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SYSTEMS FOR NUCLEAR AUXILIARY
POWER (SNAP) - ISOTOPIC SNAP
PROGRAM (M-3679, 46th. Ed.)

LARGE MILLIWATT RADIOISOTOPE THERMOELECTRIC GENERATOR PROGRAM

PARAMETRIC STUDY DATA

Volume I

General Characteristics of an Advanced, Large Milliwatt RTG

By

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April 1966

Aerojet-General Nucleonics
San Ramon, California

Monsanto Research Corporation
Dayton, Ohio

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AEC Research and Development Report

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AGN - 8182 - 1 (Rev.)

LARGE MILLIWATT RADIOISOTOPE THERMOELECTRIC GENERATOR PROGRAM

PARAMETRIC STUDY DATA

VOLUME I

GENERAL CHARACTERISTICS OF AN ADVANCED, LARGE MILLIWATT RTG

Contract AT(30-1)-3628

April 1966

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PREFACE

The work in the parametric study under Contract AT(30-1)-3628 was performed by personnel from Aerojet-General Nucleonics and Monsanto Research Corporation. The report was prepared by the following individuals from AGN:

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Carl Carney
Ivan Lamb
Don Rockwell
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The work was performed under the supervision of Paul E. Underhill, program manager (AGN); R. A. Cooley, Manager, Systems and Devices (MRC); and Harold Jaffe, Manager, Applied Science Division, AGN.

This report is printed in two volumes for the convenience of the reader: Volume I: General Characteristics of an Advanced, Large Milliwatt RTG; and Volume II: Appendixes.

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LARGE MILLIWATT RADIOISOTOPE THERMOELECTRIC GENERATOR PROGRAM

PARAMETRIC STUDY DATA*

A B S T R A C T

A parametric study was performed to investigate the general characteristics of advanced, large milliwatt, radioisotope thermoelectric generator systems that could be constructed from materials available today. The power level specified was the range from 150 to 1000 mw; three fuel forms were specified; and the operating ambient temperature range from 28 to 90°F. Based on the results of this study, it was concluded that such systems could be designed and fabricated with only modest extensions of state-of-the-art materials and fabrication technology.

This study was performed under Contract AT(30-1)-3628 between the New York Operations Office of the AEC and Aerojet-General Nucleonics with Monsanto Research Corporation. The report is issued in two volumes: Volume I contains the text; Volume II, supporting data.

The several computer programs used in this study, their application to the problems posed, and the results are reported in Volume I. Details of these programs are summarized in Volume II. (The latter volume also includes a tabulation of data on applicable thermal insulations, and a short bibliography on the subject.)

* Published by Aerojet-General Nucleonics, San Ramon, California

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LARGE MILLIWATT RADIOISOTOPE THERMOELECTRIC GENERATOR PROGRAM

PARAMETRIC STUDY DATA

VOLUME I

General Characteristics of an
Advanced, Large Milliwatt RTG

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LARGE MILLIWATT RADIOISOTOPE THERMOELECTRIC GENERATOR PROGRAM

PARAMETRIC STUDY DATA

VOLUME II

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LARGE MILLIWATT RADIOISOTOPE THERMOELECTRIC GENERATOR PROGRAM

PARAMETRIC STUDY DATA

INTRODUCTION

Nuclear energy has provided power for a variety of applications, ranging from bombs and artillery shells to submarines, ice breakers and satellites. Production, in most cases, has been limited in quantity; but relatively little attention has been given to small, silent, nuclear power sources in the power level of household flashlights. These small nuclear power sources would last for years without periodic re-fueling. The study reported in this document shows that technology today stands on the threshold of producing milliwatt radioisotope thermoelectric generator (RTG) systems that would provide the basis for numerous useful devices which one day will become as common as flashlight batteries.

The parametric study summarized in this report was authorized under Contract AT(30-1)-3628 between the New York Operations Office of the AEC and Aerojet-General Nucleonics with Monsanto Research Corporation. The contract specifies an operating ambient temperature range from 28 to 90°F; a storage temperature range from -65 to 160°F, and an operating pressure of 10 to 32 in. of mercury. The study was performed to investigate the general characteristics of advanced, large milliwatt, radioisotope thermoelectric generator (RTG) systems that could be constructed from materials available today. The power levels specified for such systems ranged from 150 to 1000 mw; the designs incorporated three of several fuel forms investigated. The study included metal as well as semi-conductor elements independently in several configurations.

The technical approach to the evaluation of each engineering parameter is discussed in Section I. To demonstrate the most efficient methods of using the design information, a complete analysis of the design problems associated with large milliwatt generators is reported in that section.

Sections II through VII each explains a major parameter of an RTG system discussed in Section I, and defines the anticipated problem areas related to that parameter. Each discussion is based on the general case of using the d-c output of the device to trickle charge a battery. The final

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section (VIII: "Sample Use of Parametric Charts") summarizes the procedure for using the data generated in this study, and lists the charts as they pertain to each heat source material. A large body of appendix material has been included in this report to provide supplementary and supporting data. Tables and figures are numbered according to the section in which they appear; the appendixes (Volume II) are identified with the section in which they are cited first. References are presented in footnotes.

This report is printed in two volumes. Volume I presents the text of the report in sufficient detail to inform the general reader; in Volume II, the appendix material is presented in considerable detail.

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I. ANALYSIS OF LARGE MILLIWATT GENERATOR SYSTEMS

A parametric study was conducted to investigate the design characteristics of advanced, large milliwatt, radioisotope, thermoelectric generator systems. There are five major components in an RTG system:

- 1) The thermoelectric module
- 2) The isotope heat source
- 3) The radiation shielding
- 4) The thermal insulation
- 5) The power conditioner

The type and amount of materials used in each component effect the thermal management and, therefore, the system efficiency. Each component can be fabricated from one of several materials and/or in one of several physical arrangements; therefore, to meet the design objective of an RTG system, the constraints of the environment in which it is to be used must be examined. These environmental factors include temperature, pressure, volume available, weight permitted, heat sink availability, shock and vibration anticipated, life expectancy required, allowable cost, and so on.

In performing this study, generators incorporating strontium-90 titanate, promethium-147 oxide, and plutonium-238 oxide were evaluated at net electrical power outputs of 150, 300, 600 and 1000 mw. Evaluation of other chemical forms of these basic fuels was reserved for a more detailed analysis during the conceptual design phase of this program to confine the number of parametric variables to a manageable size.

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Sufficient shielding was used in the design to limit the radiation dose rate to ICC shipping requirements (i.e., 200 mr/hr on the surface, and 10 mr/hr at 1 meter from the surface). In addition, Sr-90 systems were investigated for use without shielding and with half-thickness shielding.

The parametric study also included evaluation of proven thermoelectric materials and state-of-the-art, as well as developmental, thermoelectric configurations. Non-segmented couples (consisting of N- and P-type MCC 40, N- and P-type silicon-germanium, 2N/2P and 3N/3P lead telluride, bismuth telluride, and Cupron Special/Tophel Special) were investigated. Cascaded assemblies of 2N/2P lead telluride/bismuth telluride and silicon-germanium/2N/2P lead telluride semiconductor materials were also evaluated.

The significant design and operating parameters were varied over a wide range to establish optimum generator characteristics and design limitations. The major parametric results include thermal efficiency, generator size and weight, costs, and detailed data on thermoelectric module design.

A complete section of this report is devoted to each of the five major components (see above) to familiarize the reader with the many parameters applicable to each, and their effect upon the engineering and economic trade-offs involved in designing an RTG system. The detailed information in each of these sections also establishes the constants and input data for the computer programs.

There are two more sections: one discusses the reliability aspects of the design; the final section summarizes the procedure for using the data generated in this study.

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A. ANALYTICAL APPROACH

The analytical techniques developed in the design of such a system obviously should obviate subsequent invalidation by anticipated advancements in the state-of-the-art. This requirement, coupled with the sizable number of variables required to evaluate and characterize the generator systems specified, presents a difficult task. Rather than compromise the breadth and validity of the study by limiting the number of variables (a step that would, in turn, require the inclusion of many assumptions), an analytical technique was employed which is particularly suited to milliwatt generators.

The analytical technique first divided the RTG system into two subsystems (Figure I-1): the electrical system (thermoelectric module and power conditioner, if needed), and the thermal system (radioisotope fuel capsule, shielding, insulation and structure). The parametric analyses of each of these subsystems were conducted independently of each other. This approach is feasible at low power levels because the only constraint imposed upon the thermal system is the thermal conversion efficiency of the electrical system. In other words, normal interface constraints (surface area of available heat source and allowable heat flux) are not restrictive in the milliwatt power range for the radioisotope fuels specified for this study. The two parametric studies are coupled into a description of the complete system by a bridging procedure. As is shown later in the report, this technique permits systematic coverage of a large number of variables and provides for evaluation of the effects of improvements in thermoelectric technology on the design and performance of a generator.

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FIGURE I-1. SCHEMATIC OF THERMOELECTRIC GENERATOR SYSTEM

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B. THERMOELECTRIC MODULES

The power range for this study extends through a region in which thermoelectric elements of large length-to-area (L/A) ratios are required for optimum matching with the electrical load. These large L/A ratios are more readily handled with metallic thermoelectric elements, but the conversion efficiency of such thermoelectric elements is quite low, a particularly undesirable trait in view of the cost of radioisotope fuel; in addition, both the weight and volume of thermoelectric metallic thermocouple materials are excessive at the power levels considered in this study (150 to 1000 mw). The radioisotope cost factor thus increases the incentive to use the more efficient semiconductor thermoelectric materials.

Since characteristics and performance of generators in the milliwatt power regime are especially sensitive to thermoelectric materials, a survey was conducted to establish the current and projected state-of-the-art in the fabrication of such materials. In addition, data on properties, performance, compatibility, cost and reliability were reviewed with leading suppliers of thermoelectric materials, thus establishing a sound basis for the analysis.

The results of the survey showed that bismuth telluride can be fabricated today with L/A ratios applicable to the large milliwatt power regimes; and that similar applications of lead telluride 2N/2P and 3N/3P are promising as far as fabrication is concerned, although the development of thermoelectric modules is most advanced for bismuth telluride. On the basis of these results, it can be concluded that the analysis and design of the conceptual system thus should proceed, and provisions should be made in planning the project to utilize any significant advances in thermoelectric technology that may be applicable to the milliwatt power range.

Three thermoelectric optimization computer programs were developed at Monsanto Research Corporation (MRC) to evaluate non-segmented, segmented, and cascaded configurations of thermoelectric modules. (Descriptions of these

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programs are discussed in Section II.) The input data, parametric variables and results of these programs (summarized in Table I-1) illustrate their versatility. The calculated overall performance of the thermoelectric module accounts for power conditioning unit losses (where applicable), parasitic conduction losses, time-dependent degradation of thermoelectric properties, and electrical contact losses.

The primary parametric results are reported in the form of thermoelectric performance maps for the semiconductor and metallic materials (Figure I-2). That Figure shows the relationship of hot junction temperature, load voltage, and power level to module efficiency, number of couples, and L/A ratio. The variation in module efficiency at constant hot junction temperature with a change in the number of couples (or, equivalently, output voltage) for the semiconductor modules is due to the dependency of power conditioner efficiency on voltage.

In addition to these basic performance maps, detailed data on practical dimensions and cost are presented as separate tables or graphical summaries which complete the description of the thermoelectric module. The thermoelectric performance maps, however, provide the basic data required to couple the electrical system with the thermal system and completely characterize the radioisotope generator.

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Table I-1SUMMARY OF THERMOELECTRIC OPTIMIZATION COMPUTER PROGRAM CAPABILITY

<u>Input Data</u>	<u>Parametric Variables</u>	<u>Computer Results</u>
Mission time	New power output	Volume and weight
Couple failure rate	Thermoelectric materials	Cost
Allowable couple failures	Configuration (segmented, etc.)	Element length
Power conditioner efficiency	Hot junction temperature	By-pass heat loss
Thermoelectric properties	Cold junction temperature	Element cross section
Density of materials	Electrical mismatch ratio	Current
Degradation	Output voltage	Generator efficiency
Cost factors	Circuitry (series, parallel)	Couple figure of merit
	Heat flux density	Couple thermodynamic efficiency
		Generator heated area
		Generator power
		Generator reliability
		Ave. Seebeck coefficient
		Ave. resistivity
		Ave. thermal conductivity
		L/A ratio
		Couple internal resistance
		Module internal resistance
		Number of couples

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*Includes power conditioner efficiency and is equivalent to overall efficiency of electrical system.

FIGURE I-2a. SEMICONDUCTOR MATERIALS

FIGURE I-2b. METALLIC MATERIALS

FIGURE I-2. TYPICAL THERMOELECTRIC PERFORMANCE MAPS

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C. ISOTOPE HEAT SOURCES

Three radioisotope fuels were investigated: strontium-90 titanate, promethium-147 oxide, and plutonium-238 oxide. (Additional fuel forms will be investigated in the conceptual design study for any case in which improved performance can be obtained and/or operating temperatures permit; such fuels include plutonium and promethium metal, strontium fluoride, and strontium oxide.) A special sub-routine for capsule design was incorporated into TEHE (Thermoelectric Heater Evaluation) for plutonium fuel forms (Appendix I-A). This subroutine balances the void volume required to accommodate the buildup of helium pressure as the isotope decays against the thickness of the capsule wall, thus achieving minimum overall capsule weight and near minimum capsule volume. For strontium and promethium fuel forms, sufficient thickness of Hastelloy C capsule wall is applied to satisfy compatibility and safety requirements.

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D. RADIATION SHIELDING

Data on depleted uranium was input into the parametric computer code to obtain minimum shielding size and weight. In cases where cost overrides weight and size considerations, lead can be used for the shielding. Each specific application must be scrutinized from all points of view before making the final engineering compromises. Methods of shielding various isotopes are summarized in Section IV; this summary provides the design engineer with a feeling for the mass of materials needed in the design, a mass that must also be compatible with thermal management of the system, and must be supported to meet the shock and vibration requirements of the mission.

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E. THERMAL INSULATION

The cost of radioisotope energy increases the incentive to use high performance thermal insulation, thus absolutely minimizing the parasitic heat loss from the generator. The unique criteria for the use of thermal insulation with milliwatt generators are related to radiation damage resistance and non-evolution of gaseous dissociation products. The thermal insulations useful for milliwatt systems quickly narrow to MIN-K-1301 or MIN-K-503, with voids evacuated and backfilled with xenon. The Super Linde systems hold some promise for missions of 18 months if the vacuum (10^{-3} mm Hg) can be maintained. A paramount factor in limiting lifetime for such insulation systems is the outgassing of encapsulation materials.

Additional factors of importance in selecting the optimum insulation system include the shock and vibration attenuation characteristics of the final configuration of the system as well as maintenance of integrity after exposure to shock and vibration. A description of many insulating materials is given in Section V with a tabulation of their mechanical and thermal properties.

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F. POWER CONDITIONERS

A good universal power conditioner would simplify the design problems of a thermoelectric generator system; unfortunately, such a system has yet to be devised. The minimum voltage required of semiconductors limits circuit design; the efficiency of conditioning decreases with decreasing input voltage; temperatures significantly above room temperature decrease operational lifetime; and radiation damages the materials.

The power conditioners for the milliwatt systems pose a serious reliability problem for the electronic design engineer. Most of the components needed in such circuits are available, but it is difficult to select the most long-lived units. Since the reliability of the circuit is a direct function of the reliability of each component, a reliability test program is needed to obtain data for use in selecting components with lifetimes of $1\frac{1}{2}$, 5 and 10 yr. The problems involved in selection of a power conditioner design and related components are discussed in Section VI along with some suggested solutions.

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G. EVALUATION OF THE RADIOISOTOPE GENERATOR

While the initial investigations were proceeding into the five major components of the RTG system, a computer program was being written for evaluation and optimization of the complete radioisotope generator system. The application of, and results from, the computer program are discussed in detail here to illustrate the procedure followed in a complete analysis.

A "Thermoelectric Heater Evaluation" program was written in FORTRAN IV for the IBM-7094 for use in this study. (The program, TEHE, is described in detail in Appendix I-A.) The input data, parametric variables, and output data are summarized in Table I-2. The overall generator efficiency includes accounting for the efficiency of the electrical system as well as parasitic thermal insulation and structural heat losses.

The physical model used for system analyses, illustrated in Figure I-3, is based on cylindrical geometry. The parametric study was limited to an internal shield configuration based on the use of depleted uranium where it would minimize system weight. The conceptual design study will include an investigation of external and split-shield configurations, as well as other shield materials, to facilitate a cost/effectiveness evaluation.

Two insulation materials were investigated which are typical of the best fibrous and vacuum type materials, viz., MIN-K-1301 (xenon-filled) and Super Linde Insulation, respectively.

As a general case, all of the designs discussed in this report are based on the use of the d-c output to trickle charge a battery. The procedure for using the data generated for this study is outlined in Section VIII in step-by-step fashion.

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Table I-2

SUMMARY OF THERMOELECTRIC HEATER EVALUATION COMPUTER PROGRAM CAPABILITY

<u>Input Data</u>	<u>Parametric Variables</u>	<u>Computer Results</u>
Mission life	New power output	Overall generator efficiency
Fuel properties	Radioisotopic fuel	Heater efficiency
Shield properties	Shield material	Thermal losses
Insulation properties	Thermal insulation	Optimum hot junction temp.
Structural material properties	Overall diameter	Minimum diameter
	Hot junction temp.	Component dimensions
Capsule design spec.	Cold junction temp.	Components weights
Cost factors	Thermoelectric module type shield location	Costs
	Fuel capsule L/D ratio	

The capsule length-to-diameter (L/D) ratio was kept equal to 1.2 for all fuels throughout the parametric study and corresponds closely to the optimum condition for minimum overall system weight and maximum thermal efficiency (see Appendix I-B). A cold junction temperature of 170°F was assumed throughout the study and is an average value which is typical for systems in this power range where surrounded by still air at 70°F at standard atmospheric pressure (see Appendix I-C).

Since TEHE analyses the generator system independent of a specific thermoelectric module, the electrical system efficiency must be treated as a parametric variable covering the range of expected thermoelectric performance. It has been shown that the performance of the actual electrical system (thermoelectric module and power conditioner, where required) can be accurately described using a pseudo (effective) figure of merit and the form

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FIGURE I-3. SYSTEM ANALYTICAL MODEL - CYLINDRICAL GEOMETRY

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of the conventional equation for thermoelectric conversion efficiency under matched load conditions to define its temperature dependency, i.e.,

$$\eta_E = \frac{T_H - T_C}{2T_H + \frac{4}{Z_E} - \frac{T_H - T_C}{2}} \quad (1)$$

where η_E = electrical system efficiency
 T_H = hot junction temperature, $^{\circ}\text{R}$
 T_C = cold junction temperature, $^{\circ}\text{R}$
 Z_E = effective figure of merit, OR^{-1}

Figure I-4 is a set of curves calculated from Equation 1 with an effective figure of merit range of $0.1 \times 10^{-3}/^{\circ}\text{C}$ to $0.9 \times 10^{-3}/^{\circ}\text{C}$ for a constant cold junction temperature of 170°F . Actual electrical system performance curves are superimposed to illustrate the close match obtained when using a characteristic effective figure of merit. For general purposes, it may then be assumed that the electrical system efficiency for the various thermoelectric materials is approximately characterized by the following effective figures of merit:

<u>Thermoelectric Module</u>	<u>$Z_E \times 10^3/^{\circ}\text{C}$</u>
Cupron/Tophel Special and MCC-40 (non-segmented)	0.10
Silicon-Germanium Alloy (non-segmented)	0.30
Lead Telluride (3N/3P) (non-segmented)	0.40
Lead Telluride (2N/2P) and Silicon-Germanium alloy (cascaded)	0.40
Lead Telluride (2N/2P) (non-segmented)	0.55
Lead Telluride (2N/2P) and Bismuth Telluride (cascaded)	0.60
Bismuth Telluride (non-segmented) at low temperatures	0.90

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FIGURE I-4.. TEHE BRIDGING CURVES

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Figure I-4, the bridging curves, provides the basis for matching the design characteristics and performance of a radioisotope generator system in the TEHE parametric study to a specific type electrical system (thermoelectric module). That is, the performance of a specific thermoelectric module design can be overlayed on the bridging curves to determine its characteristic effective figure of merit, Z_E . The format of the primary TEHE parametric results is illustrated in Figure I-5. Detailed dimensions, component weights, temperatures, and costs are included in the computer results. The dotted portion of the efficiency curves in Figure I-5 denotes the critical overall system diameter for a 3-volt-system, i.e., the minimum diameter at which the design net electrical power output can be obtained. Each point on the curves corresponds to the optimum hot junction temperature for maximum overall efficiency. This temperature increases as the diameter increases since increasingly larger thicknesses of thermal insulation are applied. Figure I-6 has superimposed isotherms on a system performance map which show this optimum temperature trend.

The results of the TEHE parametric study are shown in Appendix I-D. (A listing of the figures and titles is given at the beginning of that appendix.) In some cases the optimum temperature exceeds the maximum temperature capabilities of bismuth telluride ($Z_E = 0.90 \times 10^{-3}/^{\circ}\text{C}$). The 0.9 curves are therefore considered to represent advanced thermoelectric materials technology. Some of the figures therefore are limited to a maximum hot junction temperature of 400°F , and specifically represent state-of-the-art performance capability for bismuth telluride. The thermoelectrics have finite temperature limitations (that for bismuth telluride is from 400 to 500°F) and therefore some of the figures represent advanced system concepts. The efficiency of a bismuth telluride system operating at 400°F is accurately reflected by the plots found in Appendix I-E.

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Note: Dotted lines (-----) show intersect for 3 volt system. Actual curves are published in Appendix I-D

FIGURE I-5. TEHE PARAMETRIC STUDY FORMAT

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FIGURE I-6. OPTIMUM HOT JUNCTION TEMPERATURE DISTRIBUTION

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A summary of the minimum overall diameters for the systems investigated is plotted in Figure I-7. The 4.5 in. and 52 lb limits specified for the study are indicated on the figure and clearly reveal which systems meet these design criteria.

Fuel cost data are plotted in Appendix I-E, representing the cost for the most efficient operating temperature (as discussed previously). The curves for fuel costs were based on the costs shown in Table I-3.

Table I-3

Present and Future Fuel Costs

Fuel	Present Cost, \$/w(t)	Future Cost, \$/w(t)
Pm-147	560	93
Sr-90	32	19
Pu-238	not available	560

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FIGURE I-7. MINIMUM SYSTEM OVERALL DIAMETERS

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II. THERMOELECTRIC DATA

A. DESIGN ANALYSIS OF THERMOELECTRIC GENERATOR SYSTEM OPERATING AT MILLIWATT POWER LEVELS

Thermoelectric generator systems operating at milliwatt power levels can be built in many different ways, using various thermoelectric materials or combinations of various thermoelectric materials, and for a great variety of operating conditions. This section gives:

- 1) A short description of the procedure followed in designing milliwatt-power-level thermoelectric generator modules
- 2) A list of the assumptions made and comments upon the validity range of these assumptions
- 3) A summary of the performance parameters that were determined for the modules

A detailed description of the design analysis and the computer codes developed in the design analysis for the thermoelectric generators with various utilization modes of the thermoelectric materials is given in the Appendixes II-A, II-B, II-C, and II-D.

- Appendix II-A discusses the design analysis and computer code for thermoelectric generators that utilize non-segmented thermoelectric elements at milliwatt power levels
- Appendix II-B discusses the design analysis and computer code for thermoelectric generators that utilize segmented thermoelectric elements at milliwatt power levels

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- Appendix II-C discusses the design analysis and computer code for thermoelectric generators that utilize cascaded thermoelements at milliwatt power levels
- Appendix II-D describes an analytical procedure for determining the reliability of various generator circuit configurations and a computer code based thereon. This computer code has been incorporated in the form of a subroutine in the computer codes of Appendixes II-A, II-B, and II-C.

