating, each preceded by an expansion in a turbine stage. The helium then flows through one side of a regenerative heat exchanger. There are n stages of compression, each followed by heat injection in a space radiator. The helium then flows through the other side of the regenerative heat exchanger and finally back into the turbine. In the analysis, it is assumed that helium, throughout the range of temperatures and pressures used, is a perfect gas. The ratio of the outlet and inlet temperatures (connected by the arrows in Fig. 10) is represented by τ. The subscript c signifies compression of the gas, t the expansion, h the heating, e the cooling, 1 corresponds to heat exchange outside the system, and 2 to the regenerative heat exchange within the system.

Calculations for weight optimization of cryostat

The over-all weight of the cryogenic system was assumed to be vested mainly in the cryogenic plant itself, including the compressors, turbines, and the regenerative heat exchanger; the space radiator for the cryogenic system; and the powerplant that drives the cryogenic compressors, including the power turbine, pumps, and space radiator. Expressions were derived for the expected dependence of the weights of each of these components on various parameters.

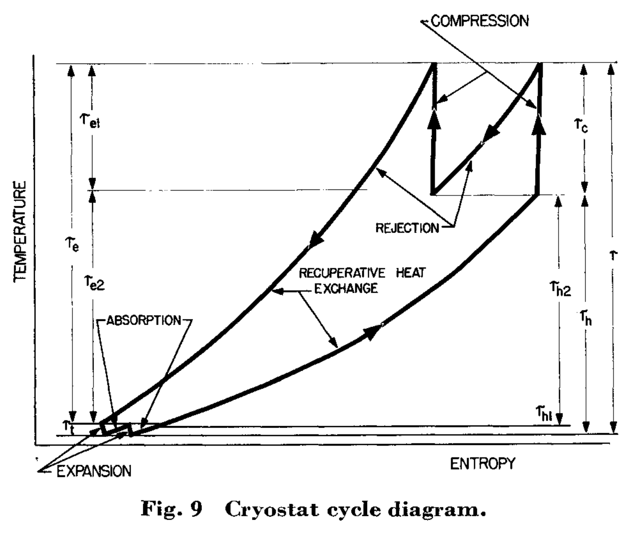

Fig. 9 Cryostat cycle diagram.

The total weight of the cryogenic system is given by

$$W_T = W_R + W_p + W_c \qquad (2)$$

where W_T is the total weight, W_R the weight of radiators required for the cryogenic system, W_p the weight of the powerplant required for powering the cryogenic plant, and W_c the weight of the cryogenic plant. The weight of the space radiators is expressed by the empirical relation, based on design experience with similar radiators,

$$W_R = 0.21\, A_R^{1.3} \qquad (3)$$

where A is effective area of radiators, ft². The weight W_p of the powerplant can be expressed in terms of the power required to drive the system W as

$$W_p = K_3 W \qquad (4)$$

From experience with similar space powerplants, the constant was first taken to be $K_3 = 7$ lb/kw.††

The weight of the cryogenic plant W_c is made up mainly of the weight of the helium compressor and of the weight of the regenerative heat exchanger, with the accompanying piping. The cryogenic compressor presents a considerable design problem. Westinghouse has designed a 22,600 hp closed-cycle gas turbine, using helium, which uses a 20-stage low-pressure unit and a 21-stage high-pressure unit to compress from 308 to 800 psi absolute;[11] the turbomachinery apparently weighs about 30,000 lb. If one-half of the total weight of the turbomachinery is chargeable to the compressor, which develops 33,000 hp, then the weight-to-power ratio is 0.45 lb/hp or about 0.61 lb/kw. For the present study it is assumed that compressor weight would not exceed 1 lb/kw; with this figure compressor weight is not a determining problem.

The regenerative heat exchanger is considered to be composed of n small tubes of diameter d_1 and thickness t_1, contained in a larger tube of diameter d_2 and thickness t_2. The high-pressure P_1 fluid flows through the small tubes, while the low-pressure P_2 fluid flows in the opposite direction outside. The length of the exchanger is l, and the density of the material of construction is γ. The weight of the exchanger W_e, neglecting end effects, is

$$W_e = (n\pi\, d_1 t_1 + \pi\, d_2 t_2)\, l\pi \qquad (5)$$

Based on the foregoing assumptions, the weight of the cryogenic plant is assumed equal to that of the regenerative heat exchanger, i.e., $W_c = W_e$. Thermodynamic and mechanical performance analyses were made to relate the weights expressed in Eqs. (2–5) to the efficiencies of the components, the effectiveness of the heat-exchanger phases, the amount of compression and expansion in the cryogenic cycle,

†† For use with an optimized fusion propulsion system, K_3 is 1–4 lb/kw.

and the number of stages of intercooling in the compression and of reheat in the expansion.

