ing negligible for 2-μ-diam particles. Errors of similar magnitude may be incurred due to slip flow.

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Re-Examination of Gas-Cycle Nuclear-Electric Space Powerplants

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Various liquid metal and gas cycles for nuclear space powerplants are compared in terms of specific radiator areas and weights at the one megawatt level. Although results confirm the previously established conclusion that the liquid-metal Rankine cycle generally provides low specific weights, it was found that certain gas cycles utilizing thermionic diodes are quite comparable in performance when diode efficiencies are in the range of 15 to 20%. The significance of these results, especially when enhanced by the reliability characteristics inherent in the noncorrosive, single-phase gas systems, indicates that gas cycles can no longer be excluded from consideration in nuclear powerplant applications for space.

Nomenclature

A_R = effective radiating area, ft^2
$(A_R)_1$ = radiating area of thermionic anodes in split radiator cycle (primary radiator), ft^2
$(A_R)_2$ = radiator area of Brayton segment of split radiator cycle (secondary radiator), ft^2
c_p = specific heat at constant pressure of the working fluid, Btu/lb-°F
h = enthalpy, Btu/lb
$\dot{m}$ = mass flow rate, lb/sec
P = pressure, psi
P_R = pressure drop in reactor, psi
P_{rad} = pressure drop in radiator, psi
Q_c = compressor power, Btu/sec
$(Q_e)_g$ = gross power output, Btu/sec
Q_e = electric power output, kwe
$(Q_e)_2$ = net electric power from Brayton segment of split-radiator cycle, kwe
Q_a = rate of heat deposition to thermionic cell cathodes, Btu/sec
Q_b = rate of heat removal from thermionic cell anodes, Btu/sec
Q_p = pump power, Btu/sec
Q_R = radiator heat rejection rate, Btu/sec
Q_T = turbine power, Btu/sec
r_p = compressor pressure ratio, outlet/inlet
T = temperature, °R
T_a = temperature of thermionic cell cathode, °R
T_b = temperature of thermionic cell anode, °R
T_0 = ambient temperature, °R
W_r = total radiator weight, lb
α = primary radiator/reactor pressure drop ratio in split-radiator cycle
β = radiator/reactor pressure drop ratio (also factor in thermionic cell efficiency)
γ = specific heat ratio of the working fluid (also factor in thermionic cell efficiency)
δ_R = fractional pressure drop in reactor (pressure drop/inlet pressure)
ϵ = mean radiator emissivity
η_c = compressor efficiency
η_G = electrical conversion equipment and transmission line efficiency
η_p = pump efficiency
η_T = turbine efficiency
η_{TH} = thermionic cell efficiency
σ = Stefan-Boltzmann constant (Btu/ft^2-sec-°R^4)
χ = $T_a - T_b$ = thermionic cell temperature drop, °F

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Introduction

PRESENT hardware commitments for nuclear-electric space powerplants in the megawatt range are limited to liquid-metal cycles. This decision was properly based on early comparative studies of these cycles with the closed Brayton gas cycle powerplants, in that the conclusions indicated liquid-metal specific weights to be lower by an order of magnitude.

In performing these comparisons, however, it was necessary to make certain assumptions regarding components of both cycles. Now that some time has elapsed, a number of the original assumptions may be revised, and it is therefore appropriate to re-examine the cycle comparison studies in the light of these revisions.

The principal new element to be included is the advent of the thermionic diode as a major consideration, principally because of its potential for higher peak cycle temperatures

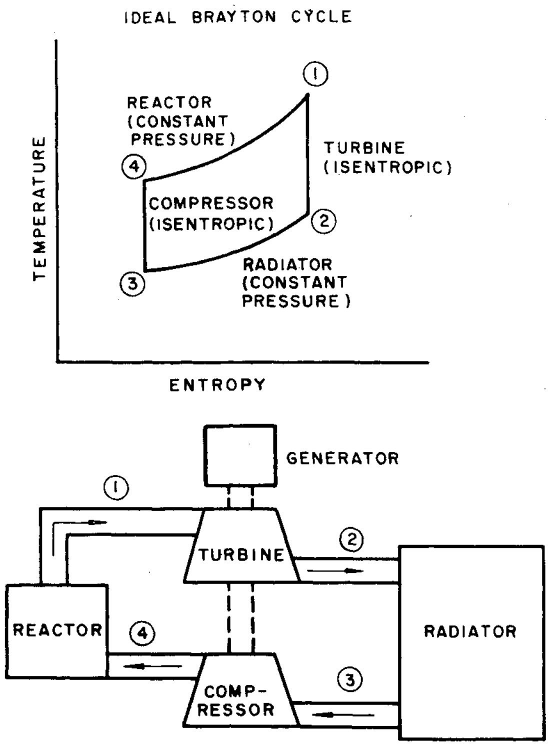

Fig. 1 Turboelectric powerplant utilizing closed gas cycle.

than are permitted by turbomachinery. The second major change is the establishment of quantitative estimates of radiator armor and redundancy requirements for given meteoroid penetration probabilities. These requirements allow consideration of the previously forbidden high radiator areas needed for efficient gas cycles. Finally, the tradeoff between cycle performance (high temperature capability) and such major considerations as operating lifetimes, reliability, and probability of successful development may be examined, at least qualitatively, in the light of several additional years of experience.

Turboelectric Systems

The only general thermodynamic systems which have been considered for turboelectric space power generation are the gas (Brayton) and vapor (Rankine) cycles. Although previous studies have indicated great weight superiority for the vapor cycles at high power levels, there are several considerations which suggest that this conclusion be reviewed. Chief among these is the requirement for long-term reliability. Gas-cooled reactors and gas-cycle turbomachinery have been notably trouble-free over years of accumulated operation, whereas alkali-metal systems have yet to demonstrate long-life performance. Further, most comparisons (e.g., Ref. 1) impose the same limitation on maximum temperature for both cycles, whereas gas turbines can almost certainly operate hotter than metal-vapor turbines. Further possible advantages of the single-phase gaseous working fluid is the avoidance of two-phase flow problems attendant to zero or low-gravity operation, and the elimination of freezing during shutdown or "coast" periods. It is therefore considered worthwhile to include the gas-cycle turboelectric system as a possible contender for long-term missions, despite its admittedly greater specific weight.

The closed thermodynamic cycle most suited to gaseous working fluids is the Brayton cycle, shown with a diagram of the system components in Fig. 1. Although there are many possible gaseous working fluids, helium appears to be well suited to the requirements of all system components because of its excellent thermal properties. Detailed analyses of reactor and radiator heat transfer and turbomachinery characteristics have been made,[2, 3] and approximate system weights have been formulated.[4–6] Gas-cooled systems of any type, turboelectric, thermionic, or thermoelectric, are not being developed at the present time due to the high radiator weight penalty discussed earlier. The details of this decision are to be reviewed subsequently.

The turboelectric mercury cycle is the oldest of the liquid-metal systems, having been in operation in ground-based central stations for over 30 years. It is the cycle now under development for the current SNAP 2 and SNAP 8 systems.

Although mercury is an excellent fluid for turbomachinery applications, it has two chief disadvantages for nuclear space systems: 1) it is a poor reactor working fluid due both to its tendency to "poison" the reactor and to become highly radioactive, requiring the entire system to be shielded instead of only the reactor and heat exchanger; and 2) it has high vapor pressure, precluding use at very high temperatures. Thus, existing mercury concepts are limited to two-stage systems (using an alkali metal as reactor coolant and an intermediate heat exchanger as shown in Fig. 2) operating well below 1500°F. It is clear that the mercury systems, because of these two major limitations, are strictly limited to first-generation hardware, and will be superseded by either the alkali metals (as soon as the latters' developmental problems have been solved), by steam, or by one of the gas cycles.

The next generation of systems to follow mercury are the two-stage turboelectric alkali-metal cycles, slated for use in the forthcoming SNAP 50/SPUR (300 kwe to 1 mwe) turboelectric system. The cycle is identical to that of Fig. 2, the mercury cycle, but uses a good nuclear coolant such as lithium in the primary loop with a low vapor-pressure, less corrosive vapor such as potassium in the power loop. Development of the lithium-cooled reactor is now in process, and many potassium, rubidium, and lithium test loops are in existence.