Using the analytical design procedures outlined in these Appendixes, milliwatt generator modules were sized for four power levels:

- PSYST = 150 mW
- PSYST = 300 mW
- PSYST = 600 mW
- PSYST = 1000 mW

The materials considered were:

- Bi_2Te_3 N/P (ASARCO)
- PbTe 2N/2P (3M Company)
- PbTe 3N/3P (3M Company)
- MCC 40 N/P (Monsanto Research Corporation)
- SiGe N/P (Radio Corporation of America)
- Cupron Special/Tophel Special (Wilbur B. Driver)

It was decided early in the program not to investigate the Platinel II (Engelhard Industries) and Chromel/Constantan (Hoskins) systems since the results of hand calculations showed that the length of thermocouple wire needed was incompatible with the maximum dimensions of the total system as specified in the work statement of the contract.

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The thermoelectric generator modules were designed for a wide range of system variables. The system variables and the ranges of their values are

- TH = hot junction temperature, °F. TH ranges from TC + 90°F to the maximum operating temperature for the material being considered
- TC = cold junction temperature, °F. The maximum operating temperatures considered were:
 - a) 450°F for Bi₂Te₃
 - b) 1000°F for PbTe 2N/2P
 - c) 1000°F for PbTe 3N/3P
 - d) 1700°F for SiGe N/P
 - e) 1700°F for MCC 40 N/P
 - f) 900°F for Cupron/Tophel Special
- QH = heat flux density at the hot junctions of the thermoelectric couples, watts/cm²; QH = 2 to 20 w/cm² to ensure compatibility of the module with all the encapsulated fuels being considered.
- VLD = load voltage, volts, VLD = 0.5 to 18v
(Note: VLD is the voltage seen by the power conditioning equipment which converts VLD to 18v. The wire-type thermoelectric generators were investigated for VLD = 18v since it presents no problem to design these generators for high voltages without using power conditioning.)

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- **PARC** = variable indicating the number of parallel circuits or the couple redundancy in the generator.
PARC = 1 indicates that the generator has all its couples in series. **PARC** = 2 indicates that the generator has two parallel circuits (wire-type generator) or two cross-connected series-parallel circuits i.e., one-fold couple redundancy (semiconductor-type generator).
- **RMIS** = mismatch ratio of external load to internal resistance of generator. **RMIS** = 1 for the semiconductor type generator; **RMIS** = 1 to 2 for the wire type generators.

Part of the assumptions made in the analytical investigations of milliwatt generator systems are listed in Appendixes II-A through II-D. Additional assumptions made are

- 1) The efficiency of the power conditioning equipment is given by the data-based, curve-fitting equation

$$\text{ETAPC} = \text{EXP}(-0.323 \cdot \text{VLD}^{-0.553})$$

- 2) The power degradation rate **EMUC** is constant over the mission length **TIME**, and that consequently the generator must be designed for an initial power output of

$$\frac{1}{1 - \text{EMUC} \cdot \text{TIME}}$$

times the power required at the end of the mission. **EMUC** was assumed to be 0.005 per 1,000 hr. This value is, according to MRC's experience, approximately equal to the average degradation rate for PbTe 2N/2P and Bi₂Te₃ N/P at 900 and 400° F, respectively. It is a somewhat conservative value for the other semiconductor materials and a very conservative value for the wire materials.

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3) The electrical contact losses are independent of time, hot and cold junction temperature, and couple geometry; and can be accounted for by a constant fraction of the gross power generated. The contact loss parameter COLPA is so defined that multiplication of the net electrical power required by $\frac{1}{\text{COLPA}}$ yields the gross electrical power requirement. COLPA = 0.75 for the semiconductor modules. This estimate is based upon MRC's experience with PbTe 2N/2P modules. COLPA = 0.75 is a conservative estimate for all other semiconductor materials. For wire-type generators: COLPA = 0.85. It is believed that this estimate is conservative.

4) The packing density PADC of the thermoelectric material in the module is constant. Assumed was PADC = 0.5 for all generators. The space around the modules was assumed to be filled with a thermal insulation system with an effective thermal conductivity THINS (THINS = 0.02 Btu/hr-ft-[°]F for semiconductor-type modules, THINS = 0.01 for wire-type modules). The value for PADC used should yield a conservative estimate of the system efficiency, since it is expected that modules are to be able to survive shock and vibration conditions, and therefore must be built with packing densities larger than 0.5.

5) For the cascaded thermoelectric systems the temperature drop in the diathermic wall was estimated at 90[°]F. It was also assumed that the heat bypassing the thermocouples in the first stage does not flow through the thermocouples in the second stage.

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The computer codes for the design analysis of thermoelectric generators utilizing non-segmented and cascaded thermocouples were run for the operational conditions listed above. While the computer code for the design analysis of thermoelectric generators utilizing segmented thermocouples was debugged and has been operated with a test case, no further runs were made with this code, since it was felt that

- 1) A fabrication technology for segmented thermocouples for operation at milliwatt levels does not exist.
- 2) The performance parameters value of milliwatt thermoelectric generators utilizing cascaded stages in many cases will represent an upper boundary for the performance parameter values of milliwatt thermoelectric generators utilizing segmented thermocouples.

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B. MATERIALS

The design of thermoelectric power systems is contingent upon the availability of reliable data for the thermoelectric properties of the material as a function of temperature. Of equal importance to the designer are data with regard to reliability of the individual power generating elements, (i.e., the thermocouples) and with regard to physical properties (vapor pressures, tensile and compressive strength, Young's modulus).

A study of the technical literature and of the sales brochures issued by suppliers of thermoelectric materials revealed that in many instances the information given was not adequate for the design study to be undertaken. It was then decided to visit representatives of several suppliers of thermoelectric materials to

- 1) Obtain additional data, and
- 2) To request clarification of questionable data where appropriate

A subsequent visit to the materials suppliers resulted in the collection of much additional information over what was available in technical literature and sales pamphlets. However, in many instances the information required was simply unavailable or available as single point data only. The paucity of reliability data was particularly noticeable. Values for catastrophic failure rates and for power degradation rates are available for only a few materials systems and even those data were not applicable to the design study at hand, since they were concerned with couple geometries and configurations not suitable for milliwatt applications (see appropriate reports on various SNAP systems).

In view of this lack of reliability data it will be clear that in regard to the material aspects of milliwatt thermoelectric systems and systems development, the program for milliwatt thermoelectric generators

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must by necessity devote a substantial effort to reliability testing of couples and modules.

The thermoelectric and physical data of various materials which, after a preliminary investigation, appeared promising for use in large milliwatt thermoelectric generator systems are summarized in Appendix II-A.

These materials are:

Semiconductors

- 1) Bi_2Te_3 N/P (ASARCO)
- 2) PbTe 2N/2P (3M Company)
- 3) PbTe 3N/3P (3M Company)
- 4) MCC 40 N/P (Monsanto Research Corporation)
- 5) SiGe N/P (Radio Corporation of America)

Metals

- 1) Cupron Special/Tophel Special (Wilbur B. Driver)
- 2) Platinel #7674/#5355 (Engelhard Industries)
- 3) Chromel/Constantan (Hoskins/Driver Harris)

Use of any of the materials listed in this section is of course, contingent upon its commercial availability. A further consideration of importance to the choice of a material is the extent to which the supplier guarantees its physical properties. Still another consideration is the state-of-the-art of the module fabrication technology associated with the material. These problem areas will be discussed in detail in Section C - Performance of Milliwatt Generator Modules.

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C. PERFORMANCE OF MILLIWATT GENERATOR MODULES

The results of the parametric study are reported as follows: First, the results of the studies of milliwatt generators featuring non-segmented thermocouples are discussed; sequence of reporting is from the least attractive of the semiconductor materials for milliwatt generator applications to the most attractive; subsequently, the results of the wire thermocouples study will be presented. Next, the results of the studies of milliwatt generators utilizing cascaded stages of semiconductor thermoelectric materials will be discussed.

1. Performance of Milliwatt Generator Modules Utilizing Non-Segmented Thermocouples

Milliwatt generator modules utilizing the various thermoelectric material systems listed below were investigated for a wide range of design conditions. The design analysis code described in Appendix II-A was used in this investigation. Input data were taken from the appropriate data sheets of Appendix II-E.

a. SiGe N/P (Radio Corporation of America)

A parametric study was made of milliwatt generators utilizing SiGe N/P thermoelectric material. The results were plotted in Figures II-1 and II-2 for three hot junction temperatures,

$$T_H = 1700^{\circ}\text{F}$$

$$T_H = 1500^{\circ}\text{F}$$

$$T_H = 1350^{\circ}\text{F},$$

for three load voltages,

$$V_{LD} = 0.5 \text{ volts}$$

$$V_{LD} = 3.0 \text{ volts}$$

$$V_{LD} = 9.0 \text{ volts}$$

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FIGURE II-1. MODULE EFFICIENCY VERSUS NUMBER OF COUPLES IN SERIES OF MILLIWATT GENERATORS UTILIZING NON-SEGMENTED SiGe N/P THERMOCOUPLES

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FIGURE II-2. LOAD VOLTAGE VERSUS L/A RATIO FOR 300 TO 1000_{mw} THERMOELECTRIC GENERATORS UTILIZING NON-SEGMENTED $\text{SiGe}_3\text{N/P}$ THERMOCOUPLES

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and for three power levels

PSYST = 300 mW

PSYST = 600 mW

PSYST = 1000 mW

Figure II-1 shows module* efficiency versus number of couples in series. While systems efficiencies are attractively high at 4 to 5% for the highest hot junction temperature (1700°F) investigated, Figure II-2 shows that all high efficiency conditions in Figure II-1 correspond to L/A ratios in excess of the maximum L/A ratio ($L/A = 250 \text{ in.}^{-1}$) claimed by the supplier. Until such time that higher L/A ratio elements can be fabricated, SiGe N/P material must be ruled out as a thermoelectric material for modules operating at milliwatt power levels.

b. MCC 40 N/P (Monsanto Research Corporation)

Milliwatt generator modules utilizing arc plasma-sprayed MCC 40 N/P materials were investigated at three hot junction temperatures,

TH = 1700°F

TH = 1500°F

TH = 1350°F,

three load voltages

VLD = 0.5 volts

VLD = 3.0 volts

VLD = 9.0 volts

* NOTE: The module efficiency is corrected for losses in the power conditioning equipment.

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and four power levels

PSYST = 150 mw
PSYST = 300 mw
PSYST = 600 mw
PSYST = 1000 mw

Figure II-3 shows module efficiency versus number of couples in series. Figures II-4 and II-5 show load voltage versus L/A ratios for MCC 40 N/P modules featuring a cross-connected parallel circuit (one-fold couple redundancy). The maximum L/A ratio demonstrated with MCC 40 N/P materials is 1250 in.^{-1} and consequently all designs of 300 to 1000 mw modules are practical for load voltages up to 9v. Practical 150 mw designs are feasible with load voltages up to approximately 6v. While MCC 40 N/P thermoelectric material facilitates the design of modules suitable for a wide range of power levels, load voltages and hot junction temperatures, it is evident from Figure II-3 that the module efficiencies are low (0.5 to 1.8%) compared to the module efficiencies feasible with such materials systems as PbTe 2N/2P and Bi_2Te_3 N/P. Therefore, MCC 40 N/P material must be ruled out as a thermoelectric material for milliwatt generator modules until demonstration of improved thermodynamic efficiency for MCC 40 N/P thermocouples.

c. PbTe 3N/3P (3M Company)

PbTe 3N/3P milliwatt generator modules were investigated for five hot junction temperatures,

TH = 1000°F
TH = 900°F
TH = 700°F
TH = 600°F
TH = 440°F ,

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FIGURE II-3. MODULE EFFICIENCY VERSUS NUMBER OF SERIES COUPLES OF MILLIWATT GENERATORS UTILIZING NON-SEGMENTED MCC 40 N/P THERMOCOUPLES

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FIGURE II-4. LOAD VOLTAGE VERSUS L/A RATIO FOR 150mw THERMOELECTRIC GENERATOR UTILIZING MCC 40 N/P THERMOCOUPLES

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FIGURE II-5. LOAD VOLTAGE VERSUS L/A RATIO FOR 300 TO 1000_{mw} THERMOELECTRIC GENERATORS UTILIZING NON-SEGMENTED MCC 40 N/P THERMOCOUPLES

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four load voltages of the module,

VLD = 0.5 volts
VLD = 1.0 volts
VLD = 2.25 volts
VLD = 3.0 volts

and three power levels,

PSYST = 300 mw
PSYST = 600 mw
PSYST = 1000 mw

Figure II-6 shows module efficiency versus number of couples in series at various design conditions. Figure II-7 shows load voltage versus L/A ratio for modules with a cross-connected parallel circuit (one-fold couple redundancy). Practical L/A ratios of 100 in.^{-1} have been demonstrated with PbTe 3N/3P; however, it is felt that L/A ratios of about 200 in.^{-1} are not too far beyond the state-of-the-art. This range of L/A ratios facilitates the design of a large number of modules for operation at the 600 to 1000 mw power levels at load voltages up to 3.0v. Fewer modules are feasible at the 300 mw level; however, a much larger number of designs are feasible if the module circuit were designed for all thermocouples in series. In that case the L/A ratio for a design characterized in Figure II-7 by a hot junction temperature and a module load voltage is one-half of the value shown and the feasibility range for the various designs has doubled. This increase in feasibility range for the designs results in a decrease in reliability of the circuitry and for each design it will be necessary to determine whether its reliability is adequate. Figure II-6 shows that the module efficiency runs from approximately 3 to 5% at hot junction temperatures of 900 to 1000°F . While these efficiencies are quite attractive, it will be shown that PbTe 2N/2P modules yield higher module efficiencies (see Figures II-8 and II-9) and consequently PbTe 3N/3P is less attractive than PbTe 2N/2P.

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FIGURE II-6. MODULE EFFICIENCY VERSUS NUMBER OF COUPLES IN SERIES OF MILLIWATT GENERATORS UTILIZING NON-SEGMENTED PbTe 3N/3P THERMOCOUPLES

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FIGURE II-7. LOAD VOLTAGE VERSUS L/A RATIO FOR 300 TO 1000_{mw} THERMOELECTRIC GENERATORS UTILIZING NON-SEGMENTED PbTe 3N/3P THERMOCOUPLES

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FIGURE II-8. MODULE EFFICIENCY VERSUS NUMBER OF COUPLES IN SERIES OF MILLIWATT GENERATORS UTILIZING NON-SEGMENTED PbTe 2N/2P THERMOCOUPLES

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FIGURE II-9. LOAD VOLTAGE VERSUS L/A RATIO FOR 300 TO 1000mw THERMOELECTRIC GENERATORS UTILIZING NON-SEGMENTED PbTe 2N/2P THERMOCOUPLES

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d. PbTe 2N/2P (3M Company)

PbTe 2N/2P modules were investigated for five hot junction temperatures,

TH = 1000°F
TH = 900°F
TH = 700°F
TH = 600°F
TH = 440°F

five load voltages of the module,

VLD = 0.5 volts
VLD = 1.0 volts
VLD = 1.5 volts
VLD = 2.25 volts
VLD = 3.0 volts

and three power levels,

PSYST = 300 mW
PSYST = 600 mW
PSYST = 1000 mW

Figure II-8 shows module efficiency versus number of couples in series for various design conditions of hot junction temperature and load voltage. Figure II-9 represents a plot of load voltage versus L/A ratios for various power levels and hot junction temperatures. The considerations with respect to module design range and L/A ratio discussed above for PbTe 3N/3P, are equally valid for PbTe 2N/2P. The design ranges for PbTe 3N/3P and PbTe 2N/2P are approximately the same, as inspection of Figures II-7 and II-9 show, but comparing the module efficiency plots for PbTe 2N/2P (Figure II-8) with those for PbTe 3N/3P (Figure II-6) shows that PbTe 2N/2P module efficiencies are approximately 1% higher than the corresponding PbTe 3N/3P

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efficiencies. Module efficiencies of PbTe 2N/2P modules vary from approximately 3.5 to 4.5% at $T_H = 900$ to 1000°F for module load voltages from 0.5 to 3.0v.

e. Bi_2Te_3 N/P (ASARCO)

Bi_2Te_3 N/P modules were investigated for four hot junction temperatures,

$T_H = 440^\circ\text{F}$

$T_H = 400^\circ\text{F}$

$T_H = 350^\circ\text{F}$

$T_H = 300^\circ\text{F}$

five load voltages of the modules,

VLD = 0.5 volts

VLD = 1.0 volts

VLD = 1.75 volts

VLD = 2.25 volts

VLD = 3.0 volts

and four power levels,

PSYST = 150 mw

PSYST = 300 mw

PSYST = 600 mw

PSYST = 1000 mw

Figure II-10 shows module efficiency versus number of couples in series for various design conditions of hot junction temperature and load voltage of the module. Module efficiencies in the range of 1.7 to 2.6% are feasible at hot junction temperatures of 400 to 450°F for various load voltages (0.5 to 3.0v). Figure II-11 shows load voltage versus L/A ratio for various power levels and hot junction temperatures. L/A ratios as high as 1500 in.^{-1} have

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FIGURE II-10. MODULE EFFICIENCY VERSUS NUMBER OF COUPLES IN SERIES OF MILLIWATT GENERATORS UTILIZING NON-SEGMENTED Bi_2Te_3 N/P THERMOCOUPLES

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FIGURE II-11. LOAD VOLTAGE VERSUS L/A RATIO FOR 150 TO 1000mw THERMOELECTRIC GENERATORS UTILIZING NON-SEGMENTED Bi_2Te_3 N/P THERMOCOUPLES

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been demonstrated for Bi_2Te_3 N/P materials and it is evident from the plot of Figure II-11 that all the designs represented there by load voltage, power level and hot junction temperature are within the realm of practical design and fabrication. A large number of the designs can be realized with commercially available Bi_2Te_3 modules featuring high L/A ratios up to 150 in.^{-1} .

f. Cupron Special/Tophel Special (Wilbur B. Driver)

Wire thermocouple materials typically show low electrical power output per couple and in any large milliwatt power level application, the number of couples needed is large and generating high load voltages is no problem even if many parallel circuits are used to increase reliability. The problems associated with handling large numbers of couples, however, are such that there is a strong incentive to reduce the number of couples by mismatching the external load to the internal resistance of the generator. For the parametric investigation all modules were sized for the system voltage requirement (18 volts); thus the study reflects the effect of hot junction temperature and mismatch ratio on performance and number of couples on an 18-volt system. The modules were investigated for three hot junction temperatures,

$$T_H = 900^\circ\text{F}$$

$$T_H = 700^\circ\text{F}$$

$$T_H = 500^\circ\text{F}$$

and five mismatch ratios of external load to internal resistance of the generator,

$$R_{MIS} = 1.0$$

$$R_{MIS} = 1.25$$

$$R_{MIS} = 1.50$$

$$R_{MIS} = 1.75$$

$$R_{MIS} = 2.0$$

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Module efficiency versus number of couples in series for a 150-mw-thermoelectric module were determined for several circuit configurations using one to ten parallel circuits. All L/A ratios for each of these configurations are below the 10^6 in.^{-1} value estimated to be feasible with metallic wire thermocouple materials. Since L/A ratios are not a critical thermoelectric design parameter for wire-type generators operating in the 150 to 1000 mw power level range, Figure II-12 shows module efficiency versus number of couples in series for a wire-type thermoelectric module. In the case of a 150-mw-module with five parallel circuits, the reliability for any design is better than, or equal to, -0.99998 if a constant failure rate of $3 \times 10^{-8}/\text{hr}$ is assumed for the 18-month mission. Figure II-12 shows that the module efficiency is about 0.8% over the entire range of mismatch ratios at a hot junction temperature of 900°F . This efficiency is low compared to those modules using semiconductor materials; and the use of Cupron Special/Tophel Special wire does not appear to be attractive in the range of power levels investigated (150 to 1000 mw). However, if for a system a Cupron Special/Tophel Special module is specified then it appears to be advantageous to increase the mismatch ratio RMIS to at least $\text{RMIS} = 2.0$ to decrease the number of couples in series and consequently increase circuit reliability. Figure II-12 shows that in the range of mismatch ratios investigated the module efficiency does not change appreciably.

2. Performance of Advanced Concepts Milliwatt Generator Modules Utilizing Cascaded Thermocouples

Cascaded thermoelectric modules can be expected to show increased performance over single stage modules if thermoelectric materials are properly matched to optimum temperature ranges. Such a fabrication technology does not exist, however, and the cascaded module must be classed as an advanced-type thermoelectric module. A parametric investigation was made of

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FIGURE II-12. MODULE EFFICIENCY VERSUS NUMBER OF COUPLES IN SERIES OF MILLIWATT GENERATORS UTILIZING CUPRON/TOPHTEL SPECIAL

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the two system to explore the superior performance parameters of these cascaded modules a (SiGe(N/P)/PbTe(2N/2P) and a PbTe(2N/2P)/Bi₂Te₃(N/P). The parametric study was made using the design analysis and computer code described in Appendix II-C. Thermoelectric material input data were taken from the appropriate data sheets of Appendix II-E.

a. SiGe(N/P)/PbTe(2N/2P) Milliwatt Generator Modules

The modules were investigated for a $T_H = 900^{\circ}\text{F}$ hot junction temperature of the high efficiency PbTe 2N/2P stage and for three hot junction temperatures of the SiGe N/P stage:

$$T_H = 1700^{\circ}\text{F}$$

$$T_H = 1600^{\circ}\text{F}$$

$$T_H = 1450^{\circ}\text{F}$$

Module efficiencies were determined at six load voltages of the module:

$$V_{LD} = 0.5 \text{ volts}$$

$$V_{LD} = 1.5 \text{ volts}$$

$$V_{LD} = 3.0 \text{ volts}$$

$$V_{LD} = 4.5 \text{ volts}$$

$$V_{LD} = 7.5 \text{ volts}$$

$$V_{LD} = 10.5 \text{ volts}$$

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Figure II-13 shows module efficiency versus load voltage for various SiGe N/P stage hot junction temperatures. At the highest hot junction temperature investigated (1700°F), module efficiencies higher than 7.0% can be reached at load voltages higher than 3.0v.

Figure II-14 shows module efficiency versus number of couples in series in the SiGe N/P and PbTe 2N/2P stages.

Figures II-15 and II-16 show load voltage versus L/A ratio for 300 mw thermoelectric generator modules for the SiGe N/P stage and the PbTe 2N/2P stage, respectively. At the power level considered, the L/A ratios required for the first stage are beyond the state-of-the-art of SiGe N/P couple technology for all but a few low voltage designs (less than 2v load voltage). L/A ratios of the PbTe 2N/2P stage of the 300 mw cascaded module are lower than those of the 300 mw module utilizing non-segmented PbTe 2N/2P couples. Effectively, the PbTe 2N/2P stage operates at both a power level and a load voltage lower than that of the module power level and module load voltage. The lower power level would, at a constant load voltage, increase the L/A ratio, while a decrease in load voltage would decrease L/A ratio. The sum of these effects results in a lower L/A ratio for the PbTe 2N/2P stage.

b. PbTe(2N/2P)/BiTe₃(N/P) Milliwatt Generator Modules

The modules were investigated for a $T_H = 400^{\circ}\text{F}$ hot junction temperature of the Bi₂Te₃ N/P stage and for four hot junction temperatures of the PbTe 2N/2P stage:

$T_H = 700^{\circ}\text{F}$
 $T_H = 800^{\circ}\text{F}$
 $T_H = 900^{\circ}\text{F}$
 $T_H = 1000^{\circ}\text{F}$

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FIGURE II-13. MODULE EFFICIENCY VERSUS LOAD VOLTAGE OF MILLIWATT GENERATORS USING CASCADED STAGES SiGe N/P AND PbTe 2N/2P

FIGURE II-14. MODULE EFFICIENCY VERSUS NUMBER OF COUPLES IN SERIES OF MILLIWATT GENERATORS UTILIZING CASCADED STAGES OF SiGe N/P AND PbTe 2N/2P

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FIGURE II-15. LOAD VOLTAGE VERSUS L/A RATIO FOR 300mw THERMOELECTRIC GENERATORS UTILIZING CASCADED STAGES SiGe N/P AND PbTe 2N/2P

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FIGURE II-16. LOAD VOLTAGE VERSUS L/A RATIO FOR 300_{mw} THERMOELECTRIC GENERATORS UTILIZING CASCADED STAGES SiGe N/P AND PbTe 2N/2P

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Module efficiencies were determined for six load voltages of the modules,

VLD = 0.5 volts
VLD = 1.0 volts
VLD = 1.5 volts
VLD = 3.0 volts
VLD = 4.5 volts
VLD = 9.0 volts

Figure II-17 shows module efficiency versus load voltage for the various PbTe 2N/2P hot junction temperatures. At the highest hot junction temperature investigated (1000°F) and at a load voltage of 3v, a module efficiency of 5.3% is reached.