Magnet Coil Design

The mirror and intermediate coils (Fig. 3) are rectangular in cross section. A section of the mirror coil containing the axis has three sides of 0.25-in. aluminum plate. The fourth, which takes the maximum compression and bending load, is an aluminum honeycomb structure. One side of the honeycomb is thick and transmits the mirror compressions forces in shear to gussets attached to the conical section of the engine shell. Bonding of the metal coating surrounding each superconducting wire forms it into sheets that are as wide as the cross section of the coils and as thick as several wires. These sheets are wound spirally with a small space between each of the elements of the spiral to provide cooling passages through which the coolant helium can flow.

Force Calculations

To estimate the size and weight of the support structure for the motor, it is necessary to determine the magnetic forces on various structural members. The following calculations were made using the parameters for the engineering design.

It is assumed that the intermediate coils will be spaced and will have currents such that the resultant axial forces will approximately cancel. Only the mirror and the intermediate coil adjacent to each mirror are assumed to have unbalanced lateral forces acting upon them. A conservative estimate (i.e., neglecting flux leakage) of these axial compressive forces on each magnet pair can be made using[12]

$$F = B^2A/2\mu_0$$

For the case of the mirror coils in this design, $F = 11.5 \times 10^7$ N or 25.6×10^6 lb. Assuming conservative values for the leakage flux between the mirror and the intermediate coils, the compressive force between the two intermediate coils adjacent to the mirror coils is then calculated to be 8.9×10^6 lb.

Coils with components of magnetic lines of force parallel to their axes are subjected to a hoop stress F_h due to the internal magnetic pressure and are calculated in the same way as a regular pressure vessel. For example, F_h for the mirror coils $= 1.51 \times 10^7$ lb, F_h for coils adjacent to the mirrors $= 1.03 \times 10^7$ lb, and F_h for other intermediate coils $= 3.08 \times 10^7$ lb. The force F_b between two adjacent bars was found to be 3.59×10^2 lb/in., and the force component radially outward F_r was found to be 0.925×10^2 lb/in. When the entire configuration of 12 bars is considered, symmetry causes ΣF_b to become zero, and the radial force seen by each bar is 1.86×10^2 lb/in.

All of these forces resulting from the interaction of the various current-carrying members were considered for structural support in this design, which postulates use of a high-strength, lightweight metal such as a titanium-aluminum-vanadium alloy.

Main Support Structure

The main support structure of the motor assembly consists of a 10-ft-o.d. cylinder about 25 ft long, plus ends conically tapered to approximately 7 ft o.d., for over-all length of $\sim$41 ft. The cylinder is subjected to a compressive force developed by the magnetic attraction of the magnetic coils. A honeycomb structure, requiring only a 20% increase in weight to prevent buckling, is used in the compression member. A lightweight structure results, involving a honeycomb laminate of approximately 0.090-in.-thick walls on a honeycomb sandwich about 1.5 in. thick. Circumferential support is obtained from the intermediate coils, which further prevent the cylinder from buckling.

The material selected for the outside vessel wall is titanium alloy similar to Ti-6 Al-4V. In compression, a 1.1 safety factor was used with a yield strength of 310,000 psi (i.e., a design stress of 280,000 psi). A 79% increase (from 130,000 to 230,000) in yield strength results from cooling present materials to cryogenic temperatures. The design stress used here is 35% above the presently manufactured Ti-6 Al-4V.‡‡ Striking improvements in yield strength of steels from recent working techniques[13] indicate that the assumed 35% may well be a conservative expectancy.

Conclusion

This paper has shown, by examining one example of a broad class of fusion propulsion rocket systems, that, with the attainment of a ^{3}He-D fusioning plasma, substantial improvements over the presently available power-to-weight ratios could be obtained. This then conceptually makes possible manned interplanetary travel not now feasible.

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‡‡ This stress level could have been taken at the present state of the art without appreciable effect on the over-all system weights. A 35% increase in structural weight increases the over-all system weight only by approximately 6%.