The ultimate alkali-metal turboelectric cycles are of the single-stage type. This configuration eliminates the heat

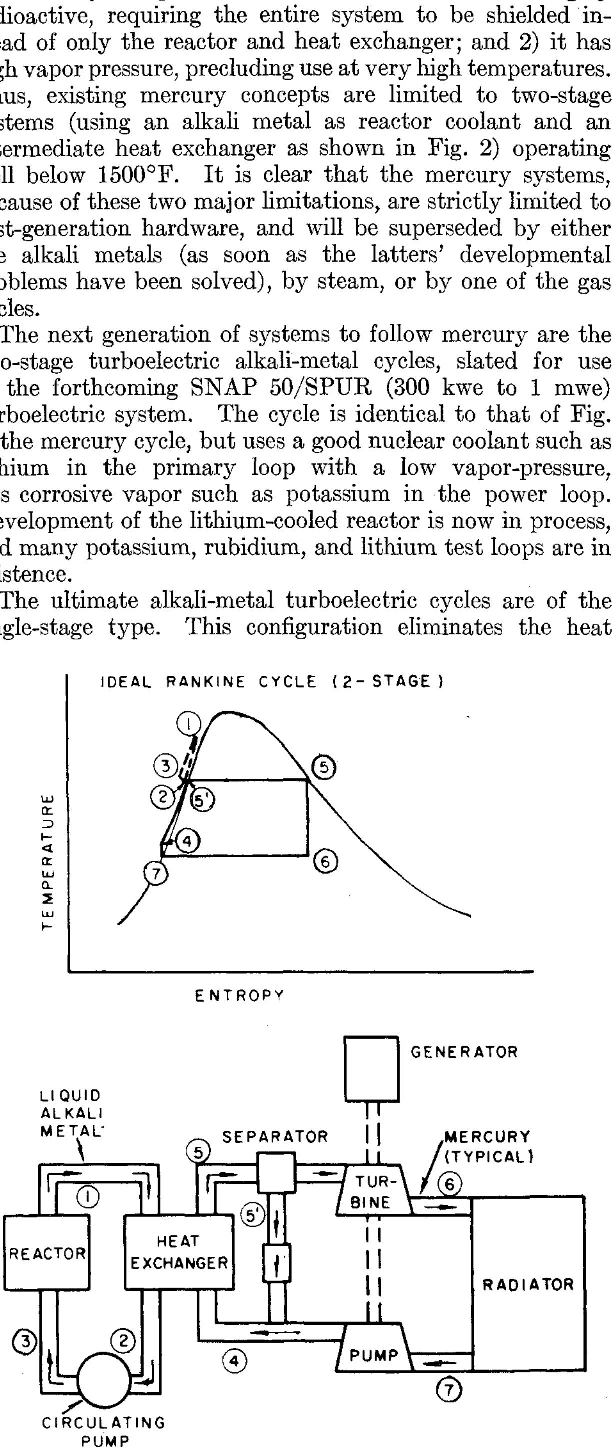

Fig. 2 Turboelectric powerplant utilizing 2-stage liquid-metal cycle.

exchanger of Fig. 2, but requires that the single cycle fluid be compatible with all system components, and that it not become radioactive so as to require shielding. It is not likely that these systems will ever appear, being first superseded by the equivalent single-fluid thermionic powerplant before a comparable turboelectric component state-of-the-art can be attained.

An alternate to the liquid-metal Rankine cycle is the conventional steam (water) Rankine cycle.[7,8] Steam is limited by vapor pressure considerations to rather low cycle temperatures as compared with either gas or liquid-metal systems, so that the radiator area requirements are quite high. However, the combination of steam's high heat capacity and the opportunity of using aluminum construction, with its high fin-to-tube area ratio, provides such low radiater weight per unit area that the specific weight of the steam system is surprisingly low.

Thermionic Systems

Thermionic systems are most conveniently thought of as either "in-pile" or "out-of-pile" systems. The out-of-pile systems are subclassified as "in-radiator" or "in-heat exchanger" systems. In the in-pile configuration, heat supplied to the cathode (electron emitter) surface comes directly from the reactor fuel and heat rejected by the anode (electron collector) is removed by a coolant passing through the reactor. In the out-of-pile systems the reactor coolant now heats the cathode and the rejected heat is either removed by a second fluid or radiated directly to space.

Thermionic cell performance is currently temperature-limited, with typical realistic long-life values of 3200°F at the cathode surface and a corresponding optimum anode temperature of 1400°F.[9] Thus the in-pile systems require reactor coolant temperatures on the order of 1800°F less than the out-of-pile systems for equivalent diode performance. This results in a somewhat different choice of coolants for the in-pile and out-of-pile versions. Inert gases, liquid metals (both subcooled and boiling), and boiling or superheated steam can be considered for in-pile systems. For the out-of-pile applications, the required high temperatures

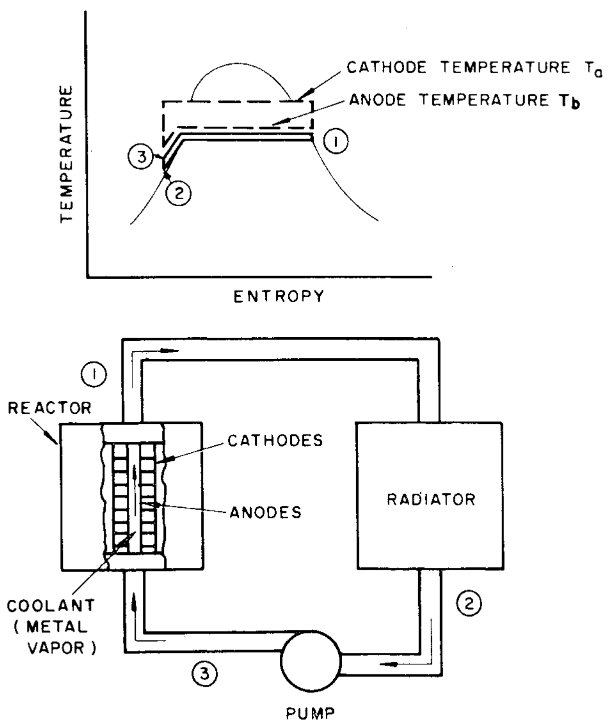

Fig. 3 Single-stage metal-vapor-cooled powerplant utilizing in-pile thermionic elements.

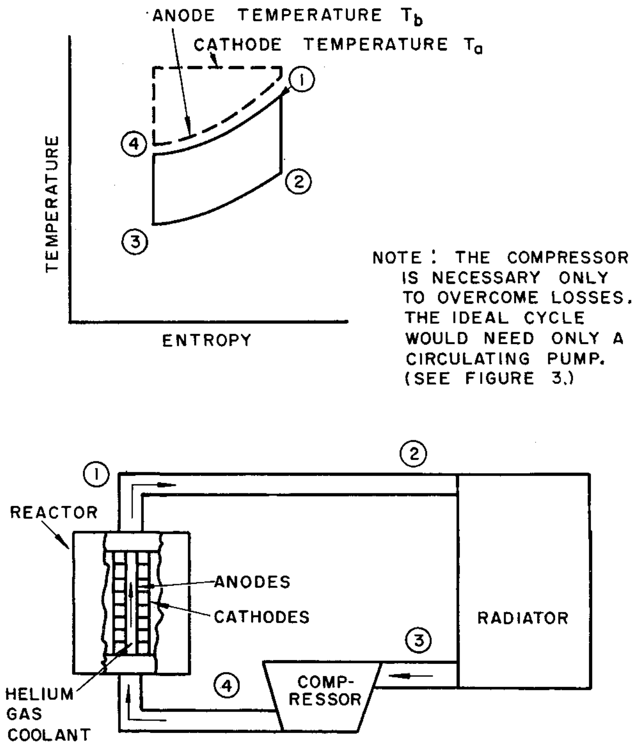

Fig. 4 Closed gas-cycle powerplant utilizing in-pile thermionic elements.

exclude steam and tend to favor inert gases over liquid metals because of their inherent capability for corrosion-free operation at higher temperatures. For the in-heat exchanger systems, boiling water becomes quite attractive as a secondary (anode-cooling) fluid[10] because of the radiator weight advantage just mentioned.

One sees that a great number of choices for diode location and working fluid appear possible. In the present study the analysis has been limited to in-pile gas cycles, out-of-pile gas cycles, and in-pile liquid-metal cycles. Consideration of steam and the various hybrid cycles are reported elsewhere.[8,10]

The liquid-metal in-pile cycle, shown in Fig. 3, has been studied in some detail.[11,12] The configuration of Fig. 3 appears capable of producing somewhat better specific system weights at the same liquid-metal operating temperatures as corresponding turboelectric cycles. However, the serious problems of thermionic cell reliability in the radiation environment, and the complex reactor construction required to contain the cells and their electrical connections, may tend to compromise these results somewhat.

Note that Fig. 3 shows the rather optimistic single-stage, two-phase, liquid-metal cycle. As in the turboelectric systems, however, it is likely that radiation problems will dictate a two-stage system with an intermediate heat exchanger.