Figure II-18 shows module efficiency versus number of couples in series in the Bi_2Te_3 N/P and PbTe 2N/2P stages.

Figures II-19 and II-20 show load voltage versus L/A ratio for 300 mw thermoelectric generator modules for the PbTe 2N/2P and Bi_2Te_3 N/P stages respectively. A comparison of the individual stages of the 300 mw cascaded module with 300 mw modules of PbTe 2N/2P and Bi_2Te_3 shows (see Figures II-19, II-20, II-9 and II-11) that the L/A ratios in the cascaded module are lower in both stages. While the power produced in each stage is less (which would normally result in a higher L/A ratio), the load voltage per stage is also lower; as a result the decrease in load voltage yields a lower L/A ratio in both stages. The feasibility of the fabrication of a $\text{PbTe}(2\text{N}/2\text{P})/\text{Bi}_2\text{Te}_3(\text{N}/\text{P})$ cascaded module depends on the feasibility of fabricating the PbTe 2N/2P stage since its L/A ratios are more critical. Figure II-20 shows that module load voltages of approximately 3.0v yield L/A ratios that are practical ($< 150 \text{ in.}^{-1}$).

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FIGURE II-17. MODULE EFFICIENCY VERSUS LOAD VOLTAGE OF MILLIWATT GENERATORS UTILIZING CASCADED STAGES PbTe 2N/2P AND Bi_2Te_3 N/P

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FIGURE II-18. MODULE EFFICIENCY VERSUS NUMBER OF COUPLES IN SERIES OF MILLIWATT GENERATORS UTILIZING CASCADED STAGES $PbTe$ 2N/2P AND Bi_2Te_3 N/P

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FIGURE II-19. LOAD VOLTAGE VERSUS L/A RATIO FOR 300mw THERMOELECTRIC GENERATORS UTILIZING CASCADED STAGES PbTe 2N/2P AND Bi₂Te₃ N/P

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FIGURE II-20. LOAD VOLTAGE VERSUS L/A RATIO FOR 300mw THERMOELECTRIC GENERATORS UTILIZING CASCADED STAGES PbTe 2N/2P AND Bi₂Te₃ N/P

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D. SELECTION OF THERMOELECTRIC MODULES FOR HIGH PERFORMANCE MILLIWATT GENERATOR SYSTEMS

Selection of thermoelectric modules for high performance milliwatt generator systems must be guided by the following criteria:

- 1) Can the module be fabricated with either state-of-the-art or moderately advanced thermocouple and module technologies?
- 2) Is the module reliable?
- 3) Is the module efficient?
- 4) What is the module cost?

1. Fabrication of Modules

The following major factors determine the degree of fabrication complexity of a milliwatt generator module:

- 1) The thermoelectric materials used
- 2) The L/A ratios of the elements in the module
- 3) The heat flux density at the hot junction of the thermocouple
- 4) The number of thermocouples
- 5) The use of non-segmented, segmented, or cascaded thermocouple configurations

The problems of choosing thermoelectric materials, choice of the thermocouple configuration, and what practical limitations should be placed on the L/A ratios, the heat flux densities and the number of couples, are not easily solved. Information with respect to module fabrication technologies, thermoelectric materials limitations, etc., is not available or is proprietary to the suppliers of thermoelectric modules. Limited data supplied by the various manufacturers of thermoelectric modules, augmented with Monsanto Research Corporation's experience in fabricating thermoelectric

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systems, yielded tentative limitations for some of these design parameters. Table II-1 below shows these limitations for the several thermoelectric materials.

TABLE II-1

ESTIMATED LIMITATIONS OF SEVERAL THERMOELECTRIC MATERIALS

<u>Thermoelectric Materials</u>	<u>Maximum L/A, in.⁻¹</u>	<u>Maximum No. Couples</u>	<u>Maximum Heat Flux Density, watts/cm²</u>	<u>Remarks</u>
SiGe N/P	250	-	-	Not suited for 150 to 1000 mw applications because of excessive L/A ratios
MCC 40 N/P	1500	40	< 10	3 to 9v at 300 mw; 3 to 6v at 600 to 1000 mw
PbTe 3N/3P	100 to 200	150	< 10	300 mw at 1.5 to 2.0v; 600 to 1000 mw at 2.0 to 3.0v
PbTe 2N/2P	100 to 200	150	< 10	300 mw at 1.5 to 2.0v; 600 to 1000 mw at 2.0 to 3.0v
Bi ₂ Te ₃ N/P	1500	300	≤ 5	150 to 1000 mw at ≥ 3.0v
Cupron Special/ Tophel Special	10 ⁶	1 to 5 x 10 ⁵	< 10	Not suited for 150 to 1000 mw because of excessive modules, weight and volume of T.E.
SiGe(N/P) / PbTe(2N/2P)	250 for SiGe 100 to 200 for PbTe	-	-	Not suited for 150 to 1000 mw modules because of excessive SiGe N/P L/A ratios
PbTe(2N/2P)	100 to 200 PbTe	200	< 5	300 to 1000 mw at 7.3v
Bi ₂ Te ₃ (N/P)	1500 for Bi ₂ Te ₃			

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It is evident from Table II-1 that Bi_2Te_3 N/P, PbTe 2N/2P, PbTe 3N/3P and PbTe(2N/2P)/ Bi_2Te_3 (N/P) are attractive materials with respect to their use in 150 to 1000 mw thermoelectric modules. Bi_2Te_3 N/P has the widest range of utilization in the power level range under consideration. It has been widely used for the production of milliwatt power level modules, and power modules for operation in the range 150 to 1000 mw are commercially available. Furthermore, the state-of-the-art of Bi_2Te_3 N/P fabrication technology has reached a high degree of development and it should be relatively straightforward to develop Bi_2Te_3 N/P modules of a design optimized to yield high performance radioisotope-powered milliwatt generator systems. The PbTe 2N/2P and PbTe 3N/3P materials are somewhat less attractive since their fabrication technologies are less well established. From a fabrication point of view, the cascaded system PbTe(2N/2P)/ Bi_2Te_3 (N/P) is the least attractive since it will compound the fabrication problems of two materials systems.

2. Module Reliability

The reliability of RTG modules (Section VII) depends on many factors (thermocouple failure rate, number of thermocouples and circuit configuration). The reliability of the modules was investigated with the aid of the analytical procedure and computer code described in Appendix II-D. The thermocouple failure rates were conservatively estimated at:

$5 \cdot 10^{-8}$ /hr for semiconductor material couples

and $2 \cdot 10^{-8}$ /hr for wire material couples.

Figure II-21 shows the reliability of cross-connected series parallel circuit configurations (i.e., of circuits with one-fold thermocouple redundancy) for a couple failure rate of $5 \cdot 10^{-8}$ /hr. Since the state-of-the-art fabrication technologies impose a limit of about 300 couples per module (see Table II-1), it appears that for the estimated failure rates semiconductor type milliwatt generator modules will show reliabilities in excess of 0.999. Therefore, there are, if the estimate of $5 \cdot 10^{-8}$ couple failures/hr can be substantiated, no reliability problems for the module.

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FIGURE II-21. RELIABILITY VERSUS NUMBER OF COUPLES IN SERIES FOR CROSS-CONNECTED SERIES-PARALLEL CIRCUITS

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3. Module Efficiency

Module efficiencies, associated maximum hot junction temperatures, and load voltages compatible with L/A ratios that can be practically realized are summarized in Table II-2 below for the various thermoelectric materials.

Of the modules that can be fabricated with state-of-the-art to moderately advanced fabrication techniques, the PbTe 2N/2P and PbTe 3N/3P systems show the highest efficiencies. The efficiency of the Bi_2Te_3 N/P system is somewhat lower at 2.2% but such a module can be fabricated with state-of-the-art techniques.

4. Module Cost

The cost data received from suppliers of thermoelectric materials were exclusively related to the cost of the ingots of thermoelectric material. While the materials-cost component of a thermoelectric module cannot usually be neglected, the fabrication- and testing-cost components are the main determinant of the cost of a module. Figure II-21 and II-22 show the module cost curves for the two most attractive material systems for milliwatt power generation, i.e., PbTe 2N/2P and Bi_2Te_3 N/P. These plots are based on Monsanto Research Corporation's experience with the fabrication of thermoelectric devices. A comparison of Figures II-21 and II-23 shows the cost of Bi_2Te_3 N/P modules to be approximately one order of magnitude lower than that of PbTe 2N/2P modules.

5. Conclusions

The considerations with respect to fabricability, reliability, module efficiency and cost discussed above indicate that three material systems exist that have superior properties with respect to use in milliwatt generator modules. Bi_2Te_3 N/P can be used over the whole range 150 to 1000 mw; it has moderately high efficiencies; it can be fabricated with state-of-the-art

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TABLE II-2

ESTIMATED MODULE EFFICIENCIES

<u>Thermoelectric Material</u>	<u>Maximum* Hot Junction Temperature, °F</u>	<u>Maximum Module Voltage, volts</u>	<u>Maximum Module Efficiency, %</u>	<u>Remarks</u>
SiGe N/P	1700	-	5.5	Not suited for 150 to 1000 mw modules because of excessive L/A ratios
MCC 40 N/P	1700	3 to 9v at 300 mw 3 to 6v at 600 to 1000 mw	1.8	Efficiencies too low
PbTe 3N/3P	900	3	3.3	Moderately advanced techniques required to reach 3.3% efficiency
PbTe 2N/2P	900	3	4.3	Moderately advanced techniques required to reach 4.3% efficiency
Bi ₂ Te ₃ N/P	400 to 450	3-9	2.2 to 2.6	Modules can be fabricated using state-of-the-art technologies
Cupron Special/ Tophel Special	900	18	0.8	Efficiencies too low
SiGe(N/P) / PbTe(2N/2P)	1700	≤ 3	≥ 7.0	Not suited for 150 to 1000 mw modules because of excessive L/A ratios of the SiGe N/P couples
PbTe(2N/2P) / Bi ₂ Te ₃ (N/P)	900	≥ 3	≥ 5.3	Advanced fabrication techniques required

* Note: The maximum hot junction temperatures were selected on the basis of their compatibility with long life (> 18 months) of the thermocouple; TC = 170°F.

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FIGURE II-22. TENTATIVE COST OF PbTe 2N/2P MODULES VERSUS POWER LEVEL

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FIGURE II-23. TENTATIVE COST OF Bi_2Te_3 MODULES VERSUS POWER LEVEL

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technologies at low cost. PbTe 2N/2P and PbTe 3N/3P can be used in the range 300 to 1000 mw; it has moderately high to high efficiency; fabrication of modules will require a moderate advance in module fabrication techniques; fabrication of modules will be expensive.

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III. RADIOISOTOPE HEAT SOURCES

The three radioactive compounds established by the AEC as reference fuels for this study were strontium titanate (SrTiO_3), plutonium dioxide (PuO_2) and promethium sesquioxide (Pm_2O_3). This section discusses these and other potential heat sources.

The AEC-Division of Isotope Development has nine nuclides under development for use as heat sources (Table III-1). Consequently there is a continued growth of technology, and frequently a shift in the relative qualification of sources for application. In view of these facts, a re-assessment of radioisotopes was made at the start of this study, but it was limited to nuclides already under development.

A. RADIOISOTOPE

The candidate radioisotopes are compared below with respect to half-life, shielding requirements, costs, near-future availability and biological hazard.

1. Half-Life

The minimum useful lifetime for a large milliwatt RTG is about 1.5 yr and, in the near future, the longest practical lifetime will be about 10 yr. Taking 1.5 as the maximum ratio for BOL/EOL*, boundary conditions exist at half-lives of 2.6 and 17 yr. A source with a half-life less than 2.6 yr would degrade too rapidly and is unacceptable. If the half-life is longer than 17 yr, the source is acceptable for the application

* Beginning of Life is the encapsulation date; End of Life is the end of application

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Table III-1
Radioisotope Heat Sources Under Development

<u>Nuclide</u>	<u>Half-Life, yr</u>	<u>Developer</u>	<u>Heat Source Delivered</u>	<u>Properties of the Pure Isotope</u>		<u>Price or Estimated Cost \$/w(t)</u>	
				<u>c/w</u>	<u>c/gm</u>	<u>Current</u>	<u>Future (1)</u>
Sr-90	27.7	Martin-ORNL	yes	148	142	30-74 ⁽²⁾	19
Pu-238	86.4	Mound	yes	30.6	16.8	1600 ⁽³⁾	535
Pm-147	2.62	BNWL	no	2788	928	560-1400 ⁽²⁾	93
Co-60	5.27	SRL	no	65	100-200 ⁽⁴⁾	21-468 ⁽⁴⁾	10
Cs-137	26.6	ORNL	yes	207	98.25	26-104 ⁽²⁾	21
Ce-144	0.781	ORNL	no	126	3180	19-63 ⁽²⁾	1
Po-210	0.379	Mound	yes	31.8	4490	780 ⁽⁵⁾	10
Cm-242	0.445	ORNL	yes	27.7	3320	170 ⁽³⁾	(6)
Cm-244	18.4	ORNL	no	29.1	79.8	1100 ⁽³⁾	480

- (1) Estimated or predicted minimum costs, assuming full production and certain technological break-throughs
- (2) AEC-Isochem Inc. contract prices, see Table III-4
- (3) Apparently no sales have been made to date; values are estimates
- (4) Arbitrary selection for heat source application, see Table III-5 for price data
- (5) Current AEC price, exclusive of encapsulation
- (6) Development will probably be stopped in favor of Po-210

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over the full range of lifetimes, but for any radioisotope with a half-life between 2.6 and 17 yr the useful application lifetime must be limited to less than 10 yr. The candidates are rated as follows:

Acceptable, full range: Sr-90, Pu-238, Cs-137, Cm-244

Acceptable, Limited Range: Pm-147, Co-60

Unacceptable (and dropped from this study): Ce-144, Po-210, Cm-242

2. Shielding Requirements

For an electrical output of 150 to 1000 mw per RTG, the thermal power required at the end of the application (EOL) can be assumed to fall between 15 and 30w, thus limiting the fuel power at encapsulation (BOL) to between 15 and 45w. Consider a thermal source which produces 30w at the end of life and let the lifetime be 10 yr (except for Pm-147 and Co-60 where BOL/EOL - 1.5 max limits the useful lifetime to 1.5 and 3 yr, respectively); furthermore, assume the fuel form is a cylindrical pellet of equal length and diameter and let the distance from pellet surface to receptor be 4 in. The radiations of significance in shielding calculations (Table III-2) are applied in Table III-3 for the conditions described above to determine the thickness and weight of uranium shielding required to reduce the photon dose rate at the receptor to 200 mr/hr. In these calculations self-shielding by the source is neglected and the source is treated as a point source.

Extending the shielding analysis over the entire range of power for this application would change the size of the nuclide source by a factor of 2 or 3, but the variation in shield weight would be only about 10%. The values in Table III-3 are therefore an adequate representation for comparison of radioisotopes. Based on the shielding analysis the sources are divided into two categories and rated as follows:

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- Category I - requiring moderate to heavy shielding
Acceptable: Sr-90, Cs-137, Cm-244*
- Category II - requiring little or no shielding
Acceptable: Pu-238, Pm-147
Less desirable, requires relatively heavy shielding: Cm-244*

Table III-2

Radioisotope Data for Shielding Design

(Only radiations which contribute significantly to the shielding or dose rate calculations are included)

Radioisotope	Energy, Mev			Abundance, Particles/sec-w
	Neutron	Beta	Gamma Bremsstrahlung	
Sr-Y-90 ⁽¹⁾	--	--	--	2.806 x 10 ⁷
			2.00	4.086 x 10 ⁸
			1.70	2.229 x 10 ⁹
			1.40	8.255 x 10 ⁹
			1.10	2.649 x 10 ¹⁰
Pu-238 ⁽²⁾	1-10	--	--	2 x 10 ⁴
Pm-147 ⁽³⁾	--	--	0.75	1.676 x 10 ⁷
			0.45	1.676 x 10 ⁷
Co-60 ⁽¹⁾	--	--	1.333	2.385 x 10 ¹²
			1.172	2.385 x 10 ¹²
Cs-Ba-137 ⁽¹⁾	--	--	0.662	6.39 x 10 ¹²
Cm-244 ⁽⁴⁾	1-10	--	--	3 x 10 ⁷
	--	--	3-7	4.3 x 10 ⁶
			1-2	1.3 x 10 ⁷
			0.5-1.0	5.3 x 10 ⁷

- (1) ORNL-3576, Handbook of Shielding Requirements, etc. E.D.Arnold, April 1964
 (2) DP-984, Radiation Properties of Pu-238, etc., D.H.Stoddard, E.L.Albenesius, July 1965.
 (3) Private communications, BNWL; the Photons are from the Pm-146 impurity.
 (4) DP-939, Radiation Properties of Cm-244, etc., T.R.Herold, et al., November 1964.

* Cm-244 is listed in both categories because its shielding requirements lie near the dividing line.

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Table III-3

Shielding Comparison for a 30 w(t) BOL Source
in a 10-yr Mission (1)

<u>Nuclide</u>	<u>Assumed Power Density, w/cc</u>	<u>Fuel Dia., in.</u>	<u>Shielding Thickness, in. Uranium</u>	<u>Shielding Weight,⁽²⁾ lb</u>
Sr-90	0.84	1.5	3.1	250
Pu-238	3.5	0.9	(3)	-
Pm-147	1.5	1.3	0.32	2.7
Co-60	27	0.5	5.2 ⁽⁴⁾	710
Cs-137	0.21	2.4	2.4	200
Cm-244	26	0.5	1.6 ⁽⁵⁾	28

- (1) For BOL/EOL - 1.5, Pm-147 mission limited to 1.5 yr and Co-60 to 3 yr
 (2) Cylindrical
 (3) No photon shielding required, only neutron shielding
 (4) Obviously this amount of shielding would result in a change of source surface-to-receptor distance in a real system; it was not changed in this comparative analysis.
 (5) Neutron shielding required in addition to this photon shielding

3. Fuel Costs

The suppliers have not clearly defined what is included in estimated cost for radioisotope fuels. To be consistent in this discussion, all costs are assumed to include the encapsulated* fuel ready for shipment at the supplier's site along with the product assurance documentation; but the encapsulation cost is not included. It is further assumed that selection of the radioisotope effectively sets the price of the fuel and that selection of an established fuel form will not affect the price.

A limited number of capsules have been fueled with certain radioisotopes being developed (see Table III-1), but full production

* The capsule is the outer container in an encapsulated system and is the ultimate containment barrier between the fuel and the environment. A liner may be inserted by the supplier at his expense.

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has not been achieved for any heat source nuclide. Consequently, none of the prices has reached the minimum attainable through high-volume production predicted for the mid-1970's.

One stabilized price structure was established when the AEC and Isochem Inc. completed a contract in November 1965 setting prices for the four fission product heat sources. Prices for the three sources of interest to this program are tabulated in Table III-4. Although these prices do not represent the desired minima, they are a reduction from earlier prices and provide a stable planning basis for the next several years. It can be seen that even though Pm-147 is a fission product there is little hope that it can compete with Sr-90 on a fuel cost basis; Pm-147 costs are comparable to Pu-238 costs. Another point of interest is that the cost of Cs-137 is roughly twice that of Sr-90 for all quantity orders except the largest; therefore, Cs-137 would be competitive with Sr-90 on a fuel cost basis only when purchased for lots of at least 33 generators of the largest size.

Table III-4

Current AEC - Isochem Inc. Price List

Radioisotope	Sr-90	Cs-137	Pm-147
Curies per watt	148	207	2788
<u>Power range, kc</u>	<u>Cost, \$/w(t)</u>		
0 to 5	74.00	103.50*	1,395.00
5 to 50	44.40	93.20	837.50
50 to 150	37.00	72.50**	698.00
150	29.60	25.90	558.00

* Price holds to 10 kc

** Price holds to 200 kc

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Table III-5 summarizes estimated costs for Co-60. Prices for quantities less than 100 kw (Group A) are much too high in comparison with Sr-90 to be considered for this application. Although Co-60 at 45 c/gm has about the same power density as Sr-90, there is no cost advantage over Sr-90 (Group B). Furthermore, Co-60 at this low specific activity would require heavier shielding than shown in Table III-3 because of the increased fuel volume. Cobalt-60 could be competitive for a single order of more than 100 kw after realization of the lower estimated cost (Group C), or the future minimum cost, as the true cost. Therefore, based on the unsettled nature of prices for heat source and the heavy shielding required, Co-60 for this application is tabled.

Prices are not established for Pu-238 and Cm-244 fuel, but estimates are shown in Table III-1. Obviously Pu-238 is not competitive with Sr-90 on a fuel cost basis, but it is the most attractive heat source for minimum weight devices. It is less expensive, or equal in cost to Pm-147 and Cm-244, and leads to a lighter generator. In consideration of the high cost of Cm-244 and its unfavorable shielding requirements in comparison to Pu-238 and Pm-147, this nuclide will not be considered further.

4. Availability

Encapsulated heat sources of Pm-147 and Co-60 will be completed this fiscal year and of Cm-244 by the end of fiscal 1967. All radioisotopes of interest to this study will have been used in demonstrations or applications in the near future, indicating that the processes and facilities for fuel forming and encapsulation exist, even though the facilities, are not qualified for full production economy.

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Table III-5

Current and Near-Future Co-60 Costs

A. AEC and commercial sales of less than 100 kc (1540w) (Partial Listing)

Specific Activity, c/gm	List Price*, \$/c	Equivalent Price, \$/w(t)
41 to 55	4.00	260.00
86 to 100	5.85	380.00
191 to 205	7.20	468.00

B. AEC sales of greater than 100 kc

Specific Activity, c/gm	Quantity Ordered, kc	Equivalent Price, \$/w(t)
0 to 15	100 kc plus	26.00
16 to 30	100 kc plus	32.50
31 to 45**	100 to 250 kc	42.20
31 to 45**	251 to 500 kc	39.00
31 to 45**	Over 500 kc	35.75

C. AEC sales from SRP production at greater than 100 kc (estimated cost)

Specific Activity, c/gm	Near-Future Cost, \$/w(t)	Future Minimum Cost, \$/w(t)
100 to 200	21.45 to 32.50	10.00

All (transfer to
Govt. agency only) 14.00 --

* Discounts: 5.001 to 25 kc 15% for 0 to 45 c/gm
25.001 to 50 kc 30% for 0 to 45 c/gm
50.001 to 100 kc 30% over 45 c/gm

** For "BNL strips" only

Production of heat sources made from fission products will increase when Isochem Inc. begins hot cell operations in 1968. Savannah River Laboratory (SRL) has a program well under way to produce Co-60 sources of high specific activity and this may be matched by developments at Hanford. Mound Laboratories will continue to provide Pu-238 sources, and the ORNL Isotope Center is progressing with Cm-244 development. Martin Co. will terminate production of Sr-90 heat sources at Quehanna, Pa. as soon as Isochem Inc. is in production.

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Current availability of radioisotopes is difficult to assess because, without exception, the supply exceeds the demand, but current production of heat sources is based on requirements. These facts should be remembered when considering the quantity of material encapsulated in AEC operations during 1965 (Table III-6).

Near-future availability of radioisotopes is assumed to stem primarily from AEC operations, but by 1980, civilian nuclear power reactors should be the principal sources. Estimated annual production is shown in Table III-6. These data indicate no serious problem in availability for any of the fuels of interest.

In review, Cs-137 is comparable to Sr-90 in half-life and weight of shielding required, but except for orders exceeding 20 kc per year, it is more expensive. Since there appears to be no predictable shortage of Sr-90 in the future, and since Cs-137 is not superior to Sr-90, this nuclide, like Co-60 and Cm-244, is set aside for this study. The remaining candidate nuclides (Sr-90, Pu-238, and Pm-147) are the three named as reference by the AEC.

Table III-6
Potential Availability of Radioisotope Fuel
in kilowatts per year

	<u>Availability,</u>					
	Sr-90	Pu-238*	Pm-147	Co-60	Cs-137	Cm-244
1965**	12	5	0.4	150	5	--
1970**	90	15	5	7500	48	100
1980***	650	60	56	7500	620	180

* Could be increased to 25 kw in 1970 and 180 kw in 1980, but may result in a 50% increase in cost

** Based on AEC operations

*** Based on civilian nuclear power operations

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5. Biological Hazard

Absolute containment of the radioactive fuel is a basic requirement in this application and safety evaluation is primarily an assessment of the integrity of the fuel container when subjected to the possible conditions of the application. It is conceivable, however, that certain missions within the range envisioned for large milliwatt generators can be justified despite the finite possibility of loss of container integrity. In these cases, the safety evaluation must be extended to include assessment of the biological hazard of ingestion as well as radiation. A selection of data on permissible concentrations is presented in Table III-7.

Table III-7

Biological Hazard Data - Critical Organ

<u>Nuclide</u>	<u>Ingestion Hazard⁽¹⁾</u>		<u>Permissible Concentration, 168-hr week ⁽²⁾, $\mu\text{c/cc}$</u>	
	<u>Effective Metabolic Half-Life, days</u>	<u>Maximum Body Burden⁽²⁾, μc</u>	<u>Water</u>	<u>Air</u>
Sr-90	6400	2	10^{-7}	10^{-11}
Pu-238	23000	0.04	5×10^{-6}	7×10^{-14}
Pm-147	570	60	2×10^{-4}	2×10^{-9}
Co-60	9.5	10	3×10^{-5}	2×10^{-10}
Cs-137	138	30	2×10^{-5}	2×10^{-9}
Cm-244	6100	0.1	7×10^{-6}	3×10^{-13}

(1) "Report of ICRP Committee II on Permissible Dose of Internal Radiation (1959)", K.Z. Morgan, W.S. Snyder, and J.A. Auxier (editors) Health Physics, 3, 380 pp (June 1960).

(2) Title 10 CFR Part 20 for general public exposure

Any radioisotope heat source would exceed the permissible concentration in all media by orders of magnitude if released. The biological hazard associated with uncontained Sr-90 and Pu-238 sources is relatively more severe than with Pm-147; therefore, Pm-147 may be the attractive nuclide for applications where containment could be lost.

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6. Summary

Of the nine radioisotopes under development, six are potential candidates for large milliwatt RTG applications; however, only the three reference nuclides, Sr-90, Pu-238 and Pm-147, warrant consideration at this time.

Among the three reference nuclides:

- Sr-90 is the least expensive, even including shielding costs
- Pu-238 is the leading choice for applications requiring minimized weight
- Pm-147 is attractive where the relative biological hazard is of significance

For the three tabled nuclides:

- Co-60 requires biological shielding that is much heavier than required for Sr-90 and pricing for desired quantities and specific activities is not stable at this time
- Cs-137 could become attractive only if the demand for Sr-90 exceeded availability, which does not appear probable
- Cm-244 requires a biological shield too heavy to make the nuclide competitive with Pu-238 and it is too expensive a fuel to compete with Sr-90

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B. FUEL FORM

In this discussion, fuel form denotes the form of the fuel in the encapsulation. Fuel forming includes conversion to the fuel compound and densification and loading of that material in the capsule or capsule liner. Processes conducted to create the radioisotope and purify it by separation from other activities and contamination (in preparation for fuel forming), are referred to as isotope production.

There are several fuel forms of Sr-90 and Pu-238 for which the forming processes are established, and Pm-147 fuel forms will achieve this status in six to twelve months. Property data and features for all the established, and nearly qualified, fuel forms of the three reference radioisotopes are summarized in Table III-8.

1. Strontium-90

A large number of strontium compounds and mixtures have been examined as fuel*. It can be stated with reasonable confidence that only the four compounds currently available warrant consideration for application in a large milliwatt RTG. These are the reference fuel, strontium titanate, SrTiO_3 ; a similar material, Sr_2TiO_4 ; the oxide, SrO ; and the fluoride, SrF_2 . As shown in Table III-8, the isotopic and cationic purity remain essentially unchanged by fuel forming.

a. Strontium Titanates

Strontium oxide and titania form a spectrum of coordination compounds. Although the titanate formulas are usually written SrTiO_3 and Sr_2TiO_4 , more correct formulas are, respectively, $\text{SrO} \cdot \text{TiO}_2$ and $(\text{SrO})_2 \cdot \text{TiO}_2$, or in general, $(\text{SrO})_x \cdot \text{TiO}_2$. The highest melting (1910°C) and least soluble material is the reference fuel form at $x = 1$, but the

* MND-P-2977, Production of Strontium Titanate Radioisotope Fuel for SNAP-7B Thermoelectric Generator, Justin L. Bloom, April 15, 1963.

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TABLE III-8 - FUEL FORMS

RADIOISOTOPESr-90Pm-147Pu-238NUCLEAR DATA

Half-life, yr	27.7	2.62	86.4
Curies per watt	148	2788	30.6
Curies per gram of pure isotope	142	928	16.8
Estimated isotopic purity, %	50	100	80
Decay per year, %	2.48	23.2	0.74
Major source of heat	Beta	Beta	Alpha
Literature reference	MND-P-2977	BNWC-45	DP-984

FUEL FORM DATA

Fuel compound	SrTiO ₃	Sr ₂ TiO ₄	SrF ₂	SrO	Metal	Pm ₂ O ₃	Metal	PuO ₂
Melting point, °F	>3400	>3400	>2500	>4300	>1900	>4100	>1100	>4100
Crystalline density, gm/cc	5.11	5.00	4.24	4.0	7.3	7.3	~16	11.46
Estimated chemical purity, %	95	95	95	95	95	95	99	99
Specific power*, w/gm	0.22	0.28	0.32	0.39	0.32	0.27	0.44	0.39
Reference forming process	Hot-press	Step-press	Step-press	Hot-press	Cast	Step-press	Cast	Fired
Estimated packing factor	0.90	0.70	0.90	0.90	95	75	100	75
Power density*, w/cc	1.0	0.90	1.2	1.4	2.2	1.5	7.0	3.5
Thermal conductivity, Btu/hr-ft-°F	~3	~3	< 1	~3	~8	~1	~20	~1

* Computed from data shown

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di-strontium titanate ($x = 2$) melts at 1885°C and it too is fairly insoluble. At $x < 1$ the melting point and power density decline rapidly and above $x = 2$, the material begins to take on the characteristics of SrO , such as an affinity for water and CO_2 . The chemical stability of $\text{SrO}\cdot\text{TiO}_2$ has been proven in several multi-year power applications. When made and stored at Quehanna in AISI Type 316 stainless steel containers, $(\text{SrO})_2\cdot\text{TiO}_2$, like $\text{SrO}\cdot\text{TiO}_2$, showed no change or interaction with its container after one year; however, the fuel was kept relatively cool so that this storage does not truly represent evidence of stability at generator temperatures.

The starting material for all Sr-90 is SrCO_3 received from the Hanford works and then dissolved in HNO_3 . The titanates are formed by co-precipitation of SrCO_3 with finely-divided TiO_2 , followed by heating in air to remove CO_2 . The final product is $(\text{SrO})_x\cdot\text{TiO}_2$, but the value of x is dependent upon the ratio of Sr and Ti in the original precipitation. Since the Sr:Ti input ratio cannot be precisely controlled in a hot cell, the product is not exactly reproducible.

The dried product is ball-milled, or ground by some other means, to form a semi-powdery material that is ready for densification by cold-, hot-, or step-pressing.

Both titanate forms yield regular-shaped pellets when cold-pressed with non-radioactive materials, whereas almost all Sr-90 pellets become distorted during sintering. This may be due to the less precise control possible in a hot cell in comparison to a laboratory. Also, all Sr-90 pellets crack and slough off dust, and even good pellets made with Sr-90 will break up with time. Because of the distortion and fragile nature of cold-pressed pellets, a large diametral clearance (gap) must be left in the capsule to facilitate loading. This and the inherent axial gaps between pellets destroy much of the advantage gained through forming dense pellets by cold-pressing and sintering.

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At ORNL, SrTiO_3 is hot pressed into undistorted pellets, but Sr_2TiO_4 is considered insufficiently stable to warrant further development. The hot cell system for hot-pressing is relatively new and measurements are not available, but ORNL anticipates fuel densities of greater than 95% of theoretical for SrTiO_3 .

At Quehanna (Martin Co.), both titanate forms can be step-pressed in the capsule or liner. Although densification is less complete with step-pressing (70 to 75% of theoretical density) than with cold-pressing and sintering (80 to 90% of theoretical density) this process eliminates the radial and inter-pellet gaps, yielding a higher power density with respect to the capsule cavity, and it improves heat transfer.

Until recently, the only fuel form listed for Sr-90 was the titanate. This fuel was selected for the SNAP-7 series of RTG mainly because of three features:

- 1) Relatively low solubility and solubility rate in fresh and sea water,
- 2) Chemical stability in air at all temperatures up to its melting point and compatibility with nickel-, cobalt-, and iron-based alloys (Hastelloy C has been most often used for encapsulation and is the reference material here), and
- 3) Formation by a relatively simple hot cell process.

Whereas the second and third advantages listed for the titanates are of utmost importance (and achievable in other Sr-90 fuel forms as well), the first advantage appears to have less significance than originally assumed. The solubility rate of radioactive SrTiO_3 in sea water is several orders of magnitude greater than for the container material, Hastelloy C (based on long-term tests being conducted by the International Nickel Co. on Hastelloy C and on measurements with fully radioactive SrTiO_3 by

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ORNL). In addition, fuel exposed to the ocean would be dispersed by the current whether dissolved or sloughed off. Consequently, nuclear safety is dependent upon container integrity only; the rate of solubility of the fuel is of minor significance.

Since the titanates have a relatively low power density and lack exact reproducibility of the fuel form, and since the fuel solubility rate is of secondary importance, other fuel forms are also considered for this application.

b. Strontium Fluoride

The only naturally-occurring, simple-structure compound of strontium is the fluoride, SrF_2 . Of all the potential heat source forms, including the oxide, it is thermodynamically the most stable, thus SrF_2 is an obvious compound for consideration.

The Martin Co. developed and fully qualified the fuel forming processes for the fluoride* and has equipped the Quehanna facility for its preparation and encapsulation. The conversion process is similar to that for the titanate, but somewhat easier to accomplish. The product is an ionic-bond compound and is exactly the same regardless of variations in operator technique or reagent ratios.

The dried material is ball-milled and step-pressed directly into a capsule or capsule liner (sintering is not required). The loaded fuel density is 90 to 92% of the crystalline density (by comparison, the titanates can be step-pressed to only 70 to 75% of their crystalline density).

The power density of the fluoride form lies midway between that for the oxide and the titanate. It is compatible with air and with nickel-, iron-, and cobalt-based encapsulation materials up to its

* MND-3062-9 (classified)

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melting point (about 1400°C) which is sufficiently high for this application. The solubility of SrF_2 is an order of magnitude greater than for SrCO_3 so that liquid waste losses are higher; however, since liquid wastes constitute only a small part of the total Sr-90 wastes from fuel forming, this loss is negligible.

c. Strontium Oxide

ORNL recently installed and checked out hot-pressing equipment in its Fuel Product Development Laboratory (FPDL). With this advance in hot cell fuel forming, it now prepares SrO fuel, the compound with the highest crystalline power density of any Sr-90 fuel form. This breakthrough in densification processing may also have application in the preparation of Pm_2O_3 at BNWL.

The conversion process for the oxide is somewhat similar to the titanate in that the carbonate is precipitated and calcined to drive off CO_2 and water. During cooling of the oxide, however, an inert atmosphere must be maintained to prevent re-absorption of CO_2 and water. The product is ground and loaded into the graphite dies of the hot-press, but a platinum foil is first placed in the die which then encases the fuel during hot-pressing, greatly limiting its exposure to air. The foil-wrap remains with the pellet during encapsulation.

The major advantages of SrO are its high crystalline power density and melting point. Its major disadvantage is an affinity for CO_2 and water (normal impurities in air), making the purity of the product dependent upon operator technique. By providing for the exclusion of air from the product, the fuel forming process is somewhat more complicated than required for SrF_2 , but the product is attractive for heat source application.

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TABLE III-9

LINER CAVITY POWER DENSITY FOR SR-90 FUEL FORMS

<u>Compound</u>	<u>Densification Process</u>	<u>Initial Power, w</u>	<u>Fuel Diameter, in.</u>	<u>Liner Cavity Volume, in.</u>	<u>Liner Cavity Power Density, w/cc</u>
SrTiO ₃	cold-press	15	1.108	20.0	0.750
		30	1.390	38.4	0.781
	step-press	15	1.150	19.6	0.766
		30	1.440	38.4	0.781
	hot-press	15	1.073	16.8	0.894
		30	1.345	32.7	0.918
Sr ₂ TiO ₄	step-press	15	1.088	16.6	0.904
		30	1.360	32.3	0.930
SrF ₂	step-press	15	1.025	13.9	1.08
		30	1.283	27.2	1.10
SrO	hot-press	15	0.955	12.7	1.18
	(with 0.01 in. foil)	15	1.213	25.3	1.19

An analysis was made of the available fuel forms of Sr-90 to compare their liner cavity power density. The analysis is outlined in Appendix III-A and the results tabulated in Table III-9.

Conclusions drawn from Table III-9 and other data are as follows:

- 1) Hot-pressed SrTiO₃ provides a reasonable improvement in cavity power density over previously used densification processes. There are apparently no operational problems in using this process at ORNL; therefore, it is the preferred process for SrTiO₃.
- 2) Sr₂TiO₄ provides only a slight advantage over SrTiO₃ in power density, but its stability and compatibility with encapsulation at elevated temperatures is not resolved.

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3) SrF_2 provides an appreciable increase in power density over SrTiO_3 . It has been studied more thoroughly than any other Sr-90 fuel form with respect to compatibility with encapsulation materials at elevated temperatures and found to be satisfactory in Hastelloy C. It is the most reproducible fuel form of Sr-90. There may be a problem in obtaining declassification for specific applications.

4) SrO provides the highest power density. It is a new form and has not been tested as thoroughly under application conditions as SrTiO_3 or SrF_2 . It may prove time consuming to obtain a license for an application.

In summary, the major features for the four forms of Sr-90 are:

SrTiO_3 :	Greatest amount of operational and licensing data, but the lowest power density
SrTiO_4 :	A similar material, with slightly higher power density than SrTiO_3 , but its stability is unproven in comparison to the other available forms; this form should not be considered further
SrF_2 :	Excellent chemical stability and relatively high power density
SrO :	The highest power density in a Sr-90 fuel, but less proof of reproducibility and compatibility with encapsulation materials than for SrTiO_3 and SrF_2

At the present time, Quehanna (Martin Co.) is prepared to make and densify SrTiO_3 , Sr_2TiO_4 or SrF_2 by step-pressing, and ORNL is prepared to make and densify SrTiO_3 and SrO by hot- or cold-pressing. Both sites prefer to use double encapsulation, i.e., seal the fuel in a thin-walled liner and then, after the outer surface is decontaminated,

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seal the liner in the capsule. In the usual scheme, the outer capsule is the ultimate container of the fuel for safety evaluation, but the inner liner is accepted by the user to facilitate loading by the fuel supplier. For all Sr-90 fuel forms, the recommended encapsulation material for both the liner and capsule is Hastelloy C.

2. Plutonium-238

Investigation at Mound Laboratories and other sites identified several compounds and mixtures of Pu-238 as promising heat sources. Of those available now, the metal and oxide are of interest to this application.

Savannah River Plant prepares Pu-238 fuel material which is converted at Mound to either the metal or oxide fuel form. Mound then assembles the heat source by double encapsulation of the fuel. The recommended liner materials are tantalum for the metal and molybdenum or tungsten alloys for the oxide. For either fuel form, Hastelloy C is the recommended outer capsule material in this application.

Plutonium metal, cast in the appropriate shape, is the preferred fuel in heat source applications because of its higher power density. Its range of application is limited to hot junction temperatures less than 1000°F because of its low melting point and highly corrosive nature as a liquid. Since the oxide is compatible with its container material to at least 1800°F, it must be prescribed for applications at 1000°F and above.

Helium pressure buildup in a Pu-238 heat source requires special consideration. For this application, design criteria are selected to coincide as closely as possible with the encapsulation experience accumulated at Mound Laboratories. On this basis the encapsulated fuel system includes the following:

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- 1) An outer container (capsule) compatible with any potential external combinations of fluid, temperature and pressure, and of sufficient strength to withstand impact, explosion and vibration at high temperature and internal pressure. The capsule provides for the containment of the fuel.
- 2) An inner liner that is compatible with the fuel and capsule over the full range of potential temperature, and that will facilitate fuel loading and container sealing.
- 3) A frame of small volume designed to maintain the fuel at one end of the liner but provide a migration path for helium from the fuel to the empty section of the system.

It should be noted that operational advantage would be gained through omission of the liner to reduce system weight and temperature differential, or, some advantage would be gained by locating the thin liner outside the capsule to eliminate the limitations on capsule material. These considerations will be studied in more detail for future units, but criteria for this study are adopted which are consistent with current Mound Laboratory practices.

The encapsulation dimensions which include provision for helium containment are determined by the methods shown in Appendix III-B. For the metal fuel form a cast pellet is assumed. In the case of the oxide fuel, a pellet is assumed to be formed by mixing high-fired PuO_2 particles with finely divided molybdenum or tungsten powder which is then pressed and sintered.

3. Promethium-147

Battelle Northwest Laboratories is responsible for development of fuel forms and encapsulation techniques for Pm-147. Although no fuel forms have been put into use, BNWL recently fueled each of four capsules with

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10 gm (approximately 3 w) of Pm_2O_3 to demonstrate the process and display the product. By the end of fiscal 1966 they intend to complete the forming of 2 kg of Pm-147, primarily as the oxide, but possibly some as the metal. The melting point of either form appears high enough for application over the full fuel temperature range in thermoelectric generators.

The oxide, Pm_2O_3 , is formed by precipitating purified Pm-147 with oxalic acid and heating to drive off CO_2 and CO. Since the oxide was first made in macro-quantity only recently, its compatibility in air is not well known, but can be deduced from long-term studies of the oxides of its neighboring elements, neodymium (Nd) and samarium (Sm). From such analysis it was concluded* that the oxide will slowly absorb CO_2 and H_2O , being less reactive than SrO , but more so than SrF_2 ; it can be densified by hot-, cold- or step-pressing, without the use of a protective foil, but it should not be stored in air for long periods. The favored liner materials are rhenium or iridium, but molybdenum is almost as good, and Hastelloy C is acceptable up to 1100°C . Since fuel temperatures as high as 1100°C are not anticipated for this application, Hastelloy C is selected for both the liner and capsule with Pm_2O_3 .

The metal is formed by converting the oxide to the fluoride, followed by reduction by lithium. Like plutonium, the metal has a higher power density and thermal conductivity than the oxide, and is corrosive in the molten state. Tantalum appears to be the best liner material, but zirconium appears also to be nearly as good and Hastelloy C is considered fair. Restrictions on the use of conventional liner materials may offset the thermal advantages of the metal.

An analysis of Pm-147 fuel forms, similar to the Sr-90 analysis, was made to compare the liner cavity power density. The analysis is outlined in Appendix III-C and the results are tabulated in Table III-10.

* Private communications with BNWL.

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TABLE III-10

LINER CAVITY POWER DENSITY FOR Pm-147 FUEL FORMS

<u>Compound</u>	<u>Densification Process</u>	<u>Initial Power, w</u>	<u>Fuel Diameter, in.</u>	<u>Liner Cavity Volume, in.</u>	<u>Liner Cavity Power Density, w/cc</u>
Pm ₂ O ₃	hot-press	15	0.896	9.25	1.62
		45	1.280	26.97	1.67
	step-press	15	0.958	11.30	1.33
		45	1.368	32.88	1.37
	high-energy compaction	15	0.836	7.52	1.98
		45	1.209	22.70	1.98
Pm metal	casting	14	0.838	7.56	1.98
		45	1.195	21.97	2.05

Conclusions drawn from Table III-10 are as follows:

- 1) It is too early in the development program to adequately compare the potential densification processes for Pm₂O₃; however, in the interim, hot-pressing is suggested as the reference process.
- 2) Since there is a similarity in power density between the oxide and metal, the oxide is receiving greater attention now at BNWL, and both materials melt high enough to be applicable throughout this program, the metal should be eliminated from this program.

4. Summary

There are three fuel forms of Sr-90 recommended for large milliwatt RTG applications: SrTiO₃ (hot-pressed), SrF₂ (step-pressed), and SrO (hot-pressed). The final selection for a specific use will depend upon licensing requirements, classification of material, total costs of delivered fuel, and the importance of fractional improvement in power density to limit system weight. Each of these forms is compatible with nickel-, iron-, and cobalt-based alloys over the temperature range of interest. Since Hastelloy C

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has been welded in hot cells in the presence of these fuel forms, and has performed well in air and hydrospace applications, it is recommended as the primary container material (in contact with the fuel) for all three forms as well as for the outer capsule.

Plutonium metal (cast) is recommended for applications where the capsule surface temperature is less than 1000°F , and PuO_2 (high-fired particles) is recommended for higher temperatures. Tantalum is recommended for primary (liner) encapsulation of plutonium metal, but molybdenum or tungsten are preferred for PuO_2 . Hastelloy C is recommended as the capsule material.

Hot-pressed Pm_2O_3 is the recommended form for all systems within this study fueled with promethium. Data are incomplete, but Hastelloy C appears acceptable for the primary, as well as secondary, encapsulation material.

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C. PERFORMANCE AND RELIABILITY OF ENCAPSULATED FUEL

In the first two parts of this section on radioisotope heat sources, attention was directed towards selection of components, i.e., the radioisotope, the fuel form and the encapsulation material. The emphasis in this part is on performance and reliability.

The heat source in the generator is used simply to provide specified amounts of thermal energy to the hot shoe of the thermoelectrics for a specified period. Strictly on a functional basis, sources are acceptable which meet the thermal requirements and do not adversely affect size, weight and cost limitations of the systems; however, safety requirements override any functional requirements. The ultimate hazards associated with a radioisotope heat source are radiation exposure and ingestion of radioactive materials.

Radiation protection is provided in the generator by the use of biological shielding and by limiting approach to the source by encasing the system in an outer container of high integrity. These components can be designed to withstand most accidents and destruction by vandalism. Where component design cannot adequately prevent a potential over-exposure, mission management must avoid the condition.

Probably the most severe hazard would be dispersion of the radioactive material in the atmosphere, ground or ocean. The maximum permissible concentrations (MPC, see Table III-7) for the nuclides of interest to this application are so low that dilutions as high as 10^{15} in water and 10^{22} in air are required for dispersed fuel. Since such dilutions are impossible to assure, containment of the radioactive material is mandatory.