The out-of-pile liquid-metal cycle eliminates the problems of in-reactor cell performance and design, but results in a significantly less attractive system because extremely high temperature liquid metals must be used to achieve equivalent system performance, as mentioned previously. A compromise is thus required between system performance and corrosion reliability, resulting in an over-all performance less attractive than that of the in-pile variation. Because the optimization of this particular cycle depends on complex engineering and materials considerations leading to a detailed system design, it is not compared quantitatively with the other cycles discussed in this paper.

In-pile gas cycles (Fig. 4) suffer some of the same problems of cell reliability and reactor construction as the liquid-metal systems. However, use of inert gases minimizes corrosion effects on the anode (as well as on all cycle components) and also may permit the anode surface to serve directly as the coolant channel wall. With liquid metal or steam coolants, an electrical insulator, which must also be a good heat conductor, is required between the anode and the coolant channel.

Another advantage of the thermionic gas systems lies in the fact that the low pressure-ratio turbomachinery of the type required has been developed for operation at temperatures comparable to or higher than those utilized by these cpcles.

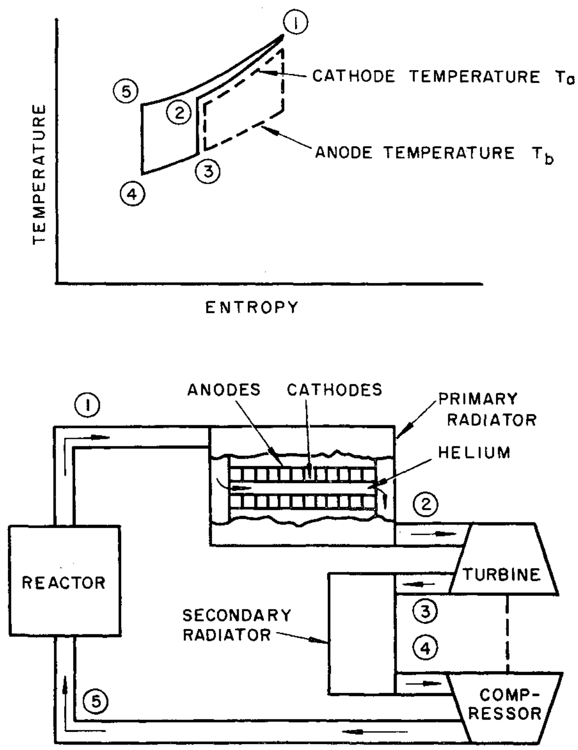

Fig. 5 Split-radiator gas cycle thermionic powerplant.

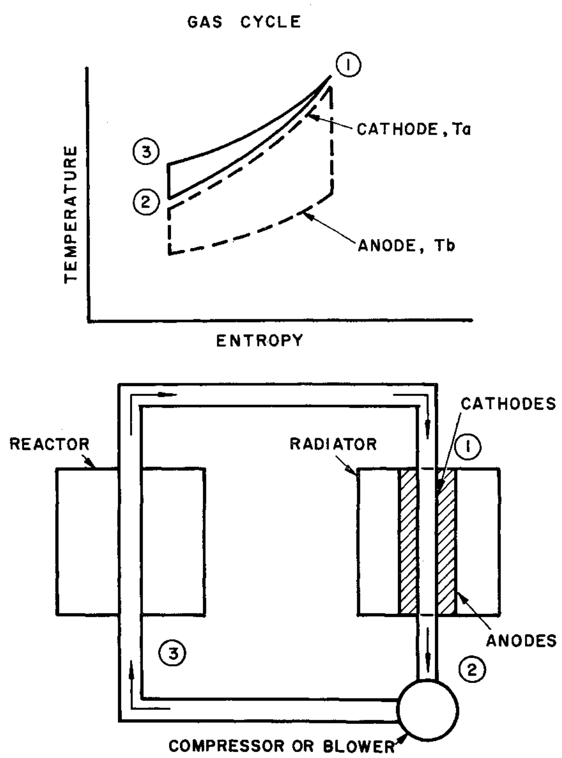

Fig. 6 Gas cycle powerplant utilizing out-of-pile thermionic elements.

Counteracting these beneficial effects of turbomachinery experience, electrical insulation and freedom from corrosion, all thermionic gas cycles suffer from the thermionic cell temperature variation which results from either heating or cooling a flowing gas. This problem can not only cause deterioration of cell efficiency, but also introduces electrical circuit difficulties due to load variation from cell to cell.

In order to avoid some of the problems inherent in the in-pile cycle, while at the same time allowing the thermionic cells to operate near their high optimum cathode temperatures, the cycles of Figs. 5 and 6 are considered. These out-of-pile configurations have the following advantages:

1) Removal of the thermionic cells from the reactor, but still permitting much higher cathode temperatures (~3,600°R) than could be realistically achieved with liquid metals. Removal of the cells from the reactor, of course, has the following advantages:[13] a) fissionable fuel need not be used, permitting cells of much higher efficiency to be constructed; b) radiation environmental difficulties are eliminated; and c) problems of electrical connections and provisions for cell redundancy are enormously simplified.

2) Reduction of turbine inlet temperatures (Fig. 5).

3) Reduction of compressor inlet temperatures, thereby improving cycle efficiency considerably.

4) Improvement of reactor design efficiency by removal of the thermionic elements, thus permitting much smaller core-element-to-gas temperature drop, smaller cores (and therefore smaller shield), and improved controllability. This factor, together with item 1, might be of enough significance to balance out the disadvantageous requirement of higher gas temperatures and specific weight of the in-pile cycle.

Advanced Systems

Three interesting "advanced systems" not considered in detail in the present paper but nevertheless believed worthy of mention for purposes of completeness, are the static powerplant (no moving parts except reactor control elements), the circulating-fuel concepts, and the magnetohydrodynamic generators. These have all been discussed in more or less detail elsewhere, and will be reviewed only briefly here.

The static powerplant concept has been discussed in Refs. 11 and 13, and treated at some length in Refs. 14–16. In principle it takes the form shown in Fig. 7, and although this certainly represents an extreme advancement in state-of-the-art, the static system (one version of which is called STAR-R by General Electric) appears to be ultimately feasible at low power levels; i.e., of the order of 100 kwe.

Because of this power limitation the static powerplant generally has not been considered as a potentially useful device; however, upon introduction of the "multi-engine" concept, the system appears worthy of serious consideration for megawatt ranges in space. Although not yet worthy of detailed study, e.g. Ref. 16, recent advances in high-temperature reactor materials technology[17,18] and thermionic in-pile elements[11,19] make this one of the early "fourth-generation" contenders.

The idea of using circulating fuel systems to increase available temperatures by eliminating the fuel-element-to-coolant drop has been discussed many times (e.g., Refs. 20 and 21), but still appears to be further beyond the state-of-the-art than even the static systems just discussed. Ideas range from conventional reactor-radiator loops using molten fissionable fuel, a system which is highly impractical due to the requirement for shielding of the entire powerplant, to Corliss's scheme of a self-contained dust-fueled reactor with thermionic element cathodes exposed to the fissionable dust, with the anodes serving as the radiator. The latter scheme only multiplies the already difficult problems of the solid-core static reactor of Fig. 7. It is not considered likely that any of the high-temperature circulating fuel devices will be developed for reliable space power systems.

Of all the advanced concepts, the magnetohydrodynamic generator appears most likely for eventual reduction to practice. MHD generators have been operated,[22] although quite inefficiently, and the rapid growth in availability of reliable high-temperature gas sources such as the arcjet or the seeded, gas-cooled reactor is an encouraging factor.

Although there exist a multitude of possible configurations, many directed at ground power stations developing a.c. power (e.g. Ref. 23), the d.c. generator summarized in Ref. 22 is best suited to propulsion applications. The cycle illustrated in Fig. 8 is essentially the same as that of the split-radiator thermionic system of Fig. 6, but has the potential of providing far higher conversion efficiency than is permitted by the limitations of thermionic converters. Possible variations include the arcjet source, which also provides low-specific-impulse propulsion, and the gaseous-core nuclear reactor.[24] The former is quite inefficient for interplanetary missions; in fact, the inverse of an MHD generator, namely an MHD accelerator, is best applied to the arcjet for high-energy missions. The gaseous-core reactor concept is interesting, but is perhaps the furthest from hardware status than any of the other systems discussed in this report.

Note that the suggested cycle of Fig. 8 is that of a gas-cooled (solid) reactor. It might at first appear that a liquid-metal cycle would provide better performance; however, the improved working-fluid conductivity and lower radiator weight of the latter are probably more than offset by the higher generator temperature available in the gas cycle.

Although the MHD cycle represents a system of rather high potential, design analyses are still rather premature at this time. Positive efficiencies have barely been attained, and although MHD hardware has been operaetd for short durations, no long-term tests have yet been attempted. Thus, this concept has not been included in the quantitative comparisons of the following section. Note, however, that the reactor, compressor, turbine, and radiator technology of the other systems are all required for the MHD powerplant as illustrated in Fig. 8.