It is obvious that the requirement for containment is adequate resistance to accidents and conditions that would tend to rupture the container. The encapsulation must be strong enough to withstand external forces and internal helium pressure (for alpha-emitter fuel like Pu-238),

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must be chemically compatible with the fuel and external environment, must prevent diffusion into and through its walls, and meet these requirements over the entire range of potential temperatures and pressure. Hastelloy C has proven satisfactory over the range of conditions applicable to this program. Of almost equal importance is the fact that Hastelloy C has been handled, welded and decontaminated by remote operation in hot cells with excellent result. Other materials, such as Haynes 25, may become equally qualified, but the significant point is that a fully satisfactory encapsulation material is now available for the three reference radioisotopes. Whereas, Hastelloy C can be used in direct contact with Sr-90 and Pm-147 fuels, a liner of tantalum must be used with plutonium metal and of a molybdenum-, or tungsten-based alloy with PuO_2 .

The final point of interest is reliability of containment. Since Hastelloy C meets the basic requirements, design of the capsule shape and size to gain acceptable integrity is a straight-forward engineering task. The final weld (made in a hot cell or glove box after the fuel is loaded) must be evaluated by nondestructive testing or by inference from similar non-radioactive specimens made under the same conditions which can then be fully examined. Either way, or both, there is no absolute proof that the weld is without voids, occlusions and unacceptable stresses.

This problem has been of major concern to radioisotope fuel developers since the start of the SNAP program. Mound Laboratories developed the procedure for welding process certification and evaluation by inference. Leak checking, swipe testing, and observation with magnification are also employed. Battelle Northwest Laboratories are working on ultrasonic testing to augment the above tests. Still, the proof of weld quality is not absolute.

Multiple encapsulation has been proposed, in addition to the above tests, as means of achieving acceptable reliability. The basic reasoning appears sound, but there is danger that security may be assumed

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without a gain in reliability, if multiple, or double, encapsulation is accepted without fuller explanation. In regard to the heat source application, single encapsulation is preferred because it affords the least number of gaps for a temperature differential and usually represents the least weight, therefore there must be an increase in reliability for multiple containment to be worthwhile. If weld integrity is the weakest point in containment reliability and the second container can be added and welded without reducing the reliability of the first weld, and if the added container meets all other containment requirements, then the added encapsulation will increase the reliability of containment.

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IV. SHIELDING

The milliwatt generator design objectives requires that shielding be provided to achieve radiation levels of 200 mrem/hr at the surface of the generator and 10 mrem/hr at one meter from the surface for Sr-90, Pm-147, and Pu-238 sources. Additional consideration was given to Sr-90 sources by exploring partial shielding in which half the total thickness of the shield was considered to determine the effect upon the radiation environment. Partial shield studies for Sr-90 further consider the effect of half the total shield weight upon radiation environment. The radiation environment from Sr-90 was also considered without shielding.

To satisfy the requirements specified for the radiation environment, parametric variations of the system outside diameter were investigated, using such shield materials as iron, lead, mallory 2000, and depleted uranium. for variations in source power over the range of 100 mw(t) to 100 w(t). Sources included for this application were Pm_2O_3 , SrTiO_3 , PuO_2 and Pu metal. Over the range of source power and shield dimensions considered, the criteria of 200 mrem/hr at the package surface was the controlling requirement that specified shield thicknesses and not the 10 mrem/hr at a meter requirement. Sources composed of larger dimension or shielding materials of a lower density than were considered for this application (such as water shielding) would, under some conditions of higher power levels, have resulted in the dose rate at a meter being the controlling criteria. However, impetus was placed upon achieving minimum weight configurations which may be best achieved where smaller thicknesses of high density shield materials are employed.

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A. SOURCE DATA

Initial investigations were directed toward the achievement of 10 mrem/hr at one meter from the surface for the purpose of generating a quantity of preliminary data for input into system optimization codes, with the intent of subsequent revision and refinement complying with the controlling surface radiation environment criteria. Isotopic sources considered for this phase of the study included Pm_2O_3 , SrTiO_3 , and PuO_2 . The data used to describe these sources is presented in Tables IV-1 through IV-4.

Table IV-1

Gamma Emission Rate of Pm_2O_3^*

Energy, Mev	Gamma Emission Rate, photon/w-sec	
0.75	1.7×10^7	(Pm-147 photon)
0.45	1.7×10^7	(Pm-146 photon)
0.25	3.1×10^8	(Pm-147 Brems.)
0.15	8.7×10^8	(Pm-147 Brems.)
0.13	2.3×10^{10}	(Pm-147 Brems.)

* Personal Communication with Pm-147 developers at Hanford.

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Table IV-2

Gamma Emission Rate of SrTiO₃*

Energy, Mev	Gamma Emission Rate, photon/w-sec	
2.00	2.082×10^7	(Brems.)
1.70	3.027×10^8	(Brems.)
1.40	1.650×10^9	(Brems.)
1.10	6.103×10^9	(Brems.)
0.80	1.958×10^{10}	(Brems.)
0.50	6.612×10^{10}	(Brems.)
0.25	1.705×10^{11}	(Brems.)

Table IV-3a

Gamma Emission Rate of PuO₂**

Energy, Mev	Gamma Emission Rate (T = 5 yr decay), photons/(gm PuO ₂)-(sec)
0.04 - 0.5	2.0×10^8
0.5 - 1.0	4.8×10^5
1.0 - 2.0	9.9×10^3
2.0 - 3.0	1.2×10^5
3.0 - 5.0	1.3×10^2
5.0 - 7.0	2.2×10^1

* E.D. Arnold, Handbook of Shielding Requirements and Radiation Characteristics of Isotopic Power Source for Terrestrial, Marine and Space Application, ORNL-3576, April 1964.

** D.H. Stoddard and E.L. Albenesius, The Radiation Properties of Pu-238 Produced for Isotope Power Generators, D B - 984, July 1965.

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Table IV-3b

Neutron Emission Rate of PuO₂*

Energy, Mev	Neutron Emission Rate, neutrons / (gm PuO ₂) (sec)
0 - 0.5	3.4×10^2
0.5 - 1.0	7.0×10^2
1.0 - 2.0	5.4×10^3
2.0 - 3.0	9.7×10^3
3.0 - 4.0	4.3×10^3
4.0 - 5.0	4.5×10^2
5.0 - 6.0	7.2×10^1
6.0 - 7.0	2.4×10^1
7.0 - 8.0	1.7×10^1
8.0 - 10.0	6.3×10^0
10.0 - 13.0	3.1×10^0
Total	2.1×10^4 Estimated as 55% of total calculated neutron activity of 3.8×10^4

Table IV-4a*

Gamma Activity of Pu-238 Metal (81% Pu-238)

Energy, Mev	Abundance, photons/(sec) (gm Pu-238 metal)			
	decay - 1d	decay - 1 yr	decay - 2.5 yr	decay - 5.0 yr
0.04 - 0.5	2.4×10^8	2.4×10^8	2.4×10^8	2.3×10^8
0.5 - 1.0	3.7×10^5	3.8×10^5	4.4×10^5	5.5×10^5
1.0 - 2.0	2.4×10^3	2.8×10^3	4.6×10^3	8.3×10^3
2.0 - 3.0	8.1×10^2	9.8×10^3	5.2×10^4	1.4×10^5
3.0 - 5.0	1.6×10^2	1.6×10^2	1.6×10^2	1.5×10^2
5.0 - 7.0	2.6×10^1	2.6×10^1	2.6×10^1	2.5×10^1

* D.H. Stoddard and E.L. Albenesius, The Radiation Properties of Pu-238 Produced for Isotope Power Generators, D B - 984 July 1965

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Table IV-4B*

Neutron Activity from Pu-238 Metal

<u>Energy,</u> <u>Mev</u>	<u>Abundance,</u> <u>neutron/(sec)(gm Pu-238 metal)</u>
0.0 - 0.5	2.5×10^2
0.5 - 1.0	3.3×10^2
1.0 - 2.0	6.7×10^2
2.0 - 3.0	4.1×10^2
3.0 - 4.0	2.0×10^2
4.0 - 5.0	1.5×10^2
5.0 - 6.0	8.1×10^1
6.0 - 7.0	2.7×10^1
7.0 - 8.0	1.9×10^1
8.0 - 10.0	7.1×10^0
10.0 - 13.0	3.5×10^0

* D. H. Stoddard and E. L. Albenesius, The Radiation Properties of Pu-238 Produced for Isotope Power Generators, D P - 984, July 1965

In the case of Pm_2O_3 the higher energy gamma emission results from Pm-146 gammas and the lower energy from the Pm-147 bremsstrahlung X-ray group. The SrTiO_3 radiations are from bremsstrahlung X-rays.

Primarily an alpha emitter, PuO_2 does emit both neutrons (from α n reactions and spontaneous fission) and gamma rays (from both Pu-238 and contaminants). The total neutron emission rate of PuO_2 has been estimated** at an activity of 2.1×10^3 n/(sec)(gm PuO_2). The neutron emission rate from Pu-238 metal was estimated at 3.8×10^4 n/(sec)(gm-238 Pu metal)**

** E.D. Arnold, Handbook of Shielding Requirements and Radiation Characteristics of Isotopic Power Sources for Terrestrial, Marine, and Space Application, ORNL-3576, April 1964

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Even though Pu-238 metal is characterized by a continuous spectrum of γ emissions, the major intensities are 0.16 Mev and 0.8 Mev. In addition, a 2.7 Mev gamma is emitted by thallium, a daughter of Pu-236 which is a contaminant. Actually, the contribution of the 2.7 Mev γ emission varies with time as the thallium approaches equilibrium (Table IV-4a). Integrating over a 10-yr period results in an average intensity corresponding to that at 5-yr decay.

For this application there are specific advantages in selecting Pu-238 metal. It was noted that in using Pu-238 metal the neutron contribution is only generally 10 to 12% of the gamma contribution where shielding such as 0.1 in. uranium is employed to diminish the 0.16 Mev contribution. As the uranium shield increases in thickness, the neutron contribution increases compared to the gamma contribution until at approximately 2 in. of uranium the neutron and gamma contributions are equal. The neutron contribution of PuO_2 on a watt basis is approximately 12 to 13 times more intense than for the Pu metal due to (α, n) reactions when 0.1 in. uranium shielding is used. As the uranium shield thickness increases, the relative neutron contribution increases compared to the gamma. It is immediately apparent that substantial reductions in the radiation environment may be realized where Pu-238 metal is selected with its reduced neutron component.

B. SHIELD OPTIMIZATION

Analysis was conducted to evaluate the gamma dose rate both at the surface and at one meter using the following expression.*

$$\phi_{\gamma} = \frac{B S R_o^2}{2(a + Z) k} F(\theta, b_2) \quad (\text{IV-1})$$

* A. Foderaro and F. Obenshain, Fluxes from Regular Geometric Sources, WAPD-TN-508, June 1955

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where

- ϕ_γ = gamma ray dose rate, mr/hr
 S_v = volume source strength, Mev/cc-sec
 R_o = radius of source material, cm
 B = dose buildup factor
 a = distance from the surface of the source to the detector, cm
 Z = effective self-absorption distance of equivalent line source, cm
 k = conversion factor from Mev/cm²-sec to mr/hr

$$F(\theta, b_2) = \int_0^\theta e^{-b_2 \sec \theta'} d\theta'$$

- θ = $\tan^{-1} \frac{h}{2(a+Z)}$
 h = height of source cylinder, cm
 b_2 = $\mu_i t_i + \mu_s Z$
 μ_i = macro-cross section of shield material i, cm⁻¹
 μ_s = macro-cross section of source material, cm⁻¹
 t_i = thickness of the i-th shield material, cm

The results of these calculations are presented in Figures IV-1 through IV-11 at the end of this section. Figures IV-1 and IV-2 depict variation in gamma dose rate with shield thickness for 100-w Pm₂O₃ and SrTiO₃ sources. Figure IV-3 and IV-4 show the variation of source power with shield thickness to satisfy the criteria of 10 mr/hr at one meter. In the case of Pm₂O₃, iron shielding was considered in addition to lead, mollybdenum, and uranium.

It was observed that the value of the mass absorption coefficient for iron was a factor of from 2 to 10 lower over the energy range of 0.75 to 0.13 Mev compared to uranium or lead, which represents substantially poorer shield effectiveness for iron. In Figure IV-4 for SrTiO₃, the maximum shield

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diameter space limitations of 5 and 10 in. are depicted. Figure IV-5 describes the gamma dose rate from a 100-w shielded Pm_2O_3 source as a function of distance. This curve, specifically calculated for Pm_2O_3 , may represent any source whose diameter is small compared to the shield outside diameter. It is observed that with 10 mr/hr at a meter, a surface located 23 cm from the surface yields a 200 mr/hr dose rate, but since the applications of this study are weight- and size-limited, all will fall inside of 23 cm, resulting in a controlling surface dose rate.

C. SURFACE DOSE RATES

Figure IV-6 describes the Pm_2O_3 shield thickness requirement, varying with source power level to satisfy the surface criteria of 200 mr/hr where the surface is defined at a diameter of 4 1/2 in. Uranium, mallowry 2000, lead, and iron were considered as shield materials. Figures IV-7 and IV-8 depict similar materials. The surface was defined by a 10 in. diameter for Figure IV-7, and 5 in. for Figure IV-8.

Figures IV-9 and IV-10 describe the unshielded surface dose rates versus source power variations for SrTiO_3 and PuO_2 respectively. The surface was defined by 3.0- and 5.0- in. diameters in the PuO_2 case and by a 5 in. diameter in the SrTiO_3 case.

Figure IV-11 presents the effects upon surface radiation level of using half the uranium shield thickness required to achieve the 200 mr/hr criteria at the surface of a 7-in. diameter shield. Corresponding half shield thickness for source power level variations are shown. For the 72.5-w case, a further calculation was conducted to establish the thickness corresponding to half the weight of the shield. The results of the surface dose rate for the half weight value are described in Figure IV-11 also.

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FIGURE IV-1. SHIELDED GAMMA DOSE RATES AT ONE METER FOR A 100-w Pm_2O_3 SOURCE

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FIGURE IV-2. SHIELDED GAMMA DOSE RATES AT ONE METER FOR A 100-w SrTiO_3 SOURCE

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FIGURE IV-3. Pm_2O_3 SOURCE SHIELD THICKNESS REQUIREMENTS FOR 10mr/hr AT ONE METER

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FIGURE IV-4. SrTiO_3 SOURCE SHIELD THICKNESS REQUIREMENTS FOR 10mr/hr AT ONE METER

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FIGURE IV-5. DOSE RATE FROM A 100-w Pu_2O_3 URANIUM-SHIELDED SOURCE VERSUS SEPARATION DISTANCE FOR A 0.66 cm URANIUM SHIELD

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FIGURE IV-6. Pm_2O_3 SOURCE SHIELD THICKNESS REQUIREMENTS AT THE SURFACE FOR A DIAMETER OF 4 1/2 in.

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FIGURE IV-7. SrTiO_3 SHIELD THICKNESS REQUIREMENTS AT THE SURFACE FOR A DIAMETER OF 10 in.

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FIGURE IV-8. SrTiO_3 SOURCE SHIELD THICKNESS REQUIREMENTS AT THE SURFACE FOR A DIAMETER OF 5 in.

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FIGURE IV-9. SrTiO_3 UNSHIELDED SURFACE DOSE RATE VERSUS POWER LEVEL (DIAMETER EQUALS 5 in.)

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FIGURE IV-10. PuO_2 UNSHIELDED SURFACE DOSE RATE FROM NEUTRONS VERSUS POWER LEVEL (DIAMETER EQUALS 3.0 AND 5.0 in.)

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FIGURE IV-11. SrTiO_3 HALF SHIELD THICKNESS VERSUS SURFACE DOSE RATE (DIAMETER EQUALS 7 in.)

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V. THERMAL INSULATION

The great cost of radioisotope energy increases the incentive to use high performance thermal insulation and thus absolutely minimize parasitic heat losses from the generator.

A. APPLICATIONAL CRITERIA

The unique criteria for the use of thermal insulation with milliwatt generators are related to radiation damage resistance and non-evolution of gaseous disassociation products.

1. Radiation Tolerance

The performance of the thermal insulation system must not be degraded by long-term exposure to nuclear radiation from the heat source. A secondary requirement is that gaseous disassociation products resulting from radiation bombardment or high temperature operation must be avoided, since appreciable release of gaseous products would increase conduction heat losses and the increased internal pressure could lead to rupture of the housing.

Radiation tolerance is inherent in inorganic materials, therefore the usual high temperature insulations prepared without organic binders are suitable for nuclear applications.

2. Mechanical Properties

The insulation system must not change characteristics after exposure to shock and vibration. This requirement excludes consideration of powdered, granular, or short fiber insulations which are subject to compacting.

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Depending upon the particular design chosen, the thermal insulation system may be used as a matrix for the thermoelectrics, and provide mechanical support as well as electrical isolation. These provisions necessitate the use of a solid thermal insulation capable of bearing moderate loads.

A low-density insulation resulting in reduced overall system weight is also a consideration.

3. Thermal Properties

The insulation system must be capable of operating at temperatures up to 1700°F (for SiGe thermoelectric systems) and have the minimum practical thermal conductivity.

The thermal conductivity of all insulation systems is reduced by operation in a vacuum since the gaseous conduction heat transfer mode is eliminated or minimized (depending on the degree of vacuum maintained). Alternatively, the performance of an insulation system can be improved by back-filling with a low thermal conductivity gas, such as argon or xenon. Besides providing a chemically inert atmosphere, these gases have thermal conductivities appreciably lower than air or nitrogen.

The maximum operating temperature, and the thermal conductivity values for available high performance insulation systems considered for the development of advanced large milliwatt radioisotope thermoelectric generator systems, are tabulated in the following section.

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B. SURVEY OF HIGH TEMPERATURE INSULATIONS

To ensure specification of the most suitable thermal insulation system for the milliwatt generator designs, the available high temperature thermal insulations were surveyed and pertinent data were tabulated and reduced to graphical form.

Data sheets containing product descriptions and chemical and physical properties of interest are included in Appendix V-A. This appendix also lists conversion factors for thermal conductivity units, and a brief bibliography of reports pertaining to high temperature insulation materials.

1. Thermal Properties

Table V-1 at the end of this section lists the thermal conductivity and maximum operating temperature of proprietary products, solid, liquid and gaseous materials, and elements considered for thermal insulation systems. Figure V-1 shows thermal conductivity as a function of mean operating temperature for those products and elements considered most applicable for the milliwatt generator system.

Figure V-2 presents the "apparent" thermal conductivity for a typical Linde Super Insulation system over the temperature interval between the temperature shown on the abscissa and ambient. The term apparent is applied because the thermal conductivity of such insulation depends on the boundary temperatures, the number and diameter of the fibers separating the shields, the number of shields, the separation of the shields, the type of residual gas, the value of the vacuum, and the surface pressure that compresses the insulation system. Because each custom-designed Super Insulation system will have different characteristics, the conductivity values shown by the curve are to be considered "typical". A design must be based on data supplied to the vendor for the specific application.

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FIGURE V-1. THERMAL CONDUCTIVITY OF AIR, XENON, ARGON AND SEVERAL HIGH TEMPERATURE INSULATIONS

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FIGURE V-2. THERMAL CONDUCTIVITY OF A TYPICAL LINDE HIGH TEMPERATURE SUPER INSULATION SYSTEM
(Low Side at Ambient Temperature)

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2. Availability of Low Thermal Conductivity Inert Gases

To provide a chemically inert, low thermal conductivity environment for an RTG heat source, a noble gas-fill or vacuum must be maintained in the voids of the thermal insulation. As the maintenance of a high vacuum over long periods of time appears impractical, the lowest thermal conductivity inert gases, argon and xenon, are prime candidates for the application. Xenon has an appreciably lower thermal conductivity, as shown in the table below, but at an appreciably higher cost.

THERMAL CONDUCTIVITY OF THREE GASES

<u>Gas</u>	$\frac{\text{Btu}}{(\text{hr})(\text{ft}^2)(^\circ\text{F}/\text{ft})}$			
	<u>300°F</u>	<u>600°F</u>	<u>900°F</u>	<u>1100°F</u>
Air	0.0202	0.0264	0.032	0.0356
Argon	0.0135	0.0174	0.0208	0.0224
Xenon	0.0043	0.006	0.0075	0.0086

These inert gases both have thermal conductivities appreciably lower than air, and the thermal conductivity of xenon at 600°F is $\frac{(0.006)(100)}{(0.017)} = 35.2\%$ that of argon. Both argon and xenon are readily available from the Matheson Company*, and the Air Products and Chemicals Company**.

The lowest purity (99.995% min.) and cost argon is cataloged in small quantities by Matheson as follows: (specific volume is 9.67 ft³/lb at 70°F and 1 atm.)

* Nearest Sales Office and Plant address is:

Matheson Company
P. O. Box 188
Phone (415) 793-2559
Newark, California 94560 (Southern Alameda County)

** Nearest stocking point is:

Air Products and Chemicals Company
Specialty Gas Department
P. O. Box 123
Forest Park, Illinois (near Chicago)

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COST OF 99.995% ARGON

<u>Argon Cylinder Size</u>	<u>Cylinder Pressure , psig</u>	<u>Contents , ft³</u>	<u>FOB Price , \$ (less cylinder deposit)</u>
1A	2200	245	32.60
2	2000	87	22.25
3	2000	35	14.25
4	2000	12	8.90
Lecture Bottle	1800	2	1.75

Xenon is only available in "research" grade and the price includes the cylinder cost of \$3.50 which will be refunded upon return. The gas purity is 99.995% minimum at the following prices:

COST OF "RESEARCH" GRADE XENON

<u>Xenon Cylinder Size</u>	<u>Cylinder Pressure, psig</u>	<u>Contents, ft³</u>	<u>Price, \$</u>
7B	625	.883	625
7A	430	.530	375
7	220	.247	210

The Air Products and Chemical Company catalog data for argon and xenon are as follows:

CATALOG DATA FOR ARGON

<u>Cylinder Size</u>	<u>Cylinder Pressure at 70°F, psig</u>	<u>Contents, ft³</u>	<u>lb.</u>	<u>FOB Price, \$ (less cylinder)</u>
A	2400	307	32	34.75
B	2265	256	27	30.50
C	2015	84	9	18.00
D-1	2015	35	4	14.00
D	2015	15	1.5	10.50
Lecture Bottle	1800	1.7	0.2	1.50

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CATALOG DATA FOR XENON

<u>Cylinder Size</u>	<u>Cylinder Pressure, psig</u>	<u>Contents, ft³</u>	<u>FOB Price, \$</u>
X-100	510	3.53	2,300.00
X-50	285	1.76	1,200.00
X-25	145	.88	625.00

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C. SELECTION FACTORS

From the information available and for mean temperatures of 500°F and above, the most suitable solid thermal insulation system for the milliwatt applications appears to be evacuated MIN-K 1301. If it is not possible to maintain a vacuum because of other design considerations, the flushing and backfilling of this insulation with xenon will provide an appreciably lower thermal conductivity than can be obtained with ambient air.