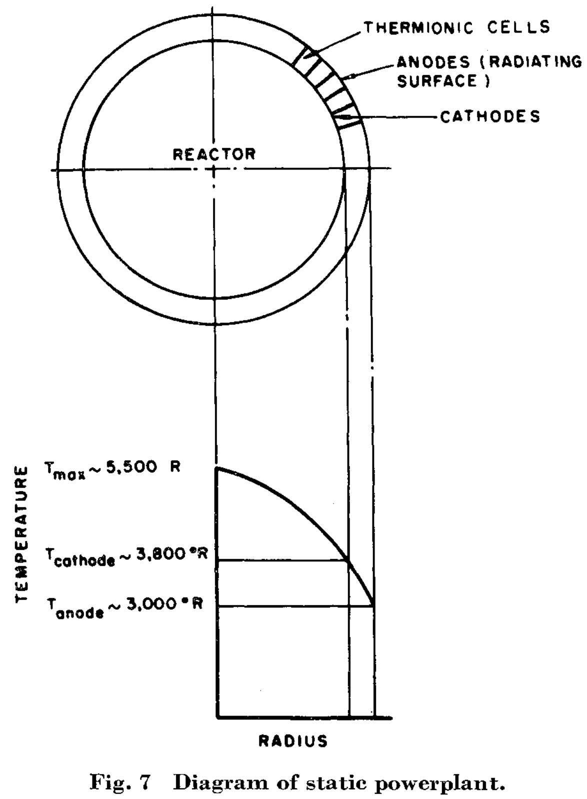

Fig. 7 Diagram of static powerplant.

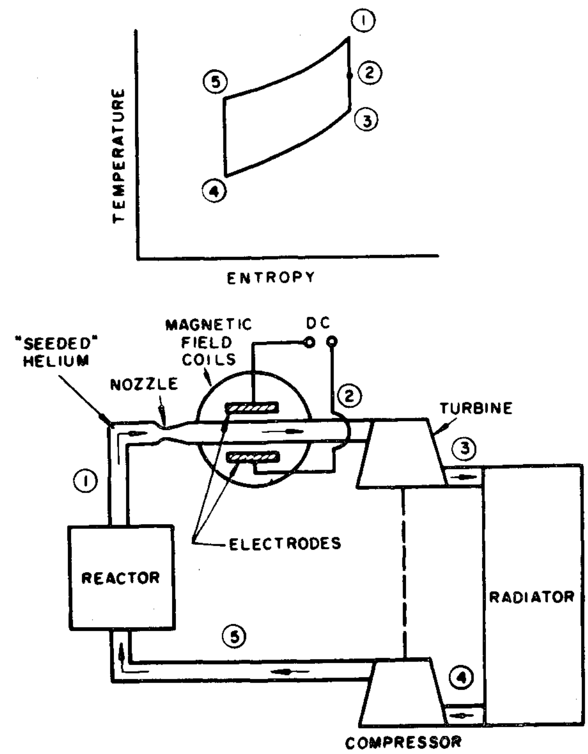

Fig. 8 Closed-cycle magnetohydrodynamic powerplant.

First-Order Performance Analyses

The basic quantitative optimization criterion for space powerplants is the minimization of specific weight. However since this must include not only the reliability requirements for a given system lifetime but also the effect of long-flight duration on payload weight, it is necessary to include the reliability index (or equivalent weight necessary to produce a specified reliability) and the total flight time as parameters in the optimization analyses. These important items, however, are functions of the mission, and cannot be specified adequately until at least a preliminary mission profile has been established. Thus, the present study includes quantitative evaluation of performance only, together with the previously stated qualitative comments concerning reliability.

At the power level of 1 mwe considered in this analysis, the radiator is generally accepted as the dominant weight. For the purposes of a first-order estimate, therefore, the general practice of comparing the various systems in terms of radiator weight alone[5] will be used.

In the cycle analysis of space power systems, use is often made of the well known Carnot-cycle criterion that the optimum sink (radiator) temperature is three-fourths that of the source (reactor) temperature. Blue and Ingold[25] point out that this is an excellent approximation, even for cycles which are very poor Carnot engines, and they have shown further that this approximation is theoretically optimum for a radiation-cooled thermionic converter. In the present study, however, we use the Carnot-cycle criterion only for the liquid-metal turboelectric system. In the case of the gas cycles, pumping power considerations require selection of an optimized relation between temperature ratio and pressure ratio. This is done by straightforward analytical techniques.

The choice of optimum thermionic cell operating temperatures is a much more difficult problem. Rasor and Weeks[26] state that the maximum theoretical thermionic cell efficiency η_{TH} for a cathode temperature T_a and an anode temperature T_b is given by

$$\eta_{TH} = \beta[1 - \gamma(T_b/T_a)]$$

where β and γ are constants obtained from actual cell performance data. Note that if β and γ are unity, this expression is identically that for the Carnot efficiency. Although values for the constants β and γ have been published,[26] they do not appear to agree with present experimental data in the temperature range of interest here. Therefore the empirical thermionic-cell data of Ref. 9 are used throughout the present analysis.

For the liquid-metal cycle of Fig. 2, the required radiator area is given by $\dot{m}(h_6 - h_7) = \sigma\epsilon T_6^4 A_R$.

The net power output Q_e is

$$Q_e = (Q_T - Q_p)\eta_G$$

where

$$Q_T = \text{turbine power} = \dot{m}\eta_T(h_5 - h_6)$$

$$Q_p = \text{pump power} = \dot{m}\eta_p(h_4 - h_7)$$

Thus

$$\frac{A_R}{Q_e} \approx \frac{h_6 - h_7}{\sigma\epsilon\eta_G T_6^4[\eta_T(h_5 - h_6) - \eta_p(h_4 - h_7)]}$$

Since the pump work for an all-liquid medium is negligible,

$$\frac{A_R}{Q_e} \approx \frac{h_6 - h_7}{\sigma\epsilon\eta_G\eta_T T_6^4(h_5 - h_6)}$$

Taking the turbine efficiency η_T at 80%, the conversion equipment efficiency η_G at 95%, and the mean emissivity ϵ at 0.90, the specific radiator area becomes

$$\frac{A_R}{Q_e} = 2.92\,\frac{h_6 - h_7}{h_5 - h_6}\cdot\frac{1}{(T_6/1000)^4},\ \text{ft}^2/\text{kwe}$$

where T_6 is in degrees Rankine.

Using theoretical thermodynamic data for sodium from Ref. 27 and the forementioned criterion that $T_6/T_5 = \frac{3}{4}$, we may calculate the optimization data of Fig. 9 for different turbine inlet temperatures, based on the assumption that radiator surface temperature is equal to fluid temperature in the radiator (i.e., good heat transfer from fluid to tube and through tube wall).

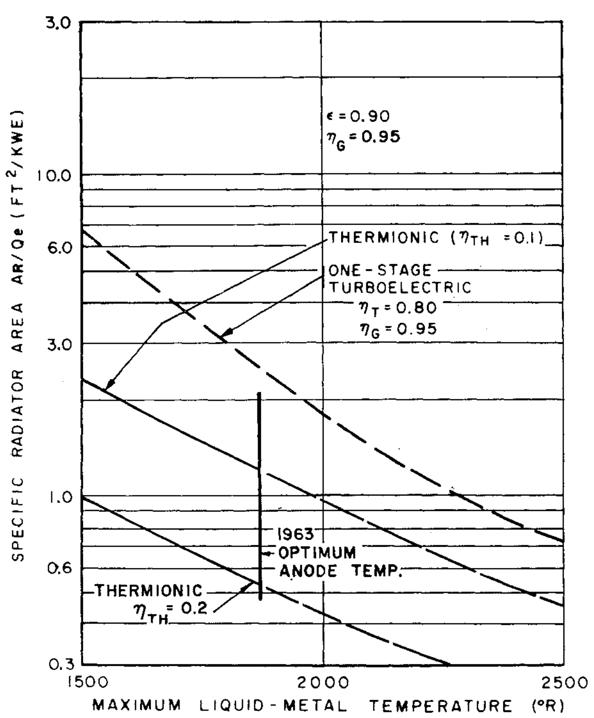

Fig. 9 Comparison of ideal turboelectric and in-pile thermionic liquid-metal cycles.

For the thermionic in-pile alkali metal system (Fig. 3), the required radiator area is given by

$$\dot{m}(h_1 - h_2) = \sigma\epsilon T_1^4 A_R = Q_a - (Q_e/\eta_G)$$

The power output (again neglecting pumping power) is simply $Q_e = \eta_{TH}\eta_G Q_a$. Thus the specific radiator area is

$$\frac{A_R}{Q_e} = \frac{2(1 - \eta_{TH})}{\epsilon\eta_{TH}\eta_G(T_1/1000)^4},\ \frac{\text{ft}^2}{\text{kwe}}$$

Using $T_1 = 1860°\text{R}$, $\epsilon = 0.9$, and $\eta_G = 0.95$, the specific radiator areas are compared with the turboelectric liquid-metal radiator area in Fig. 9 for several typical cell efficiencies.

In this instance, it is likely that the reactor weight will be more significant than in the case of the turboelectric cycle, both because the radiator weight is lower and because of the volumetric inefficiency of the reactor (and associated shield) required by in-pile thermionic elements. This will be discussed later.

Optimization of the gas cycle powerplants is not quite so direct as that of the liquid-metal systems, since the temperatures in the reactor and radiator are not constant, and there will also be a finite pressure drop (small enough to be neglected in the liquid-metal case) in these cycle components.

For the turboelectric (Brayton) gas cycle of Fig. 1, the compressor power may be written

$$Q_c = \frac{\dot{m}c_p}{\eta_c}(T_4 - T_3) = \frac{\dot{m}c_p T_3}{\eta_c}(r_p^{(\gamma-1)/\gamma} - 1)$$

The turbine power is

$$Q_T = \eta_T\dot{m}c_p(T_1 - T_2) = \dot{m}c_p\eta_T T_1\left[1 - \left(\frac{P_2}{P_1}\right)^{(\gamma-1)/\gamma}\right]$$

Also, $P_2 = P_3 + \Delta P_{\text{rad}}$ and $P_1 = P_4 + \Delta P_R$.

Defining

$$\beta \equiv \Delta P_{\text{rad}}/\Delta P_R$$

and

$$\delta_R \equiv \Delta P_R/P_1$$

$$\frac{P_2}{P_1} = \frac{P_3/P_4 + \beta\delta_R}{1 - \delta_R}$$

and the net power output of the cycle (including a conversion efficiency η_G) becomes

$$Q_e = \eta_G\dot{m}c_p\left\{\eta_T T_1\left[1 - \left(\frac{(1/r_p) + \beta\delta_r}{1 - \delta_r}\right)^{(\gamma-1)/\gamma}\right] - \frac{T_3}{\eta_c}(r_p^{(\gamma-1)/\gamma} - 1)\right\}$$

The compressor pressure ratio r_p may now be optimized by maximizing Q_e. Considering all other variables to be reasonably independent of r_p, we set $\partial Q_e/\partial r_p = 0$, which results in a relation between optimum compressor ratio $(r_p)_{\text{opt}}$ and temperature ratio $(T_1/T_3)_{\text{opt}}$

$$\left[\frac{\eta_T\eta_c}{(1 - \delta_R)^{(\gamma-1)/\gamma}}\right]^\gamma\left(\frac{T_1}{T_3}\right)^\gamma_{\text{opt}} = (r_p)_{\text{opt}}^{2\gamma-1}\left[\left(\frac{1}{r_p}\right)_{\text{opt}} + \beta\delta_R\right]$$

In order to determine the optimum combination, we now use the fact that the fluid heat loss in the radiator is equal to the radiated heat loss (assuming ideal heat transfer; i.e., no temperature drop between fluid and radiator surface). Thus, $-\dot{m}c_p dT = \sigma\epsilon T^4 dA_R$ where A_R = radiator surface area. Integrating,

$$A_R = \frac{\dot{m}c_p}{3\sigma\epsilon}\left(\frac{1}{T_3{}^3} - \frac{1}{T_2{}^3}\right) = \frac{\dot{m}c_p}{3\sigma\epsilon}\cdot\frac{1}{T_3{}^3}\left[1 - \left(\frac{T_3}{T_2}\right)^3\right]$$

Now,

$$\frac{T_3}{T_2} = \frac{T_3}{T_1}\frac{T_1}{T_2} = \frac{T_3}{T_1}\left(\frac{P_1}{P_2}\right)^{(\gamma-1)/\gamma}$$

But

$$\frac{P_1}{P_2} = \frac{P_4 - \Delta P_R}{P_3 + \Delta P_{\text{rad}}} = \frac{1 - \delta_R}{(1/r_p) + \beta\delta_R}$$

Thus,

$$A_R = \frac{\dot{m}c_p}{3\sigma\epsilon}\frac{1}{T_3{}^3}\left[1 - \left(\frac{T_3}{T_1}\right)^3\left(\frac{1 - \delta_R}{(1/r_p) + \beta\delta_R}\right)^{3(\gamma-1)/\gamma}\right]$$

Finally,

$$\frac{A_R}{Q_e} = \frac{\dfrac{1}{3\sigma\epsilon\eta_G T_3{}^4}\left[1 - \left(\dfrac{T_3}{T_1}\right)^3\left(\dfrac{1 - \delta_R}{(1/r_p) + \beta\delta_R}\right)^{3(\gamma-1)/\gamma}\right]}{\eta_T\left(\dfrac{T_1}{T_3}\right)\left[1 - \left(\dfrac{(1/r_p) + \beta\delta_R}{1 - \delta_R}\right)^{(\gamma-1)/\gamma}\right] - \dfrac{1}{\eta_c}\left(r_p{}^{(\gamma-1)/\gamma} - 1\right)}$$

We obtain a close approximation to the optimum values of A_R/Q_e by plotting this expression against r_p, where (T_3/T_1) is given by $(T_3/T_1)_{\text{opt}}$ as determined by the relation between $(T_3/T_1)_{\text{opt}}$ and $(r_p)_{\text{opt}}$ derived previously. For $\epsilon = 0.9$, $\eta_G = 0.95$, $\eta_c = 0.85$, $\eta_T = 0.8$, $\delta_R = 0.1$, $\beta = 0.5$, and $\gamma = 1.67$, the optimum r_p is found to be 3 and $(T_1/T_3)_{\text{opt}}$ is found to be 3.6. The resulting specific radiator areas are plotted against peak cycle temperature in Fig. 10.

The direct gas cycle using in-pile thermionic elements, shown in Fig. 4, may be analyzed on the same general basis as that of the corresponding liquid-metal cycle; however, the effects of varying gas temperature and finite reactor and radiator pressure losses must be included as was done in the Brayton cycle.

Since we consider that the turbine is eliminated and the compressor is driven electrically, T_2 is now the same as T_1, and the radiator area just calculated becomes simply

$$A_R = \frac{\dot{m}c_p}{3\sigma\epsilon}\frac{1}{T_3{}^3}\left[1 - \left(\frac{T_3}{T_1}\right)^3\right]$$

To determine the net electrical power output, first write the gross useful electrical power $(Q_e)_g$ as $(Q_e)_g = \eta_G\eta_{TH}Q_a$. The power required to drive the compressor is

$$Q_c = (\dot{m}c_pT_3/\eta_c)(r_p{}^{(\gamma-1)/\gamma} - 1)$$

Thus, the net electrical power is

$$Q_e = (Q_e)_g - Q_c = \eta_G\eta_{TH}Q_a - \frac{T_3\dot{m}c_p}{\eta_c}(r_p{}^{(\gamma-1)/\gamma} - 1)$$

Assuming that the heat lost in the power converter is radiated directly, the power radiated from the radiator, Q_R, is $Q_R = (Q_e/\eta_c)$. Eliminating Q_a, the specific radiator area may be written,

$$\frac{A_R}{Q_e} = \frac{\left[1 - \left(\dfrac{T_3}{T_1}\right)^3\right]\left(\dfrac{1}{\eta_{TH}\eta_G} - \dfrac{1}{\eta_c}\right)}{3\sigma\epsilon T_3{}^4\left[\left(\dfrac{T_1}{T_3} - 1\right) - \dfrac{(r_p{}^{(\gamma-1)/\gamma} - 1)}{\eta_{TH}\eta_G\eta_c}\right]}$$

In this turbineless cycle, r_p now is simply the sum of the pressure drops, (i.e., $r_p = 1 + \delta_R + \beta\delta_R$) and, therefore, is not a strong optimization parameter. The primary quantity to optimize is T_3/T_1, which is accomplished in the usual way by setting $[d(A_R/Q_e)/d(T_3/T_1)] = 0$ and solving for T_3/T_1:

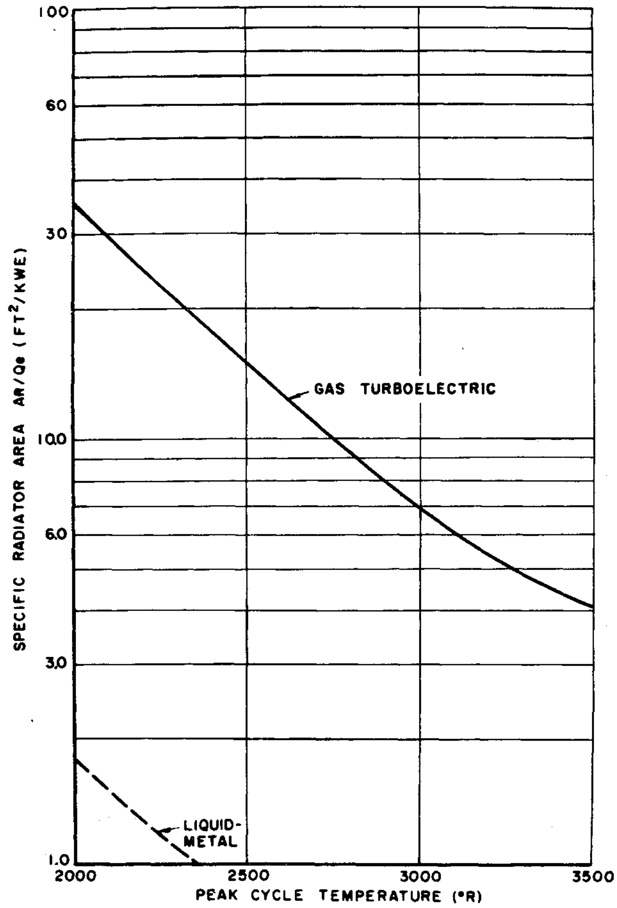

Fig. 10 Effect of peak cycle temperatue on optimum performance of turboelectric gas cycle.

$$\left(\frac{T_3}{T_1}\right)^3 = \frac{r_p{}^{(\gamma-1)/\gamma}}{3\left(\dfrac{T_1}{T_3}\right)\left[1 - \dfrac{T_3}{T_a}\dfrac{(r_p{}^{(\gamma-1)/\gamma} - 1)}{\eta_{TH}\eta_G\eta_c}\right] - 2r_p{}^{(\gamma-)1/\gamma}}$$

It is of interest to note that a single value $T_3/T_1 = 0.65$ was found to be a good approximation to the solution over a wide range of design variables.

It is now necessary to establish values for the cathode temperature T_a, in the reactor, and either the reactor exit temperature T_1 or the radiator exit temperature T_3. We take T_a at its maximum practical value at 3660°R, and because the anode surface temperatures (assumed equal to the gas temperature in the reactor for ideal heat transfer) are not constant, we make the approximation that a mean anode temperature T_b may be defined by $T_b \approx (T_3 + T_1)/2$. Taking T_b at its optimum value of 1860°R, optimum values for A_R/Q_e may be obtained graphically in terms of the cell efficiency η_{TH}. Using previous values for η_G and ϵ, and taking $\eta_c = 0.8$, these optimum values of A_R/Q_e appear in Fig. 11.

Turning now to the out-of-pile split-radiator cycle of Fig. 5, the Brayton-cycle components may be analyzed generally as was just done, except that the primary radiator must now be considered as an additional pressure drop, and, in the simplest version of the cycle, the total turbine power is used only to drive the compressor.

Introducing, therefore, the primary radiator fractional pressure drop

$$\alpha \equiv \frac{\Delta P \text{ in primary radiator}}{\Delta P \text{ in reactor}} = \frac{P_1 - P_2}{P_5 - P_1} \qquad \text{(see Fig. 5)},$$

the previous Brayton cycle analysis provides, with the notation change from that of Fig. 1 to that of Fig. 5, the net output of the Brayton segment of the cycle (which we set equal to zero for the simplest version):

$$(Q_e)_2 = \dot{m}c_p \left\{ \eta_T T_2 \left[1 - \left(\frac{(1/r_p) + \beta\delta_R}{1 - (1+\alpha)\delta_R} \right)^{(\gamma-1)/\gamma} \right] - \frac{T_4}{\eta_c} \left(r_p^{(\gamma-1)/\gamma} - 1 \right) \right\} = 0$$

where, in Fig. 5,

$r_p \equiv$ compressor ratio $= P_5/P_4$

$$\beta \equiv \frac{\Delta P \text{ in secondary radiator}}{\Delta P \text{ in reactor}} = \frac{P_3 - P_4}{P_5 - P_1}$$

$$\delta_R \equiv \text{fractional reactor pressure drop} = \frac{P_5 - P_1}{P_5}$$

and other terms are as defined previously.

Now, the previous Brayton cycle optimization was based on maximization of Q_e. Since $(Q_e)_2$ is zero in the present case, a unique relationship between compressor ratio r_p and Brayton-segment temperature ratio T_2/T_4 may be obtained:

$$\frac{T_2}{T_4} = \frac{1}{\eta_T \eta_c} \frac{r_p^{(\gamma-1)/\gamma} - 1}{1 - \left[\dfrac{(1/r_p) + \beta\delta_R}{1 - (1+\alpha)\delta_R} \right]^{(\gamma-1)/\gamma}}$$

A minimum for T_2/T_4 results, thus specifying the most favorable operating point for the cycle.

The secondary radiator area is calculated exactly as before:

$$(A_R)_2 = \frac{\dot{m}c_p}{3\sigma\epsilon} \frac{1}{T_4^3} \left\{ 1 - \left(\frac{T_4}{T_2} \right)^3 \left[\frac{1 - \delta_R(1+\alpha)}{(1/r_p) + \beta\delta_R} \right]^{3(\gamma-1)/\gamma} \right\}$$

In the case of the primary radiator, however, one must now include the thermionic cell efficiency. The incremental electric power output may be written in terms of primary radiator gas temperature T_a: $dQ_e = \eta_{TH}\eta_G \dot{m}c_p dT_a$, where the cell efficiency η_{TH} has been taken constant at an average value.

Since the waste heat must be rejected by the radiator,

$$(1 - \eta_{TH})\dot{m}c_p dT_a = \sigma\epsilon T_b^4 d(A_R)_1$$

Taking $T_b = T_a - \chi$, where for the temperature ranges of interest χ is approximately constant, this may be integrated,

$$(A_R)_1 = \frac{(1-\eta_{TH})\dot{m}c_p}{3\sigma\epsilon\, T_4^3} \left[\frac{1}{\left(\dfrac{T_2}{T_4} - \dfrac{\chi}{T_4} \right)^3} - \frac{1}{\left(\dfrac{T_1}{T_4} - \dfrac{\chi}{T_4} \right)^3} \right]$$

Since it is desired to minimize the specific radiator area, one obtains $Q_e = \eta_{TH}\eta_G \dot{m}c_p(T_1 - T_2)$ and, therefore,

$$\frac{(A_R)_1 + (A_R)_2}{Q_e} = \frac{1}{3\eta_{TH}\eta_G \left(1 - \dfrac{T_2}{T_1}\right) \sigma\epsilon T_4^3 T_1} \times \left\{ (1-\eta_{TH}) \left[\frac{1}{\left(\dfrac{T_2}{T_4} - \dfrac{\chi}{T_4} \right)^3} - \frac{1}{\left[\left(\dfrac{T_2}{T_4} \right)\left(\dfrac{T_1}{T_2} \right) - \dfrac{\chi}{T_4} \right]^3} \right] + \left[1 - \left(\frac{T_4}{T_2} \right)^3 \left(\frac{1 - \delta_R(1+\alpha)}{\dfrac{1}{r_p} + \beta\,\delta_R} \right)^{3(\gamma-1)/\gamma} \right] \right\}$$

This result is plotted in Figs. 11 and 12, using typical data and values for minimum T_2/T_4 obtained as just discussed.

It may be shown that the fraction of total radiator area required by the turbine-compressor segment of the cycle is a large percentage of the total. It is therefore of interest to examine the performance of the split-radiator cycle *without* the turbine, i.e., using an electric motor to drive the compressor. This is simply a thermionic out-of-pile cycle as shown in Fig. 6.