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TABLE V-1

THERMAL CONDUCTIVITY OF HIGH TEMPERATURE INSULATION AND GASES

<u>Material</u>	<u>Mean Temp. °F</u>	<u>Btu/(hr)(ft²) °F/ft</u>	<u>Max. Operating Temperature, °F</u>	<u>Reference</u>
I. PROPRIETARY PRODUCTS				
1. MIN-K 1301 (20 lb/ft ³)	300 400 600 800 1000	0.0166 0.0175 0.0192 0.0208 0.0225	1300	Johns-Manville
2. MIN-K 501 (10 lb/ft ³)	200 300 400	0.0117 0.0125 0.0133	500	Johns-Manville
3. MIN-K F-100-10 (10 lb/ft ³) (Flexible Form)	100 200 400 600 800	0.015 0.0167 0.0208 0.0242 0.0292	1200	Johns-Manville
4. MT-10	100 200 300 400	0.0216 0.0234 0.0267 0.030	800	Eagle-Picher
5. Thermax-MT Carbon Black	100	0.0332	--	R. T. Vanderbilt Co.
6. Micro-Quartz Felt (3 lb/ft ³)	200 400 600 800 1000 1200 1400	0.0216 0.0334 0.0465 0.059 0.0716 0.085 0.0965	2279	Libby-Owens-Ford
7. Feltmetal Type A (nickel)	200	0.36	--	Huyck Metals
8. Feltmetal Type A(302SS)	200	0.28	--	Huyck Metals
9. Mono Block	400	0.035	1200	Baldwin-Ehret-Hill
10. Thermasil	1200	0.038	1200	Baldwin-Ehret-Hill
11. Thermalite	350	0.036	600	Baldwin-Ehret-Hill
12. Eccospheres SI	0 300	0.03 0.04	2500	Emerson & Cuming
13. Dyna-Quartz (10 lb/ft ³)	600 800 1000 1200 1400 1600 1800 2000 2100	0.043 0.049 0.055 0.061 0.068 0.075 0.083 0.093 0.098	2750	Johns-Manville
14. Kaylo Block	350	0.0375	1200	Owens Corning
15. Santocell A	-100 0 100 200 300	0.0098 0.0115 0.0133 0.0153 0.0176	1300	Monsanto

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TABLE V-1 (Continued)

<u>Material</u>	<u>Mean Temp. °F</u>	<u>Btu/(hr) (ft²) °F/ft</u>	<u>Max. Operating Temperature, °F</u>	<u>Reference</u>
16. PV Supertemp Block	200 400 600 800 1000	0.030 0.0334 0.0391 0.045 0.053	1900	Eagle-Picher
17. Foamglas (9 lb/ft ³)	0 50 300	0.029 0.0316 0.046	800	Pittsburgh-Corning
18. Dyna-Flex (8 lb/ft ³)	1000 1500	0.067 0.112	2700	Johns-Manville
19. Refrasil Batt A-100 (6 lb/ft ³)	600 800 1000 1200 1400	0.0375 0.046 0.054 0.063 0.075	2000	H. I. Thompson
20. Solami	500 700 900 1100 1300	0.0731 0.0833 0.095 0.107 0.120	1800	Solar Aircraft
II. <u>SOLIDS</u>				<u>Reference*</u>
1. Laminated Asbestos (40/in.)	100 200 300 400 500	0.033 0.037 0.040 0.044 0.048	700	3 3 3 3 3
2. Laminated Asbestos (20/in)	100 200 300 400 500	0.045 0.050 0.055 0.060 0.065	500	3 3 3 3 3
3. Magnesia (85% - 13 lb/ft ³)	100 200 300 400	0.034 0.036 0.038 0.040	600	3 3 3 3
4. Glass Wool	70 600	0.010 0.048	600	9 9
5. Aluminum Foil (7 air spaces/2.5 in.)	70 1000	0.023 0.543	1000	9 9
6. Diatomaceous Earth	70 1000	0.038 0.068	1600	9 9
7. Rock Wool	100 200 300 400 500 600	0.030 0.034 0.039 0.044 0.050 0.057	--	3 3 3 3 3 3
III. <u>GASES</u> (atmospheric pressure)				
1. Air	32 212 392 572 620	0.0140 0.0183 0.0226 0.0265 0.02692		1 1 1 1 2

* Sources of this information are listed at the end of this table.

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TABLE V-1 (Continued)

<u>Material</u>	<u>Mean Temp., °F</u>	<u>Btu/(hr)(ft²) °F/ft</u>	<u>Max. Operating Temperature, °F</u>	<u>Reference</u>
1. Air (Continued)	710	0.02862		2
	800	0.03022		2
	980	0.03339		2
	1160	0.03628		2
2. Argon	32	0.0095		3
	212	0.0123		3
	392	0.0148		3
	572	0.0171		3
	621	0.0173		5
	975	0.0214		5
	1342	0.0248		5
3. Helium	0	0.0784		2
	32	0.0818		3
	200	0.0977		2
	400	0.114		2
	600	0.130		2
	800	0.145		2
	1000	0.159		2
	1200	0.172		2
4. Nitrogen	32	0.0139		3
	212	0.0181		3
	392	0.0220		3
	572	0.0255		3
	752	0.0287		3
5. Xenon	32	0.0029		4
	212	0.0040		8
	392	0.00497		8
	572	0.00591		8
	752	0.00685		8
	932	0.00779		8
	1112	0.00866		8
6. Krypton	32	0.0051		4
	80,5	0.0054		5
	270	0.0069		5
	621	0.0096		5
7. Neon	32	0.0268		5
	80,5	0.0281		5
	270	0.0338		5
	621	0.044		5
IV. LIQUIDS				
1. Freon-12 (CCl ₂ F ₂)	32	0.0559		7
	104	0.0469		7
	167	0.0392		7
	200	0.035		7
2. Freon-114 (C ₂ H ₂ F ₄)	32	0.0515		7
	104	0.0424		7
	167	0.0344		7
	200	0.030		7
3. Transformer Oil	86 to 212	0.103		6

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VI. POWER CONDITIONING

A good universal power conditioner could considerably simplify the design problems of a thermoelectric generator system. Theoretically, the thermoelectric design would be that indicated as best by the reliability, efficiency and mechanical considerations of the heat source and thermoelectric converters.

Unfortunately, such a conditioner has yet to be devised. The minimum voltage requirements of semiconductors limit the freedom to merely parallel the thermal elements; efficiency of conditioning decreases with decreasing the thermal elements in the search for increased reliability; efficiency of conditioning decreases with decreasing input voltage; temperatures significantly above room temperature decrease operational lifetime; and radiation damages the semiconductors. In short, the various parameters of a power conditioner must be evaluated from the viewpoint of an overall system to derive an optimum thermoelectric generator system design.

A. POWER CONDITIONER FUNCTION

The basic function of the power conditioner is to transform the output electrical power of the thermoelectric converter into a more useful form of electrical power. In most systems, this means transforming low voltage, high current power into a higher voltage lower current power. Other functions may include matching the ultimate load to the electrical generator for greatest efficiency; simplifying the design and manufacture of the thermoelectric converter; providing the power regulation; and removing excess heat from the thermoelectric elements in the form of electrical power.

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B. METHODS

Power conditioning is not a new art. For various reasons, however, the classical methods of conditioning (such as rotary converters, vibrators and the more recent SCR static inverters) are not satisfactory for use with low power thermoelectric generators. Consequently, new techniques have been developed for this type of application. The new static inverters operate by "chopping" the d-c input to produce a-c and then apply transformers to derive higher voltages. Two basically different semiconductors have been used to perform this chopping function: tunnel diodes and transistors. Tunnel diodes are useful below approximately 0.5v and transistors from approximately 0.5v and upwards. A literature search generally indicates that at 0.5v or more, transistor circuits exhibit the higher efficiencies.

At the same time, the complexity of the transistor circuits is usually greater than that of the tunnel diode circuitry. Despite the greater complexity of transistor circuits with attendant reliability implications, the higher efficiency factor of the transistors indicates the desirability of their use. It has been demonstrated that thermoelectric outputs of approximately 0.5v result in usable but low efficiency conditioning. Requiring 3v, however, does not unduly affect the construction costs, efficiency, or reliability of the system's electrical generator. Accordingly, the data provided for the parametric analysis of the Large Milliwatt Generator Program is based upon transistor technology.

1. Typical Circuitry

In general, the simplest and most efficient circuit for a dc-to-dc converter is a magnetically coupled multivibrator. This circuit requires a transformer to produce a voltage step-up, two switches to alternately control current flow in the primary winding of the transformer, and a rectifier circuit to restore the converter output waveshape to a dc state.

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In a typical circuit (such as Figure VI-1) transistors operate only as switches and serve the same function as the switch contacts of a mechanical vibrator. Once the switching section has been initiated, the energy required to operate the transistor switches is derived from a feedback winding of the transformer itself, thereby making it self-sustaining. Since the transistor switches control current flow on a go-no go basis, maximum use of the transformer core can be realized if the core material can be readily saturated with the current that is controlled by the transistor switches. In this way the core flux can be alternately driven to positive and negative saturation as first one switch and then the other closes. During the switching action an alternating square wave is induced in the transformer windings that allows, with a proper turns ratio, a higher square wave voltage to be induced on the output side of the transformer. This square wave is then rectified by diodes to produce a dc voltage at a higher voltage level than was supplied by the thermoelectric source.

FIGURE VI-1. TYPICAL CONVERTER CIRCUIT

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The detailed operation of the typical circuit shown in Figure VI-1 can best be understood by reference to the transformer hysteresis loop curve shown in Figure VI-2.

FIGURE VI-2. TRANSFORMER HYSTERESIS

Assume transistor Q_2 is off and Q_1 is starting to conduct, and that previous conditions have caused the transformer to be in negative saturation and at point A of Figure VI-2.

As transistor Q_1 starts to conduct, the core flux moves from point A to point B and then toward C. This core flux change induces a voltage in all the transformer windings so that the base voltage of the conducting transistor Q_1 becomes more positive and the base voltage of transistor Q_2 becomes more negative.

As the base voltage of Q_1 is made more positive, Q_1 is driven into heavy current conduction thereby causing the core flux to move,

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at a relatively constant rate, from point B to beyond point C where the core goes into positive saturation. At core saturation, further increases in the collector current of Q_1 does not increase the core flux.

Since the core flux remains comparatively constant when it is saturated and since induced voltages are given by Equation VI-1

$$e = N \frac{d\phi}{dt} \times 10^{-8} \quad (\text{VI-1})$$

where:

e = induced voltage

N = number of turns

ϕ = core flux

the induced voltage in the windings drops to zero during saturation, thus removing the base drive voltage to the conducting transistor Q_1 , thereby causing it to turn off.

When Q_1 turns off, the collector current flowing through winding N_{1a} to Q_1 stops, causing the core flux to decrease towards the point D; however, as the core flux moves toward point D, a voltage is induced in the windings with such a polarity that the base voltage of transistor Q_1 becomes negative and the base voltage of Q_2 becomes positive. Transistor Q_1 is thus driven further into cutoff, transistor Q_2 is driven into conduction, and the core flux is driven from point D to point E and towards negative saturation.

Once negative saturation occurs, the state of the transistors is again reversed and the first cycle repeats.

Since the core flux changes at a relatively constant rate between points B and C and between points E and F and since the induced voltages depend on the core flux time rate of change, the output voltage from the transformer will be a square wave.

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2. Loss Mechanisms

To achieve maximum reliability and maximum efficiency with a simple circuit, careful attention must be paid to the number and the ratings of the components that are used.

In an optimum design the number of dissipative components (such as resistors, capacitors, diodes, and inductors) must be kept to a minimum, and where possible eliminated, since power is dissipated by these components as waste heat, thereby reducing the overall converter efficiency.

The typical circuit shown in Figure VI-1 uses the minimum number of components necessary to effect the required operation, and it is only in the optimum use of these components that power savings, and consequently higher efficiencies, can be realized.

In general, the power losses which must be minimized and that are common to any converter design consist of the following specific losses:

- 1) Transistor losses (P_a)
- 2) Transformer losses (P_b)
- 3) Copper losses (P_c)
- 4) Resistor losses (P_d)
- 5) Rectifier losses (P_e)

The efficiency of the power conditioner then is given by Equation VI-2:

$$\eta\% = \frac{P_1 - P_t}{P_1} 100 \quad (\text{VI-2})$$

where:

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P_l = power input

P_t = total converter losses = $P_a + P_b + P_c + P_d + P_e$

a. P_a Transistor Losses

Since the switching transistors have three distinct states (on, off, and switching), different mechanisms are responsible for the losses that occur during each of these three states. The choice of transistors then is never the elimination of one loss mechanism but rather the optimum combination to minimize the sum of all three.

The losses that occur during the switching transitions between the conducting and blocking states can be quite high especially if the converter is also operating at a high frequency.

During the on or conduction state, especially if the operating frequency is low, the major power loss is due to normal forward conduction dissipation. This loss can be expressed as

$$P_{al} = I_c V_{cc(sat)} + I_b V_{be} \quad (VI-3)$$

Since I_c is the collector current which in turn is a function of the output power requirements and can only be changed slightly, reduction of conduction losses is restricted to minimizing the saturation voltage V_{cc} of the transistor used. At present this loss can be minimized only by using germanium transistors. The ultimate reduction is limited by the state-of-the-art of transistors and effort should be expended in this direction since it is this saturation voltage that ultimately sets the lower limit of converter input voltage.

The second term in the equation $I_b V_{be}$ is the base drive power that causes the transistor to conduct current through the transformer. Normally this power loss mechanism is deliberately made large to

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ensure positive switching. Care must therefore be directed toward the achievement of the best compromise between drive power and acceptable losses.

During the off or non-conducting state, transistor losses are very low and, except for ensuring that external circuit current leakage is eliminated, little gain can be expected from minimizing this source of loss.

The losses that occur during the transition time between the conducting and non-conducting state can be particularly high at high converter operational frequencies. The switching losses can be expressed as

$$P_{a2} = \frac{2}{3} [2 V_1 I_1 f(t_r + t_f)] \quad (\text{VI-4})$$

where:

V_1 = fixed output thermoelectric voltage

I_1 = required converter input current (collector current)

f = converter square wave repetition frequency.

From the above equation (VI-4) and term definitions it can be seen that significant reductions in switching power losses can be realized only by reducing the transistor rise time delay t_r and fall time delay t_f . The transition time is dependent upon the transient qualities of the base switching signal, the transistor parameters, and the load current. In general, the path taken in going from point A to point B of the curve shown in Figure VI-2 will determine the switching losses since, in essence, it determines the switching time delay. Further, the path is primarily dependent on the transistor load circuit.

Clearly then, for any given frequency, maximum efficiency can be realized by using a core material which has a very square hysteresis curve, thus minimizing the time it takes to go from point A to B.

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b. P_b Transformer Losses

Since the flux within the core of the transformer is undergoing periodic reversals, there will be a loss of power in the magnetic circuit. This loss has two components, eddy current and hysteresis losses.

The hysteresis loss (P_{bl}) is incurred in re-orienting the core magnetic domains to coincide with each flux reversal and is an energy loss that appears as heat in the core. This loss can be expressed

$$P_{bl} = K_h f B_m^{1.6*} \quad (VI-5)$$

where:

K_h = constant that depends on the core material

f = the frequency of the flux reversals

B_m = the saturation flux density

For a fixed converter operational frequency, an increase in the converter efficiency gains can only be realized in the selection of the core material and the saturation flux density.

In this connection, high saturation flux density material requires less core volume and less winding copper. While this may be ideal for maximum miniaturization and high output voltages, it is clear from the equation that, for low converter output power levels, the core material can become a significant loss if the saturation flux density B_m is not carefully matched to the application.

* Value of exponent empirically derived by Steinmetz.

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The eddy current losses (P_{b2}) are caused by currents circulating in the core material. The eddy currents induce voltages in the core which are converted into heat as I^2R losses. Eddy current losses can be expressed

$$P_{b2} = K_e f^2 B_m^2 \quad (\text{VI-6})$$

where:

K_e = a constant that depends on the thickness of the transformer laminations, the permeability, and the conductivity of the core material

The previous comments on hysteresis losses apply to eddy current losses except that, in this instance, the eddy current losses vary as the square of the frequency and the saturation flux density, and are therefore more critical.

Sufficient studies by other investigators have determined that at least two suitable core materials are available for converter applications. These are: 50% NiFe grain-oriented alloy called Orthonol and 79% non-oriented alloy called HyMu-80.

c. P_v Copper Losses

In any transformer design, a suitable choice must be made concerning the distribution of losses between core losses and winding copper losses. This choice will always be a compromise since the requirements for the number of turns in the transformer winding is in direct conflict with the saturation flux density and size requirements of the core. In other words, a decrease in copper losses results in an increase of core losses, and conversely.

Maximum gain, and consequently maximum efficiency, can be realized only through proper selection of the tradeoffs of all transformer conflicting requirements.

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d. P_d Resistor Losses

Ideally, resistors would not be used in a low power converter, since these components function properly only by dissipating power. In practical circuits, however, there is usually one resistor in the transistor base circuit so that the base current can be adjusted to optimize the operation of the transistor pair for the correct collector current and, consequently, the correct output power. This requirement means that the base current is correct only for a given set of input voltage and output load conditions. Ideally, the circuit should be designed so that the base current is always proportional to the collector current and, although one such circuit arrangement has been reported in the literature*, more work is required in this area.

e. P_e Rectifier Losses

Since the load circuit requires a d-c output from the converter, rectifiers must be provided to restore the transformer output square wave to a d-c state. This restoration is achieved at the expense of a power loss in the rectifiers due to the forward voltage drop necessitated by the diode semiconductor material for proper operation.

Power losses from the source can be minimized by selecting the best available diode.

3. Efficiency

In performing the parametric analysis for the overall system, an important consideration was variation of power conditioner efficiency as a function of input voltage. Unfortunately, there appears to be no natural function which specifically determines efficiency vs input voltage results. For specific designs, limits can be ascribed due to individual

* DC to DC Converter Operates at Low Input Voltage, Goddard Space Flight Center, GSFC-130, Duke University.

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components and configurations; however, based upon results obtained from many different circuits designed for varying power levels, it is possible to construct a reasonable curve for evaluation of tradeoffs. This curve is not identical with the results obtained by any investigator, but does provide a guide which assumes no breakthrough in state-of-the-art and only moderate gains due to improved components. Inputs of 0.5 to 18v were considered in deriving the following equation from a compilation of the results of other investigators:

$$\eta = e^{-0.323 \left(\frac{1}{V_1}\right)^{0.553}} \quad (\text{VI-7})$$

The curve described by this equation is shown in Figure VI-3. To reiterate, this curve is not the curve one would derive from a specific conditioner with an input voltage varying between 0.5 and 18v; it is a plot of maximum efficiency one could expect if a specific input were chosen and the conditioner designed for that voltage.

It is generally well known that optimum power transfer occurs when the load impedance (Z_L) is equal to the generator internal impedance (Z_g), i.e., match ratio = 1, and that for this condition the voltage across the load is equal to one-half of the generator open circuit voltage. To achieve maximum efficiency of the thermoelectric generator, the match ratio $\left(\frac{1 \text{ load impedance}}{\text{generator impedance}}\right)$ must consider the thermoelectric material and the temperature conditions in the generator.

In practice, however, it is advantageous to use match ratios other than 1; hence, the term mismatch is commonly used. The reason for mismatch is to optimize the thermoelectric conditions over the lifetime of the radioisotope powered system. The following discussion will clarify the details of optimized mismatch.

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FIGURE VI-3. VARIATION OF EFFICIENCY WITH INPUT VOLTAGE

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a. Load Voltage Dependence

A schematic diagram of an RTG and its load is shown below. For simplicity the internal impedance and open circuit voltage of the many thermoelectric elements (connected in a series or a series-parallel matrix) are lumped into Z_g and E_g blocks respectively (this will have no effect on the conclusions reached concerning load voltage and power).

FIGURE VI-4. RTG SCHEMATIC

Power Conditioner
input voltage (V_1) = generator open circuit voltage (E_g) -
voltage drop in generator ($I_1 Z_g$)

$$V_1 = E_g - I Z_g = E_g \left(\frac{E_g}{Z_g + Z_l} \right) Z_g = E_g \frac{Z_l}{Z_g + Z_l} \quad (\text{VI-8})$$

Let M = mismatch ratio = $\frac{Z_l}{Z_g}$

$$\text{Then } V_1 = E_g \frac{M Z_g}{Z_g + M Z_g} = E_g \frac{M}{1 + M} \quad (\text{VI-9})$$

This function is labelled "load voltage" in Figure VI-5 and when $Z_l/Z_g = 1$, the load voltage is seen to be one-half of the

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FIGURE VI-5. EFFECT OF LOAD RESISTANCE ON POWER AND VOLTAGE TO LOAD
(For actual power multiply by $E_G^2/4R_G$; for voltage, by E_G)

REF ID: A66011

generator open circuit voltage (E_g). For RTG applications in which the load impedance (Z_l) is generally considerably greater than the generator internal impedance (Z_g), the load voltage is seen to increase moderately with increasing load impedance.

b. Load Power Dependence

The power developed in the generator load (loaded power conditioner) is:

$$P_1 = I_1^2 Z_l = \left(\frac{E_g}{Z_g + Z_l} \right)^2 Z_l = E_g^2 \frac{Z_l}{(Z_g + Z_l)^2} \quad (\text{VI-10})$$

For maximum power in the load,

$$\frac{d P_1}{d Z_l} = 0 = \frac{E_g^2 (Z_g - Z_l)}{(Z_g + Z_l)^3} \quad (\text{VI-11})$$

from which

$$Z_l = Z_g \quad (\text{VI-12})$$

Therefore, for maximum power into the load, the impedance of the load must be made equal to the internal impedance of the generator. For this condition

$$P_{1(\max)} = \frac{E_g^2}{4 Z_g^2} = \frac{E_g^2}{4 Z_l}$$

and the efficiency of power transfer is 50% (half of the generated power is dissipated in the generator, half in the load). The maximum power obtainable from a particular generator is seen to be inversely proportional to its internal impedance.

The dependence of load power on load impedance is obtained as follows:

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$$\text{Let } M = \frac{Z_l}{Z_g} \text{ (mismatch ratio)} \quad \text{VI-13}$$

$$\begin{aligned} \text{Then } P_1 &= E_g^2 \frac{Z_g}{(Z_g + M Z_g)^2} = E_g^2 \frac{M Z_g}{[Z_g (1+M)]^2} \\ &= \frac{E_g^2}{Z_g} \frac{M}{(1+M)^2} \end{aligned} \quad \text{(VI-14)}$$

This function is labelled "load power" in Figure VI-5. The maximum is seen to occur when $Z_l = Z_g$. What is particularly significant for RTG applications is the relatively weak functional dependence on load impedance for values of $Z_l/Z_g > 1$. Therefore, larger thermoelectric elements which are easier to handle and fabricate and which will be more reliable under shock environments may be considered (depending on the load impedance) even though they will result in a lower generator impedance. Parallel element arrays to improve reliability will also decrease generator impedance but again should be considered even though they may result in a $Z_l/Z_g > 1$.

In an actual RTG maximum power and $0.5 E_g$ conditions will occur at a Z_l/Z_g ratio slightly greater than one because of the non-linear voltage-current characteristic at low current levels.

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C. RELIABILITY

Data pertaining to the reliability of the most sensitive components in the power conditioner are not readily available. Furthermore, reliability is not confined to that of the components themselves but must include the factors introduced in fabrication and packaging; environmental factors will then further influence the expected lifetime. In the absence of a specific design, reliability can be discussed only in general terms.

1. Transformers

Careful design and selection of insulation can produce a transformer which would perform successfully for more than 5 years. No advances in technology are required for this component to achieve the required dependability.

2. Semiconductors

Semiconductors represent a moderate problem in reliability. They can fail because of faulty mechanical construction, migration of materials in the junctions, high impurity content, transient voltages, overload conditions, improper fabrication techniques during device assembly, and harsh environments. Sufficient degrees of control exist to reduce all factors, except those of the semiconductor itself, to an acceptable level. Accordingly, the major gains in reliability must be provided by the manufacturer of the selected semiconductor or by a selection system employed by the conditioner fabricator.

The circuits being considered use two types of semiconductors: transistors and diodes. Of these, transistors have been shown to be more reliable primarily because the junctions are "grown" or diffused as opposed to welded. Typical transistor ratings* (commercial grade) are 0.05% failures/1000 hr when derated to 1/2 and operated at low temperatures. The Minuteman

* T. Peter Sylvan, "Unijunction Device Gets High Marks in Stringent Tests of Reliability", Electronics 14 June 1965, p 98.

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program was able to obtain SCR's with a rating of 0.004% failures/1000 hr by careful specification and quality control. Unijunction transistors are reported to exhibit failure rates of 0.01%/1000 hr.

Diodes have been the major source of difficulty in the past. The unreliability can be attributed to the quality of the "cat's whisker" joint. Newer types of diodes are using grown construction and should exhibit reliability equal to that of transistors.

Further reliability increases can be achieved by individual selection within a given type. This subject will be discussed further under Problem Areas, and Recommendations.

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D. PROBLEM AREAS

The greatest problem area in power conditioning is that of reliability. Except for the more complex designs, integrated or monolithic circuits with their demonstrated high reliability are not suitable in this application. Various manufacturers have instituted component reliability programs but do not yet have good data for all components. In the absence of specific data, generalized data must be used. Careful design, fabrication, packaging, and testing can eliminate many common causes of failure, but failures will still occur. To obtain highest reliability, the physics of failure must be understood. Industry is pursuing various programs to determine these factors.

Since time cannot be adequately scaled for the reliability period required in these applications, some test units must be monitored continuously. The most feasible way of gaining significant information on reliability, and the possible degradation of characteristics over the lifetimes anticipated, is to measure the change in operating parameters as a function of life during exposure to reference environments. Even a fairly comprehensive reliability program, however, will demonstrate little more than that the published reliability data for such equipment are conservative. Obviously, full-life test programs are not within the scope of this program.

In the meantime, ways must be devised to select components having the least susceptibility to failure.

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E. RECOMMENDATIONS

Many reliability programs have shown that acceptance-rejection criteria based solely on passing function performance specifications do not eliminate seemingly spontaneous failures. The devices merely perform better during their working lifetime. Accordingly, a selective method to produce greater reliability must derive information not obtainable by ordinary measurements of electrical parameters. One such method, thermography, has been reported in the literature.*

This method uses infrared sensors to scan the various components during test and to determine heat output and heat dissipation distribution. Since heat production is a basic cause of failure in electronic components, thermography is a sensitive technique for improving reliability. The method can sense very small temperature differences between identical components being tested and determine the effectiveness of heat transfer to heat sinks. Incipient failures which cannot be sensed in any other manner can be detected by thermographic methods. Development of this method is urged for application to the entire Large Milliwatt Generator Program.

* Infrared for Electronics Reliability, EDN, July 1965, p 42.

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VII. SYSTEM RELIABILITY

The reliability of a radioisotope thermoelectric generator (RTG) system is essentially controlled by the thermoelectric and power conversion subsystems. Both subsystems are composed of a large number of elements susceptible to failure from mechanisms not yet fully controllable; however, the reliability of the thermoelectric subsystem can be increased to the required level by a redundant-connection scheme. The required reliability of the power conversion subsystem must be achieved by conservative design in which components are operated well below their rating and by the use of high-reliability components developed for such critical applications as manned space capsules, communications satellites, and submarine cable repeaters.

It must be emphasized that conservative design and statistical analysis are only two elements of a high-reliability program. The third essential element is a carefully managed qualification test program which accurately simulates the ultimate operating environments.

A. SYSTEM RELIABILITY MODEL

A radioisotope generator system is composed of the following:

- Heat source
- Thermoelectric array
- Power conditioner (converter/regulator)
- Heat rejection/case subsystem
- Thermal insulation

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For successful operation these subsystems must all function within the design limits; therefore, the series-reliability model is applicable, and the system reliability is described mathematically as

$$R_{RTG} = (R_{HS}) (R_{TA}) (R_{PC}) (R_{HR}) (R_{TI}) \quad (VII-1)$$

where R = reliability,
and the subscripts:

RTG = radioisotope thermoelectric generator system

HS = radioisotope heat source subsystem

TA = thermoelectric array

PC = power conditioner

HR = heat rejection/case subsystem

TI = thermal insulation

From previous experience, and the inherent reliability of conservatively designed and thoroughly inspected static, low-operating-temperature elements, the reliability of the heat-rejecting case and thermal insulation subsystems is very high and can be considered essentially unity for the design lifetime. For practical purposes, therefore, the reliability function for the system reduces to

$$R_{RTG} = (R_{HS}) (R_{TA}) (R_{PC}) \quad (VII-2)$$

The reliability of these critical subsystems is discussed in detail in the following sections.

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B. RELIABILITY APPORTIONMENT

The target reliability goal for the RTG system is 95% reliability of operation for 18 months with a 90% confidence level.

Because of the importance of the integrity of a radioisotope heat source capsule this subsystem will be designed and tested for a reliability essentially equal to one for the design lifetime. The system minimum-design reliability is then

$$R_{RTG} = 0.95 = (1) (R_{TA}) (R_{PC})$$

As the effects of a failure in the thermoelectric array or the power conditioning subsystem are the same, the minimum-design reliability for these subsystems can be apportioned equally and

$$R_{TA} = R_{PC} = (0.95)^{\frac{1}{2}} = 0.9747$$

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C. RADIOISOTOPE HEAT SOURCE SUBSYSTEM RELIABILITY

The radioisotope heat source subsystem consists of a double-walled capsule that contains the fuel. The reliability of this capsule is affected by the operating temperature and, possibly, buildup of internal gas pressure. Unlikely external environments (such as corrosive agents, pressure and extreme heat) must also be accommodated.

The required high-reliability of the fuel capsule will be achieved by sophisticated design, exhaustive development of closure techniques, careful fabrication, and thorough inspection. Designing for high-reliability requires an appreciation for the statistical nature of material-strength and operating stress.

1. Design Strength Properties

The data found in a handbook or supplied by a vendor on the strength of materials are average or "mean" values obtained from the results of many standard tests. The designer must appreciate that any given sample of the material may deviate appreciably from these values because of difference in composition, manufacturing and fabrication. When high-reliability, light-weight mechanical assemblies are required (as for radioisotope fuel capsules), it is prudent to perform the significant mechanical property tests on the material-lot with which the capsules will be manufactured. Enough tests must be performed to define the statistical variation of properties about the mean values. The design must then be based on the lowest rather than the mean value of the physical properties.

2. Design Stress Values

The operating stress encountered by the fabricated assembly is also a statistical quantity. For example, the stress resulting from shock loading of the capsule as a result of dropping, etc., is highly dependent on the point of impact; the kinetic energy is more uniformly distributed in a broadside impact than in a corner hit.

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The maximum operating stress must be based upon sufficient shock and pressure tests to define the statistical variation function. A judicious safety factor must be applied to this maximum operating stress rather than the mean.

Figure VII-1 illustrates the stress and strength statistics as they affect the proposed high-mechanical-reliability design techniques. The results of many strength and strain tests (suitably normalized by stress calculations) are plotted, and analytical statistical techniques are used to define the distribution functions. The minimum-weight design for high-reliability is one in which no overlapping of the stress and strength functions occurs. A design safety factor is then applied to further separate the functions to accommodate such design unknowns as unanticipated loading stresses, corrosion mechanisms, etc.

The statistical approach to design strength and operating stress described above, which is based on physical property tests of the actual material-lot to be used and tests of the actual capsule geometry, will result in a minimum weight design with a known safety factor which can accommodate all material, fabrication and operational variations.

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FIGURE VII-1. STRESS AND STRENGTH STATISTICS AFFECTING RELIABLE MECHANICAL DESIGN

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D. THERMOELECTRIC ARRAY SUBSYSTEM RELIABILITY

Multiple thermoelectric junctions must be cascaded in an RTG to achieve a useful terminal-potential. At least 0.5 v is required to provide an efficient input for a power conditioner (dc transformer/regulator). To obtain this input value, tens of thermoelectric components are required. If an electronic conditioning device is not used, hundreds of thermoelectric components are required to produce desired load voltages.

Because the reliability of the thermoelectric array is controlled by the inherent reliability of the thermoelectric components and their method of interconnection, the reliability of the three basic interconnection modes is developed in the following subsections.

1. Simple Series-Connection

The simplest method of achieving a higher output voltage from the thermoelectric components is a simple series-connection of N and P junctions as shown schematically below:

a. Reliability Theory for Simple Series-Connection

The reliability of the overall array is the product of the probabilities of non-failure of each component (i.e., each must function for the array to be successful). Mathematically the reliability of the array (probability of non-failure during the time interval of interest) is,

$$R_S(t) = R_1(t) \cdot R_2(t) \cdot R_3(t) \dots R_n(t) = \prod_{k=1}^{k=n} R_k(t) \quad (\text{VII-3})$$

If all of the thermoelectric components are identical and independent then,

$$R_1(t) = R_2(t) = R_n(t) \equiv R_c(t) \quad \text{and} \\ R_S(t) = [R_c(t)]^n \quad (\text{VII-4})$$

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where

$R_S(t)$ = the reliability of the simple series-array

$R_c(t)$ = the reliability of the thermoelectric components

n = the number of thermoelectric components in series

If the thermoelectric components have a constant failure rate with time during the design-lifetime of the generator, which is the most reasonable assumption where failures are due only to random unknown and unpredictable mechanisms, then the exponential probability density function applies, and,

$$R_S(t) = e^{-n\lambda_c t} = e^{-\lambda_A t} \quad (\text{VII-5})$$

where

$R_S(t)$ = the reliability of the simple series-array

e = the base of natural logarithms

n = the number of thermoelectric components in series

λ_c = the failure rate for the thermoelectric components

λ_A = the failure rate for the array

t = the time interval of interest

and

$$\lambda_A = n\lambda_c$$

b. Quantitative Evaluation of Simple Series-Connection

The failure rate for the array is seen to be "n" times the failure rate for the individual components. This indicates why a system composed of many series-elements must have highly reliable components if the system is to achieve a reasonable reliability.

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The reliability of an array composed of identical series-elements of reliability R_c is shown in Figure VII-2. Component reliabilities in the range of interest were assumed in preparing this figure; the calculations cover up to 2000 series-components.

2. Parallel-Series Connection

The reliability of the simple series-connected thermoelectric string may be appreciably increased by a parallel-connection of strings as indicated schematically below:

For the condition in which "n" units are in each string, the terminal-potential for this array is the same as the simple series-connection; however, the number of individual couples is increased by the factor which is equal to the order of string-redundancy.

a. Reliability Theory for Parallel-Connected Elements

The reliability of a redundant array is developed as follows: consider several independent elements (series strings in this case) operating in a parallel arrangement shown schematically below.

FIGURE VII-2. RELIABILITY FOR ARRAY WITH IDENTICAL SERIES COMPONENTS

 $(R_c = \text{Component Reliability})$

Let the reliability of element i (probability that it will not fail in the time-duration of interest) be R_i . As the array will only fail if all of the elements fail, the reliability, R_p , is:

$$\begin{aligned} R_p &= 1 - (\text{unreliability})_p = 1 - (\text{probability of array-failure}) \\ &= 1 - (Q_1 \cdot Q_2 \cdot Q_3 \dots Q_i), \end{aligned}$$

where

$$\begin{aligned} Q_i &= \text{probability of element } i \text{ failure} \\ &= (1 - R_i) \cdot \text{Therefore,} \\ R_p &= 1 - (1 - R_1) (1 - R_2) \dots (1 - R_m) = 1 - \prod_{i=1}^m (1 - R_i). \end{aligned} \quad (\text{VII-7})$$

If all of the elements are identical then,

$$R_1 = R_2 = R_3 = R_m \equiv R_e$$

and

$$R_p = 1 - (1 - R_e)^m = 1 - Q_e^m \quad (\text{VII-8})$$

where

$$\begin{aligned} R_p &= \text{parallel-system reliability} \\ R_e &= \text{element (or series-string reliability)} \\ Q_e &= \text{element unreliability} = 1 - R_e \\ m &= \text{number of parallel elements} \end{aligned}$$

b. Quantitative Evaluation of Parallel-Connected Elements

The reliability of a system composed of identical parallel elements (or series-string) of reliability R_e is shown in Figure VII-3. Several element-reliabilities in the range of interest were assumed and the calculations cover up to seven parallel elements. The maximum percentage increase in system-reliability is seen to occur when one redundant

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FIGURE VII-3. RELIABILITY FOR ARRAY WITH IDENTICAL PARALLEL ELEMENTS

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string is added. Additional redundancy is much less effective. For example, for an element (or series string) reliability of 0.85, the addition of one redundant element yields a system-reliability of 0.9775, a 15% increase. The addition of a second redundant element to yield three parallel elements provides a system-reliability of 0.9966, only a 17.2% increase over the one element system. From economic considerations, therefore, redundancy beyond first order is not recommended. The cost of additional redundancy is better expended on increasing component-reliability.

c. Reliability Theory for Parallel-Series Connection

If the parallel elements are the series-string considered in the previous section each with a reliability R_e , where

$$R_e = (R_c)^n,$$

the reliability of the parallel-series array is,

$$R_{PS} = 1 - [1 - (R_c)^n]^m \quad (\text{VII-9})$$

where

- n = the number of components in a series-string
- m = the number of paralleled series-strings.

d. Quantitative Evaluation of Parallel-Series Connection

Numerical values for the parallel-series connection array for various assumed component-reliabilities are shown in the figures following the next section which pertains to the still more reliable series-parallel thermocouple-interconnection scheme.

3. Series-Parallel Connection

As shown previously, the reliability decrement associated with a series-connection of thermocouples may be ameliorated by a parallel-connection of the series strings; however, a very appreciable further increase in array-reliability can be achieved when a relatively large number

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of components are involved by parallel-connecting every thermocouple to form a series-connection of parallel units. The basic configuration is then

which

is equivalent to the following array requiring fewer electrical connections:

and this configuration has the same terminal-potential as the simple series-connection of "n" units or the parallel-connection of simple series-strings each with "n" units (the parallel-series connection).

a. Reliability Theory for Series-Parallel Connection

Consider several independent thermocouples connected in a series-parallel array as follows:

For each unit there are two possible paths of current-flow; for the complete array there are 2^n possible current-paths. For a given unit "i" to be operative, either of the two thermocouple components

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(a and b) of the unit must carry current. The probability that unit "i" is operative (the reliability function) is therefore

$$\begin{aligned} R_{ij} &= \text{probability (component "a" is operative or component "b" is operative)} \\ &= 1 - \text{probability (component "a" is not operative and component "b" is not operative)} \\ &= 1 - Q_{aj} Q_{bj} \end{aligned} \quad (\text{VII-10})$$

where:

$$\begin{aligned} Q_{aj} &\text{ is the unreliability function for the "a" component of unit "j"} \\ Q_{aj} &= 1 - R_{aj} \text{ (} R_{aj} \text{ is the reliability function).} \end{aligned}$$

As the units are independent, the array-reliability is the product of the unit reliabilities (from series-connection theory), or

$$R_{SP} = (1 - Q_{a1}Q_{b1}) (1 - Q_{a2}Q_{b2}) \dots (1 - Q_{an}Q_{bn}) = \prod_{j=1}^n (1 - Q_{aj}Q_{bj}) \quad (\text{VII-11})$$

If all of the thermocouple components are identical with a reliability R_c and an unreliability Q_c ($Q_c = 1 - R_c$) then

$$R_{SP} = \prod_{j=1}^n (1 - Q_c^2) = (1 - Q_c^2)^n \quad (\text{VII-12})$$

b. Quantitative Comparison of Interconnection Schemes

The superiority of the series-parallel connection scheme is clearly illustrated on Figures VII-4 through VII-7 which indicate the reliability of the series, parallel-series and series-parallel interconnection schemes as a function of the number of electrically effective components. Data for component-reliabilities of 0.960, 0.970, 0.980 and 0.990 are presented. This range of component-reliabilities covers most applications in which a system-reliability of > 90% is required and the

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FIGURE VII-4. RELIABILITY OF THERMOELECTRIC ARRAYS ($R_c = 0.960$)

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FIGURE VII-5. RELIABILITY OF THERMOELECTRIC ARRAYS ($R_c = 0.970$)

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FIGURE VII-6. RELIABILITY OF THERMOELECTRIC ARRAYS ($R_c = 0.980$)

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FIGURE VII-7. RELIABILITY OF THERMOELECTRIC ARRAYS ($R_c = 0.990$)

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number of series-components is from about 10 to 1000. If more than 1000 series-components is required, individual component-reliabilities of greater than 0.990 will be needed.

4. Reliability of Thermocouple Arrays With Failed Elements

The series-parallel connection arrangement for thermocouples also has a very marked advantage when the reliability of the various connection arrays is calculated for the situation in which components have failed.

The reliability of the three connection schemes will be compared below, considering failed elements, 100 equivalent series-components initially, and a component reliability $R_c = 0.9900$ ($Q_c = 1 - R_c = 0.0100$).

a) Single Series-String Connection

(1) Reliability assuming no component failures

$$R_S = (R_c)^n = (.9900)^{100} = \underline{\underline{0.3664}}$$

(2) Reliability assuming one component failure

$$R_S = \underline{\underline{0}} \text{ (chain is broken)}$$

b) Parallel-Series Connection

(1) Reliability assuming no component failures

$$\begin{aligned} R_{PS} &= 1 - (1 - R_S)^2 = 1 - (.6336)^2 \\ &= 1 - 0.4014 = \underline{\underline{0.5986}} \end{aligned}$$

(2) Reliability assuming one component failure

$$R_{PS} = \underline{\underline{0.3664}} \text{ (same as single series-connection with no failure)}$$

c) Series-Parallel Connection

(1) Reliability assuming no component failures

$$R_{SP} = (1 - Q_c^2)^n = (.9999)^{100} = \underline{\underline{0.9908}}$$

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(2) Reliability assuming one component failure

$$R_{SP} = R_c (1 - Q_c^2)^{n-1} = (.9900) (.9999)^{99} \\ = (.9900) (.9909) = \underline{\underline{0.9810}}$$

(3) Reliability assuming two components failed (not in same unit)

$$R_{SP} = R_c^2 (1 - Q_c^2)^{n-2} = (.9801) (.9999)^{98} \\ = (.9801) (.9910) = \underline{\underline{0.9713}}$$

(4) Reliability assuming three components failed (no two in same unit)

$$R_{SP} = (R_c)^3 (1 - Q_c^2)^{n-3} = (.9311) (.9999)^{97} \\ = (.9311) (.9912) = \underline{\underline{0.9229}}$$

This information is summarized in Table VII-1. The superiority of the series-parallel array when random component-failures are considered is clearly evident from this display. It is also evident that an array-failure resulting from two successive component failures in the same unit of the series-parallel array is a very low probability event ($\sim 1/200^2$). There is a 25% probability, however, that two components-failures will cause a system failure in the parallel-series connection (assuming that current flow in the string does not affect the failure rate). A single component failure obviously causes a system failure in the single series-string connection. Strictly speaking, these conclusions are only valid for very early component failures as the subsystem reliabilities used in the analysis are the initial values which decrease with operating time.

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TABLE VII-1

RELIABILITY OF THERMOCOUPLE CONNECTION ARRAYS CONSIDERING
FAILED COMPONENTS

(Assuming 100 series units, single component reliability = 0.990)

<u>ARRAY CONNECTION</u>		<u>ARRAY RELIABILITY</u>			
Number of Components failed:		0	1	2	3
Single Series		.366	0	0	0
Parallel-Series		.599	.366	.366* .366*	0** 0**
Series-Parallel		.991	.981	.971***.923***	
* Assuming failures in same string					
** Assuming failures in separate strings (two strings)					
*** Assuming failures in different units (100 total units)					

5. Series-Connection of Parallel-Series Sub-Arrays

Figure VII-4 through VII-7 indicated the greatly improved array-reliability to be had by cross-connecting the cold junction legs of every couple; however, it may be advantageous considering the ease of fabrication to cross-connect only parallel-connected sub-arrays as illustrated below:

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Such a connection scheme could be desirable when each sub-array is an easily fabricable mechanical module such as a wafer or bundle.

Depending upon the number of sub-arrays (cross-connections), the reliability of this connection mode will fall anywhere between the parallel-series array (curve b) and the series-parallel array (curve c) as shown on Figures VII-4 through VII-7.

6. Power Loss From Thermocouple Arrays with Failed Elements

The implication of failed elements must also be considered as to its effect on power delivered to the load. The increased reliability gained by a redundant connection of thermoelectric elements would be of no value if the device-power-output were to fall below a usable value. Fortunately, the series-parallel connection is relatively stable in this regard when a reasonable number of elements are cross-connected.

a. Simple Series-Connection

The power and voltage output from a simple series-connection of thermoelectric elements both fall to zero when one element fails in the open circuit-condition.

One short circuit can be tolerated in all connection modes if the remaining circuitry is not grounded.

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b. Parallel-Series Connection

The internal resistance of a parallel-series connected thermoelectric element-array will increase by a factor of two upon an open-circuit failure in one element. Assuming that the load was initially matched in resistance to the thermoelectric array (the optimum condition), such an increase in internal resistance will decrease the power to the load about 55% and decrease the load voltage 34% (see Figure VI-5).

It is to be noted that the effect of one thermo-electric element failure can be mitigated by initially operating the RTG with a load resistance much higher than optimum.

For example, with a load resistance such that $R_{Load} / R_{Generator} = 4$ (about 64% of the maximum potential power in the load), a single thermoelectric element-failure will result in only a 31% decrease of power delivered to a fixed load and a load voltage decrease of 17.5%. Of course, with this design a severe penalty is paid in lost efficiency during normal operation.

c. Series-Parallel Connection

A series-parallel connection of n thermoelectric elements will have the same internal resistance as a like number of parallel-series elements (note that no current flows between equipotential junctions).

Consider a series-parallel connected array with $2n$ total elements, each of internal resistance r and an open-circuit terminal-potential equal to n simple series-connected elements as follows:

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The terminal-resistance of this array is $R_G = (n) \left(\frac{r}{2}\right)$. For a single element-failure the internal resistance becomes $R_G = (n-1) \frac{r}{2} + r = (n+1) \left(\frac{r}{2}\right)$ and the open-circuit terminal-potential remains the same. For "x" failed elements (not in the same cross-connected cell) the internal resistance is $R_G = (n+x) \left(\frac{r}{2}\right)$.

For a series-parallel connected thermoelectric array with 300 total elements ($n = 150$), an open-circuit failure of one element results in a 0.67% increase in R_G , and therefore a negligible change in load-power and load voltage. For 10-element-failures (no two in the same cell) R_G increases by 6.7%. For an initially matched load this failure condition will result in about 93% of original power dissipation in the load and a 2% increase in load voltage.

7. State-of-the-Art Reliability for Thermoelectric Components

The increasing number of successful operational hours for radioisotope thermoelectric generators provides an excellent basis for the accurate prediction of reliability of similar systems.

a. Failure Rate Data From Past Programs

The number of lead telluride operating couple-hours to date is estimated at about 25 million with a failure rate estimated at 2×10^{-7} /hr (50% confidence level).

b. Estimates of Achievable Failure Rates

An increased understanding and subsequent correction of failure modes, based on results of long-term laboratory tests, has made it possible to predict the achievement of failure rates as low as 5×10^{-8} /hr, or better, in the near future. The achievable failure rate for thermoelectric components obviously depends on the operating environment, particularly hot junction temperatures, and all estimates must be based on the assumption of conservative design applications.

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E. POWER CONDITIONING SUBSYSTEM RELIABILITY

The power conditioning subsystem is a complex interconnection of such electronic components as diodes, transistors, resistors, capacitors, transformers, etc. The reliability of the composite device depends on the failure rates of the components. This in turn is highly dependent on component operating temperature and such applicational stresses as voltage, power dissipation and duty cycle. Conservative use of components well below their ratings and the use of component-redundancy where possible are well established techniques for optimizing the reliability of electronic systems.

A most important consideration is the use of components and assembly techniques which have been proven in other programs where high-reliability has been a prime consideration. Examples are the manned space vehicles, deep space probes, communications satellites and the submarine cable amplifiers applications. The qualified components used in these applications are available from the various specialty vendors at a reasonable cost premium as is the equipment for making high-reliability interconnections.

1. State-of-the-Art Reliability

Information supplied by Honeywell pertaining to their low-input-voltage converters (LIVC) states "calculated reliability typically 62,700 hours MTBF"*. This is a mean-time-before-failure equivalent to 7.157 years. Unfortunately, there is not enough operating history with these devices to establish a firm statistical failure model; however, reasonable bounds for their reliability can be established by assuming failure distributions which have been developed for similar electronic devices.

* Low Input Voltage Conversion, undated brochure, Honeywell Ordnance Division, Hopkins, Minn.