Using the cycle diagram of Fig. 6, the gross power output is

$$(Q_e)_g = \eta_G \eta_{TH} Q_b = \eta_{TH}\sigma_G \dot{m}c_p(T_1 - T_2)$$

The input power necessary to drive the compressor is

$$Q_c = \frac{\dot{m}c_p}{\eta_c}(T_3 - T_2) = \frac{\dot{m}c_p T_2}{\eta_c}\left(r_p^{(\gamma-1)/\gamma} - 1 \right)$$

However, the power needed to drive the compressor must be supplied from Q_e through an electric motor (assumed 100% efficient). The net power is thus,

$$Q_e = \eta_{TH}\eta_G \dot{m}c_p(T_1 - T_2) - \frac{\dot{m}c_p T_4}{\eta_c}\left(r_p^{(\gamma-1)/\gamma} - 1 \right)$$

The radiator area is still given by,

$$A_R = \frac{(1-\eta_{TH})\dot{m}c_p}{3\sigma\epsilon} \left[\frac{1}{(T_2 - \chi)^3} - \frac{1}{(T_1 - \chi)^3} \right]$$

The specific radiator area is thus,

$$\frac{A_R}{Q_e} = \frac{(1-\eta_{TH}) \left[\dfrac{1}{\left(1 - \dfrac{\chi}{T_2} \right)^3} - \dfrac{1}{\left(\dfrac{T_1}{T_2} - \dfrac{\chi}{T_4} \right)^3} \right]}{3\sigma\epsilon T_2^4 \left[\eta_{TH}\eta_G \left(\dfrac{T_1}{T_2} - r_p^{(\gamma-1)/\gamma} \right) - \dfrac{\left(r_p^{(\gamma-1)/\gamma} - 1 \right)}{\eta_c} \right]}$$

where the compressor pressure ratio is now completely specified as

$$r_p = \frac{P_3}{P_4} = \frac{P_3}{P_1}\frac{P_1}{P_2} \approx 1 + \delta_R + \beta\delta_R = 1.15$$

for the assumed data. This result is plotted in Fig. 11 in which the turbineless cycle is compared with the complete split-radiator cycle at one value for the thermionic cell temperature drop χ and a conversion equipment efficiency η_G of

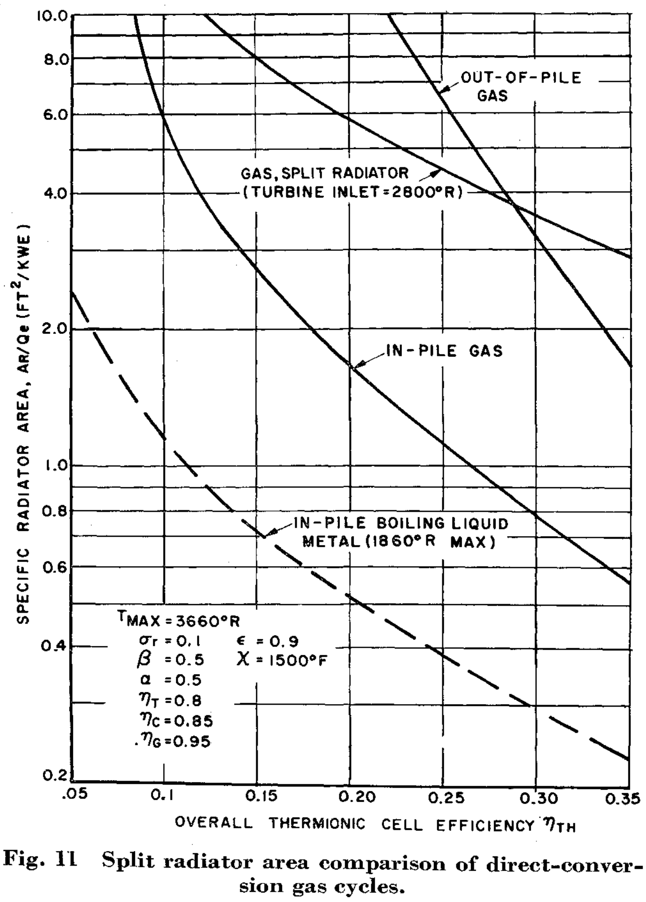

Fig. 11 Split radiator area comparison of direct-conversion gas cycles.

0.95. It is clear that this mode of operation is effective only at very high thermionic cell efficiencies, when the severe penalty for the all-electric compressor drive begins to diminish, or at low turbine inlet temperatures, where the turbine efficiency deteriorates.

Estimation of System Specific Weight

Radiator Weight

The principal performance parameter introduced up to this point is the specific radiator area A_R/Q_e. The purpose of this section is to translate this into terms of specific weight.

Before accurate estimates of radiator weight can be established, there are three factors which must be considered. These are meteoroid damage, redundancy (reliability-weight tradeoff), and heat transfer.

All three of these factors have been considered in prior radiator optimization analyses.[28, 26] However, the task of making analyses of this type with sufficient generality to be applicable to the various systems considered here is far too formidable for the present preliminary study. Since the basic purpose of this paper is the presentation of comparative system potentialities, the optimized values of weight per unit area for metal vapor and gas radiators given in Refs. 29 and 6, respectively, are used directly to convert the calculated specific areas to specific weights. Values for beryllium-armored gas radiators at the 100 kwe power level were given directly in Ref. 6 as 0.35 lb/ft^2 of prime radiating area. Values for the metal vapor condenser-radiator were taken from the beryllium-armored 1 Mwe optimized design of Ref. 29, but were modified to include the updated meteoroid penetration data of Refs. 29–31. The resulting value (calculated as shown in Ref. 10) was approximately 1.8 lb/ft^2 of prime radiating area.

Although the radiator weight is dominant at the one megawatt level and above, it is necessary in any detailed system studies to include weights of the reactor and electrical conversion equipment (turbomachinery or thermionic cells), including power conditioning where necessary. These will, in fact, vary considerably from cycle to cycle; for example, the reactor and shield for an in-pile thermionic system will be far heavier than for the corresponding out-of-pile or turboelectric cycle. Also, the inclusion of thermionic cells in a radiator will increase its weight, and the expected large weight of power conditioning equipment is expected to penalize the thermionic systems somewhat.

Discussion of Results

Results presented in this paper are directed principally toward highlighting the various cycle comparisons, and are not intended as detailed performance data. For example, radiator specific areas and weights are all calculated on the basis of ideal cycles, accounting for losses by means of estimated efficiencies and pressure-drop ratios. Recuperators, which benefit several of the cycles considerably, have not been included in the analyses. Also, radiator heat transfer is assumed ideal in all cases; i.e., the temperature drop from fluid to radiating surface is assumed to be small compared to the temperature level. Finally, only radiator weights are considered, and although they may dominate the system at the one megawatt level and above, inclusion of the other components will certainly affect the results somewhat, as discussed in the previous section.

Quantitative Cycle Comparisons

Quantitative cycle comparisons are summarized here in terms of radiator specific weight, as discussed in previous sections, since the specific area data of Figs. 9–12 do not include effects of the different working fluids. Instead of

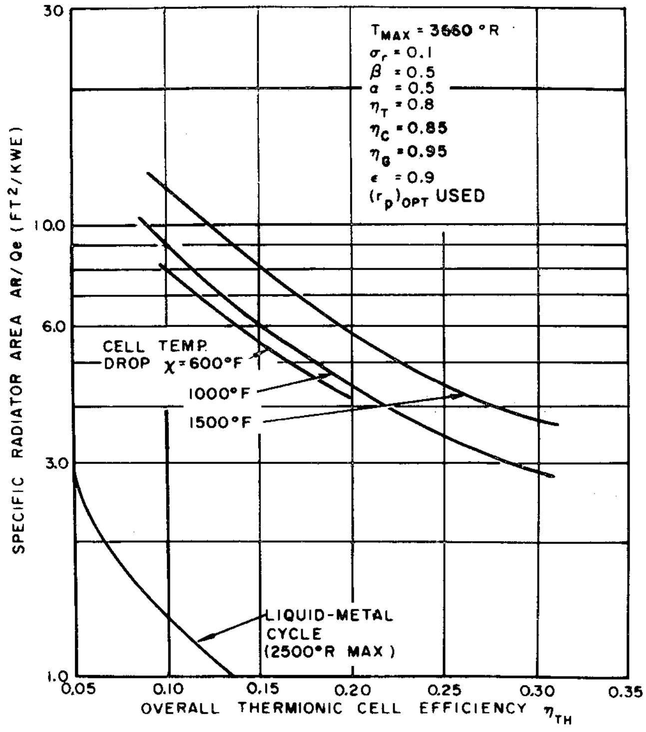

Fig. 12 Performance of split-radiator gas cycle.

simply repeating all data of these figures, however, only the comparisons of major interest will be highlighted.

First, only the maximum temperatures compatible with reasonable extrapolation of current technology are considered here. These are estimated as 1) liquid-metal cycles: 2500°R maximum fluid temperature; and 2) gas cycles: 2800°R fluid temperature at the turbine inlet, 3660°R fluid temperature in the reactor.

Second, the selection of a constant thermionic cell temperature drop of 1500°F was taken as an approximation to the true optimum,[25] which, of course, varies somewhat with temperature.

Using appropriate specific area data from Figs. 9–12, comparative radiator specific weights have been calculated as described earlier, and appear in Fig. 13. Note that a fundamental assumption in all results presented was that of "ideal" heat transfer, i.e., local fluid temperature equal to radiator surface temperature. This idealization favors the gas cycles somewhat, but the use of helium gas at moderately high pressures (see next paragraph) makes this assumption very nearly valid. Detailed heat-transfer analyses are reported in another paper.[10]

Armor thicknesses required for meteoroid protection are sufficiently great that the heat transfer penalty often paid by gas cycles is minimized, since radiator tube strengths resulting from these thicknesses, even at operating temperatures, permit the use of moderately high cycle pressures without failure. It might appear that simultaneous protection against meteoroid penetration and pressure cannot be assumed, but since meteoroid indentations are highly localized, whereas pressure loading is distributed over the entire circumference of the pipe, even deep (but nonpenetrating) individual meteoroid craters will not destroy the pressure-containment effectiveness of the pipe. Of course, as the total number of such craters increases, the over-all strength reduction can become significant, but the probability of an eventual penetration is probably much higher than pressure failure due to excessive numbers of deep but nonpenetrating craters.

The outstanding result of Fig. 13 is that the various gas cycles using thermionics are, with the exception of the out-of-pile gas cycle, superior to the simple Brayton cycle at reasonable values of cell efficiency (15–20%). In the case of the in-pile gas cycle, performance exceeds that of the liquid metal turboelectric system.

Note that considerable improvement in all the thermionic gas cycles can be achieved by "bottoming out" with a steam turboelectric system, as discussed in detail in Ref. 10, to take advantage of the resulting improvement in cycle efficiency and the low weight per unit area of the steam radiator.

Despite the relatively poor performance shown in Fig. 13 for the thermionic out-of-pile cycles, a major advantage of these systems over the in-pile thermionic gas configuration is the reactor simplification and the resulting improvement in reliability. This is a major consideration in these very high-temperature powerplants which must operate for one or more years. Some of the benefits resulting from out-of-pile location of the thermionic cells have been discussed earlier, and in terms of system reliability requirements, these may turn out to dominate the powerplant selection criteria.

The liquid-metal cycles are, of course, very attractive, as is evident from the present research and development activity in this area. In terms of performance, their advantage over the more conventional gas cycles is clear; however, some of the considerations discussed in the next paragraph will have a major effect on the ultimate powerplant selection. Nevertheless, they still represent a major powerplant possibility because of their excellent performance in terms of specific weight.

In weighing the value of high performance (which in the case of space powerplants means specific weight) against less easily defined parameters such as reliability, cost, development, time, etc., no truly quantitative estimates can be made unless a complete mission analysis is performed. However, there are certain general conclusions which can be drawn.

First, for manned flights, it is evident that reliability will be the dominant powerplant characteristic. This implies two possible courses for powerplant design: degrade performance, introduce redundancy or, most likely, a combination of the two.

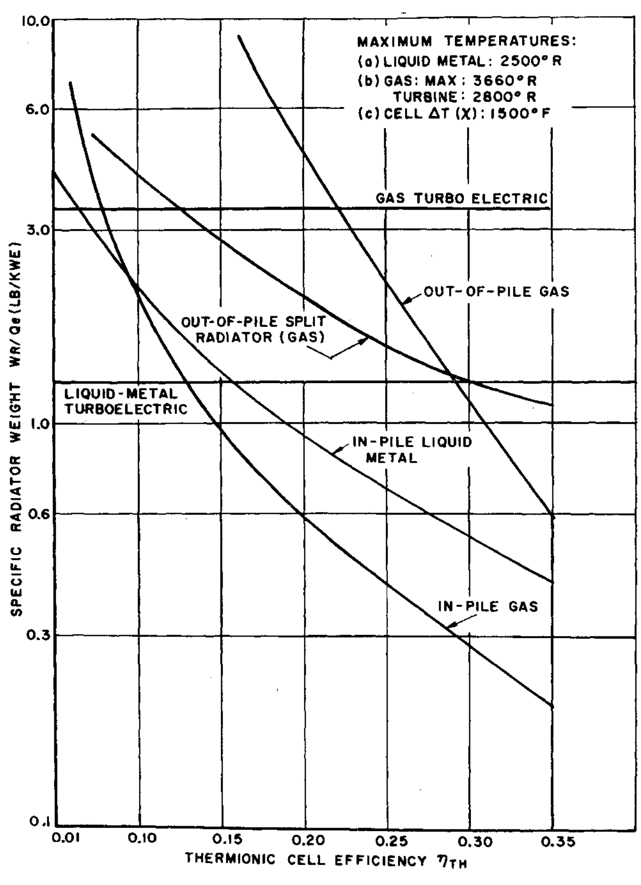

Fig. 13 Specific radiator weight for the various cycles (summary).

The introduction of powerplant redundancy, although not treated in the present report, has at least been considered,[32] and appears to be an effective method for reducing system weight for a specified (high) probability of mission success. Optimization of components by the use of redundancy concepts is an accepted design technique[28] and will certainly be incorporated into any powerplant slated for use on manned missions.

However, as mission times become longer and power levels higher, the effectiveness of redundancy begins to diminish. Although the idea of performance degradation in favor of reliability is sometimes distasteful, it can be useful. For example, although liquid-metal powerplants are superior with regard to performance, long-term operation at the required temperatures and with the required components has not yet been established, even after some ten years of research and development. There are no operational liquid-metal nuclear powerplants in existence at the time of writing, even at the low temperature levels of SNAP-8. On the other hand, gas-cooled systems have had a generally good developmental history. As an example, early in-pile loop experiments[33] forecast performance of the ML-1 reactor.[34] High-temperature gas-cooled reactor technology also is being developed in our nuclear rocket and ramjet programs, and although these are strictly short-term devices, the replacement of hydrogen or air by helium, argon, or neon, with the concomitant reduction in the materials problem, represents a major step toward the development of high-temperature gas-cooled reactors of longer duration. Thus, from the standpoint of long-term operational capability, it would appear that the gas cycles, although generally suffering in comparison to the liquid metals in terms of performance, are nevertheless worthy of major consideration.

One of the most critical elements in the gas cycle, aside from the radiator discussed in detail in this report, is the reactor itself. There are no existing development programs for gas-cooled space-power reactors, principally because of the early poor performance estimates of the direct Brayton cycle discussed earlier. However, there are no basic arguments against gas-cooled reactors; in fact, the current non-space-power technology of this field, as just pointed out, is far more advanced than that of the liquid metals.

In terms of maximum temperature, the gas-cooled systems can certainly operate at much higher temperatures than the liquid-metal powerplants. With regard to heat transfer, the high specific heat of helium, the most likely gas, together with the high cycle pressures made possible by meteoroid armor (discussed earlier), tend to decrease to a significant degree the liquid metals' heat-transfer superiority.

In the consideration of thermionic elements in moderately high-pressure helium gas systems, there exists the strong possibility of leakage into the cells. In order to counteract this problem, should it arise, it will be necessary to change to argon or neon, which would cause some degradation of heat-transfer performance in the smaller reactors needed at low power levels. Unpublished heat-transfer studies[35] show that for the larger systems, argon may be used in place of helium without great penalty.

An operational advantage of the gas cycle is the absence of two-phase flow, which can offer serious problems at zero gravity. This is a problem which has not yet been fully evaluated, and may turn out to be seriously detrimental to the liquid-metal systems. A second operational advantage of gases is elimination of the problem of freezing the working fluid during shutdown or coast periods. Such an eventuality in a liquid-metal system would either cause an aborted mission or require considerable equipment and ingenuity to remelt the fluid.

Although no cost data can be estimated at this preliminary stage in the analysis of these powerplant cycles, the tradeoff between high gas-cycle reliability and high liquid-metal performance will be reflected in over-all system costs. Again, the existence of operational gas-cycle technology tends to favor these systems in terms of development costs.

Conclusions

1) Ideal cycle analyses of various nuclear space powerplants indicated that use of the thermionic diode significantly improves the radiator weight of gas cycles over the pure Brayton cycle. In the case of the in-pile gas cycle, the specific radiator weight is below that of the liquid-metal turboelectric cycle. The in-pile gas cycle has the lowest specific weight of all cycles when the over-all thermionic cell efficiency is greater than 0.13.

2) Nonperformance considerations such as reliability, ease of operation, and development time, tend to favor the gas cycles over liquid metals. The disadvantage of poor heat transfer in gases, not discussed in this report, is minimized by a) use of helium, with its good heat-transfer characteristics, and b) the effect of meteoroid armor in permitting moderately high radiator gas pressures without further weight penalties.

3) Problems associated with the use of in-pile thermionic cells tend to favor out-of-pile thermionic systems or, in some instances, turboelectric systems, despite the performance penalty incurred.

4) An out-of-pile thermionic gas cycle in which the turbine used to drive the compressor is replaced by an electric motor has lower specific weight only for either very high thermionic cell efficiency or low turbine temperature.

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