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a. Constant Failure Rate

If the power conditioning subsystem is so designed that its operating lifetime is appreciably less than the "wear out" time of any of the components, all failures (assuming quality control has rejected initially defective units) can be attributed to random mechanisms. Under this assumption, the failure rate will be constant for a population of such subsystems. The Poisson (exponential) probability density function is applicable and the reliability (probability of non-failure) for this model is

$$R(t) = e^{-\lambda t}$$

where

- R(t) = reliability as a function of operating time
- e = the base of natural logarithms
- λ = the device failure-rate (sum of all component failure-rates)
= 1/MTBF (MTBF = mean-time-before-failure)
- t = the time interval of interest

For a MTBF of 62,700 hr (7.157 yr) ($\lambda = 1.595 \times 10^{-5}/\text{hr}$), the probability of no failure in 18 months (1.296×10^4 hr), is

$$\begin{aligned} R(18 \text{ months}) &= e^{-(1/6.27 \times 10^4) (1.296 \times 10^4)} = e^{-(0.1595) (1.296)} \\ &= e^{-0.2067} = \underline{0.814} \end{aligned} \quad (\text{VII-14})$$

b. Increasing Failure Rate

If the times to failure for a population of converters are "normally" distributed, then the rate of expected failure will increase with operating time. The survival curve for these Gaussian statistics is characterized by a MTBF (μ) and a standard deviation (σ).

For a MTBF = 6.270×10^4 hr and an assumed $\sigma = 3.135 \times 10^4$ hr, the probability of non-failure during 18 months of operation is:

$$R = 1 - F_N(x) \quad (\text{VII-15})$$

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where

$F_N(x)$ is the cumulative normal distribution function

$$= \frac{1}{\sqrt{2\pi} \sigma} \int_{-\infty}^t e^{-(X-\mu)^2/2\sigma^2} dx$$

$$= \frac{1}{\sqrt{2\pi}} \int_{-\infty}^t e^{-\tau^2/2} d\tau$$

where

$t = (X - \mu) / \sigma$ and
 $\mu = \text{MTBF}$
 $\sigma = \text{standard deviation}$
 $X = 18 \text{ months (this case)}$

Therefore,

$$t = \frac{1.296 \times 10^4 - 6.270 \times 10^4}{2.135 \times 10^4} = \frac{-4.974}{3.135} = -1.586 \quad (\text{VII-16})$$

From standard statistical tables, $F_N(-1.586) = 0.0566$

Therefore,

$$R(18 \text{ months}) = 1 - 0.057 = \underline{\underline{0.943.}}$$

As the reliability for this assumed failure-distribution is considerably higher than the exponential distribution, it is clear that accurate reliability predictions must be based on the considerable body of test information which will define the actual failure distribution.

2. Estimate of Achievable Reliability

Based on the preceding calculations for the reliability of state-of-the-art power conditioning equipment and the continuing operation of the SNAP-3 RTG in the Transit IV-A satellite, it seems reasonable to expect that improvements in conservative circuit-design-techniques and component-reliabilities will lead to power-conditioning subsystem failure rates in the near future of $2 \times 10^{-6}/\text{hr}$ or better.

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A considerable history for the failure-rates of typical electronic components is now available to the designer of electronic sub-systems. Most component vendors can supply this information as a result of participation in a major NASA or military program. Figure VII-8 indicates the failure-rate requirements imposed by several major programs for the various types of electronic components.

Another method of decreasing the failure-rate of electronic components is illustrated in Figure VII-9 which indicates the failure-rate dependence of a typical electronic capacitor on operating temperature and voltage. A low-operating temperature and operation at less than rated voltage is seen to appreciably decrease the failure rate.

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FIGURE VII-8. FAILURE-RATE REQUIREMENTS (ELECTRONIC COMPONENTS) IMPOSED BY HIGH RELIABILITY PROGRAMS

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FIGURE VII-9. TYPICAL CAPACITOR FAILURE-RATE DEPENDENCE ON OPERATING TEMPERATURE AND VOLTAGE

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F. RTG SYSTEM EXPECTED LIFETIME PREDICTION

The lifetime of the RTG system will be determined by the reliability of the thermoelectric array and the power conditioning subsystem. Both are decreasing functions of operating time. Figure VII-10 indicates the predicted reliability of an RTG composed of 100 equivalent series units series-parallel connected with a power conditioning subsystem. The upper curve indicates the reliability that is expected to be achievable with advanced technology and conservative application; the lower curve indicates state-of-the-art system reliability which is controlled by the power conditioning subsystem as indicated by Table VII-2 which presents the subsystem reliabilities whose product determines the system reliability.

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FIGURE VII-10. RELIABILITY OF RTG SYSTEMS
(90% Confidence Limit)

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TABLE VII-2
RELIABILITY OF RTG SYSTEMS

100 Unit Series-Parallel Connected Thermoelectric Array with Power Conditioner

R_{TA} = thermoelectric array reliability

R_{PC} = power conditioner reliability

R_{RTG} = RTG system reliability = $(R_{TA}) (R_{PC})$

λ = failure rate per hour

Operating Time		State-of-the-Art Systems λ thermoelements = 2×10^{-7} /hr λ power conditioner = 1.595×10^{-5} /hr			Advanced Technology Systems λ thermoelements = 5×10^{-8} /hr λ power conditioner = 2×10^{-6} /hr		
Mos.	Hrs.	R_{TA}	R_{PC}	R_{RTG}	R_{TA}	R_{PC}	R_{RTG}
0	0	1	1	1	1	1	1
6	4380	.9999	.9325	.932	1	.9913	.9913
12	8760	.9997	.8700	.870	1	.9827	.9827
18	13140	.9993	.8115	.811	1	.9740	.9740
24	17520	.9988	.7570	.756	.9999	.9656	.9655
30	21900	.9981	.7055	.704	.9999	.9570	.9569
36	26280	.9973	.658	.656	.9998	.9489	.9487
42	30660	.9963	.614	.612	.9998	.9405	.9403
48	35040	.9951	.573	.570	.9997	.9321	.9318
54	39420	.9939	.535	.532	.9996	.9242	.9238
60	43800	.9924	.498	.494	.9995	.9161	.9157

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VIII. USE OF THE PARAMETRIC DATA

The foregoing sections have summarized the data developed in the parametric study, and shown how certain specific power levels can be obtained in terms of temperature, heat source, shielding, and so on. Such data can be immensely useful to the designer if it is possible to interpolate for power levels not specifically covered in the study. This section demonstrates the use of this accumulation of data in establishing, in a general way, the feasibility of a specific large milliwatt RTG for a specific application.

Generally, RTG systems are chosen for specific applications because of their characteristic highly reliable operation over an extended life. The large milliwatt systems available today achieve reliability by conservative design practices. End of life (EOL) performance is assured by supplying excessive fuel at the beginning of life (BOL); electronic components are de-rated to achieve reliability; thermoelectrics are de-rated by lowering the beginning of life temperatures and by controlling the environment throughout life. The lifetime of the entire device is extended by providing excellent isolation from shock and vibration, by carefully selecting critical components, and by conservative packaging.

In each section of this document, specific recommendations have been made in regard to the components available today to meet the requirements of a large milliwatt RTG. These recommendations are summarized in Table VIII-1.

Analysis of the design problems was simplified by considering it as two separate systems: the thermal system and the electrical system. Each system was then analyzed by itself, using a computer; the two analyses were

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TABLE VIII-1

SUMMARY OF RTG DESIGN RECOMMENDATIONS

<u>Material</u>	<u>Comment</u>	<u>Ref. Section</u>
1. Thermoelectric module	Bi_2Te_3 because of low temperature, good effic.	Section II
2. Isotope Heat Source	a) Pm_2O_3 - 1-1/2-year-mission b) SrTiO_3 - 5-year-mission c) Pu - 10-year-mission The longer mission fuels can be used for shorter mission if justified	Section III
3. Biological Shielding	Uranium most compact Mallory-2000 Lead - least expensive	Section IV
4. Thermal Insulation	MIN-K-1301 good thermal and shock isolation qualities	Section 4

brought together by bridging curves. The process for designing a 1 w(e), fully shielded SrTiO_3 system, using the data from this study, is illustrated in Figure VIII-1. A specific design may involve different curves, depending upon the starting data, external shielding, exact thermal management, fabricability considerations, and reconciliation of these factors via the computer program.

A. MECHANICAL DESIGN

The calculational procedure required to determine overall radio-isotope thermoelectric generator design and performance characteristics (based on the parametric results included in this report) is as follows:

- 1) Determine the electrical system efficiency at end of life, ETASYS (EOL), for a cold junction temperature $T_C = 170^\circ\text{F}$, and given hot junction temperature T_H , and any appropriate set of module design parameters which are fabricationally feasible (see Figure II-10).

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FIGURE VIII-1.

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- 2) Plot ETASYS(EOL) for a given TH on the Bridging Curves (Figure I-4) to determine the approximate effective figure of merit Z_E which describes the temperature dependency of ETASYS(EOL).
- 3) The overall system characteristics are plotted in Appendix I-D as a function of Z_E . Select the figure corresponding to a given power output, radioisotope fuel, shield configuration, and insulation material. System efficiency ETA(EOL), weight, and costs are plotted versus diameter for characteristic Z_E 's.

Optimum diameter must be determined on the basis of user limitations, maximum efficiency, minimum weight, or minimum cost. There is usually a tradeoff requiring a judgment decision based on specific requirements; however, generally the region near the knee of the efficiency curve is a good design area.

Figure VIII-1 illustrates this procedure for a 1-watt, SrTiO_3 , internal full uranium shield, MIN-K-1301 insulation, RTG based on the use of a Bi_2Te_3 N/P module. For a load voltage VLD = 3 volts, TH = 400°F , and TC = 170°F the thermoelectric module has the following characteristics:

ETASYS(EOL) = 2.23 (Figure VIII-1a)
No. couples = 140
L/A = 47 in.⁻¹

The fabrication requirements can be met with state-of-the-art technology and the design point is therefore realistic. Transferring this datum point to the Bridging Curves shows that ETASYS(EOL) is characteristic of $Z_E = 0.90$.

Since Bi_2Te_3 is used, the system analysis curves which are limited to TH = 400°F and $Z_E = 0.9$ are applicable. The design point selected corresponds to minimum weight giving:

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ETA(EOL) = 1.50%
 Weight = 222 lb
 Diameter = 11.0 in.

Fuel cost curves are included for the general system analysis figures, but not for Bi_2Te_3 . Fuel cost can be calculated based on the thermal power at beginning of life $Q(\text{BOL})$.

$$Q(\text{BOL}) = Q(\text{EOL})e^{\lambda t}$$

where

$$Q(\text{EOL}) = \frac{P_E}{\text{ETA}(\text{EOL})}$$

$$\lambda = \text{radioisotope decay constant}$$

$$= \frac{0.693}{t_{1/2}}$$

$$t_{1/2} = \text{half-life}$$

$$t = \text{mission time}$$

For Sr-90, $t = 5$ years, $t_{1/2} = 27.7$ yrs, $\lambda = 0.025$; therefore,
 $Q(\text{EOL}) = 66.7$ watts and $Q(\text{BOL}) = 75.7$ watts.

Fuel costs for Sr-90 are:

Present = 32 \$/watt

Future = 19 \$/watt

Costs for the reference case are therefore \$2420 (present) and \$1440 (future). Performance characteristics can be determined from the parametric curves for any variation in module design features except TC, (e.g., different load voltage, degradation, mismatch ratio and contact loss,) by determining ETASYS(EOL) by the methods described in Section II - Thermo-electric Data, then defining Z_E as described above.

Design and performance characteristics are included for 150 mw, 300 mw, 600 mw, and 1 w for all systems. Data for intermediate power output

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can be obtained by interpolative curve plots based on these reference points.

The design procedure for other thermoelectric materials is identical; however, the general system analysis curves which are based on optimum hot junction temperature (not limited to 400°F) are used (Appendix I-D).

The calculational procedure required to estimate the effect of a change in cold junction temperature TC is as follows:

1. Determine ETASYS(EOL), for TC = 170°F.
2. Determine ETA(EOL), for TC = 170°F.
3. Calculate the system parasitic heat loss Q_{P_1} ,

$$Q_{P_1} = \frac{P_E}{ETA(EOL)_1} - \frac{P_E}{ETASYS(EOL)_1} \text{ watts}$$

4. Since Q_P is essentially a conductive heat loss and proportional to $\Delta T = TH - TC_1$, the change in Q_{P_1} is approximately

$$Q_{P_2} = Q_{P_1} \left(\frac{TH - TC_2}{TH - TC_1} \right) \text{ watts}$$

The corresponding change in heat source size also affects Q_{P_2} , but the exact calculation is an iterative procedure which can best be accomplished using the TEHE program. For small changes in ΔT , this approximation is reasonably accurate.

5. Determine ETASYS(EOL)₂ for the required TC₂ by the methods shown in Section VIII - Electrical System.

6. Calculate new ETA(EOL)₂ for required TC₂,

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$$\text{ETA}(\text{EOL})_2 = \frac{P_E}{Q_{P_2} + \frac{P_E}{\text{ETASYS}(\text{EOL})_2}}$$

therefore $Q(\text{EOL})_2 = \frac{P_E}{\text{ETA}(\text{EOL})_2}$

and $Q(\text{BOL})_2 = Q(\text{EOL})_2 e^{\lambda t}$

Fuel costs are calculated as previously described.

For small changes in TC, system size for the optimum design point is not changed significantly since small changes in insulation thickness are acceptable and can compensate for changes in fuel size and shield thickness. However, small variations in shield thickness can significantly affect the weight. This effect can be approximated as follows:

7. Determine change in uranium shield thickness X_S due to change in $Q(\text{EOL})$,

$$X_S(\text{in.}) = F_S \left[\frac{\ln \frac{Q(\text{BOL})}{A}}{\text{Phi}} \right] \text{SE}, X_{S_1} - X_{S_2} f[Q(\text{BOL})_1] - f[Q(\text{BOL})_2]$$

where

F_S = shield thickness factor, e.g., 0.5 for half-shield

SE, A, Phi = constants dependent on fuel

$Q(\text{BOL})$ = fuel thermal power (Btu/hr)

	<u>Sr-90</u>	<u>Pm-147</u>
SE	1.0	3.106
A	0.0485	0.0615
Phi	2.95	9.36

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8. Determine change in fuel diameter D_F assuming fuel length is unchanged and L/D of fuel = 1.0,

$$D_{F_1} = \left(\frac{4 Q(BOL)_1}{\pi q_F} \right)^{1/3} \text{ in.}$$

$$D_{F_1} - D_{F_2} = \left(\frac{4 Q(BOL)_1}{\pi q_F} \right)^{1/3} - \left(\frac{4 Q(BOL)_2}{\pi q_F D_{F_1}} \right)^{1/2}$$

where

q_F = bulk fuel power density - Btu/hr/in.³

<u>Fuel</u>	<u>q_F (Btu/hr/in.³)</u>
SrTiO ₃	47.0
Pm ₂ O ₃	100

9. To compensate for the effect of moving the internal shield in or out, an amount equal to the change in fuel diameter is applied to the shield thickness; therefore, the effective change in shield thickness ΔX_E is

$$\Delta X_E = X_{S_2} - X_{S_1} + (D_{F_2} - D_{F_1})$$

and the weight change Δwt is approximately,

$$\Delta wt = \frac{\Delta X_E}{\Delta S_1} wt_1$$

$$wt_2 = wt_1 + \Delta wt$$

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Example:

Find effect of decreasing TC from 170°F to 75°F for previous system (1-watt SrTiO₃).

$$1) \quad \text{ETASYS (EOL)}_1 = 2.23\%$$

$$2) \quad \text{ETA (EOL)}_1 = 1.50\%$$

$$3) \quad Q_{P_1} = \frac{1.0}{.015} - \frac{1.0}{.0223} = 21.9 \text{ watts}$$

$$4) \quad Q_{P_2} = 21.9 \left(\frac{375-75}{400-170} \right) = 28.6 \text{ watts}$$

$$5) \quad \text{ETASYS (EOL)}_2 = (2.23)(1.50) = 3.35\%$$

$$6) \quad \text{ETA (EOL)}_2 = \frac{1.0}{28.6 + \frac{1.0}{0.335}} = 1.71\%$$

$$Q(\text{EOL})_2 = \frac{1.0}{.0171} = 58.4 \text{ watts}$$

$$Q(\text{BOL})_2 = (58.4)(1.123) = 65.5 \text{ watts}$$

$$7) \quad X_{S_2} = \frac{1}{2.95} \ln \left[\frac{(65.5)(3.41)}{.0425} \right] = 2.87 \text{ in.}$$

$$X_{S_1} = \frac{1}{2.95} \ln \left[\frac{(75.7)(3.41)}{.0485} \right] = 2.91 \text{ in.}$$

$$X_{S_1} - X_{S_2} = 0.04 \text{ in.}$$

$$8) \quad D_{F_1} = \left[\frac{(4)(75.7)(3.41)}{(\pi)(47)} \right]^{1/3} = 1.913 \text{ in.}$$

$$D_{F_2} = \left[\frac{(4)(65.5)(3.41)}{(\pi)(47)(1.913)} \right]^{1/2} = 1.777 \text{ in.}$$

$$D_{F_1} - D_{F_2} = 0.136 \text{ in.}$$

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9) $\Delta X_E = 0.176 \text{ in.}$

$$\Delta wt = \left(-\frac{0.176}{2.91}\right)(222) = -13.4 \text{ lb}$$

$$wt_2 = 222 - 13.4 = 209 \text{ lb}$$

B. THERMOELECTRIC DESIGN

The calculation procedure to determine the specifics of the thermoelectrics are shown below.

Input data required for sizing a Bi_2Te_3 N/P module for an RTG are:

1. Hot junction temperature TH(B(L))
2. Cold junction temperature TC
3. Load Voltage VLD
4. Linear degradation rate EMUC
5. Mission length time
6. EOL net power output requirement PSYST(EOL)
7. Thermal decay ratio $Q(\text{BOL})/Q(\text{EOL})$ of the radioisotope heat source where $Q(\text{BOL})$ and $Q(\text{EOL})$ are the BOL and EOL isotope thermal power, respectively.

The calculational procedure evolves through the following steps:

1. Determine TH(EOL) from the thermal decay ratio and TH(BOL) - TC.
2. From Figure II-10 determine the module efficiency ETA, at TH(BOL) and TH(EOL) for 6.5% total power degradation, $TC = 170^\circ\text{F}$ and 3 volts load voltage.

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3. Adjust ETA_1 (BOL) and ETA_1 (EOL) to reflect zero degradation (divide values obtained by $1 - 0.065$). Resulting values are ETA_2 (BOL) and ETA_2 (EOL).
4. From Table VIII-2 read scale factor η for proper mismatch ratio RMIS (see steps 10 through 15) and TC. Multiply ETA_2 (BOL) and ETA_2 (EOL) with the appropriate η factors. Resulting values are ETA_3 (BOL) and ETA_3 (EOL).
5. From Table VIII-3 read scale factor θ for proper load voltages VLD. Multiply ETA_3 (BOL) and ETA_3 (EOL) with the appropriate θ factors. Resulting values are ETA_4 (BOL) and ETA_4 (EOL).
6. Determine PSYST (BOL), the BOL net power requirements

$$\text{PSYST}(\text{BOL}) = \frac{\text{PSYST}(\text{EOL})}{1 - \text{EMUC} \cdot \text{TIME}} \times \frac{\text{ETA}_4(\text{BOL})}{\text{ETA}_4(\text{EOL})} \cdot \frac{Q(\text{BOL})}{Q(\text{EOL})}$$

7. Determine

$$Q(\text{BOL}) = \frac{\text{PSYST}(\text{BOL})}{\text{ETA}_4(\text{BOL})}$$

8. Determine

$$Q(\text{EOL}) = \frac{Q(\text{EOL})}{Q(\text{BOL})} \cdot Q(\text{BOL})$$

9. The EOL module efficiency ETA_5 (EOL) is now

$$\text{ETA}_5(\text{EOL}) = \frac{\text{PSYST}(\text{EOL})}{Q(\text{EOL})}$$

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TABLE VIII-2

Scaling Factors for Bi_2Te_3 N/P Modules Operating at Various Hot Junction Temperatures
Reference Conditions $T_C = 170^\circ\text{F}$, Net Power Output 1 watt (e), 3 Volt Load Voltage, RMIS = 1

MISMATCH RATIO RMIS	COLD JUNCTION TEMPERATURE T_C , $^\circ\text{F}$																	
	170°F			150°F			120°F			100°F			80°F			50°F		
	T	L/A	N	T	L/A	N	T	L/A	N	T	L/A	N	T	L/A	N	T	L/A	N
$T_H = 375^\circ\text{F}$																		
1.00	1.00	1.00	1.00	1.03	1.10	0.91	1.27	1.24	0.81	1.34	1.34	0.75	1.42	1.44	0.70	1.58	1.59	0.63
1.25	1.02	0.91	0.97	1.05	0.97	0.82	1.30	1.09	0.72	1.37	1.20	0.67	1.46	1.27	0.63	1.62	1.40	0.57
1.50	1.02	0.82	0.90	1.05	0.87	0.76	1.28	1.00	0.68	1.36	1.08	0.62	1.45	1.14	0.58	1.60	1.27	0.53
1.75	1.00	0.75	0.85	1.03	0.81	0.72	1.25	0.90	0.64	1.33	0.97	0.59	1.41	1.04	0.55	1.56	1.16	0.50
2.00	0.97	0.69	0.81	1.00	0.73	0.68	1.22	0.83	0.61	1.28	0.90	0.56	1.37	0.95	0.52	1.51	1.06	0.47
$T_H = 400^\circ\text{F}$																		
1.00	1.00	1.00	1.00	1.11	1.09	0.94	1.29	1.22	0.83	1.40	1.30	0.77	1.48	1.39	0.73	1.65	1.52	0.66
1.25	1.02	0.89	0.89	1.16	0.97	0.85	1.32	1.09	0.74	1.44	1.15	0.69	1.50	1.24	0.66	1.67	1.39	0.60
1.50	1.02	0.81	0.83	1.13	0.87	0.79	1.31	0.98	0.69	1.43	1.04	0.64	1.49	1.11	0.61	1.66	1.22	0.55
1.75	1.00	0.73	0.78	1.11	0.79	0.74	1.29	0.89	0.65	1.41	0.95	0.61	1.48	1.01	0.58	1.65	1.11	0.52
2.00	0.97	0.66	0.75	1.09	0.73	0.75	1.08	0.81	0.62	1.38	0.87	0.58	1.46	0.93	0.55	1.61	1.01	0.49
$T_H = 425^\circ\text{F}$																		
1.00	1.00	1.00	1.00	1.13	1.08	0.93	1.31	1.19	0.81	1.44	1.27	0.74	1.51	1.35	0.70	1.65	1.47	0.64
1.25	1.02	0.89	0.90	1.15	0.96	0.84	1.34	1.06	0.73	1.47	1.13	0.67	1.53	1.20	0.62	1.70	1.31	0.57
1.50	1.02	0.80	0.84	1.15	0.86	0.78	1.33	0.96	0.67	1.47	1.02	0.62	1.52	1.08	0.57	1.70	1.18	0.54
1.75	1.00	0.72	0.80	1.14	0.78	0.73	1.31	0.87	0.64	1.45	0.93	0.59	1.50	0.98	0.54	1.65	1.08	0.51
2.00	0.98	0.67	0.76	1.12	0.73	0.70	1.29	0.79	0.60	1.42	0.85	0.56	1.48	0.90	0.52	1.62	0.98	0.49
$T_H = 450^\circ\text{F}$																		
1.00	1.00	1.00	1.00	1.17	1.07	0.91	1.36	1.18	0.79	1.51	1.25	0.72	1.57	1.32	0.70	1.71	1.42	0.64
1.25	1.04	0.89	0.90	1.20	0.95	0.83	1.39	1.05	0.71	1.54	1.11	0.65	1.60	1.26	0.62	1.75	1.27	0.58
1.50	1.04	0.80	0.83	1.19	0.86	0.76	1.38	0.95	0.65	1.51	1.00	0.61	1.59	1.05	0.58	1.73	1.14	0.53
1.75	1.01	0.73	0.79	1.17	0.79	0.73	1.35	0.86	0.62	1.49	0.91	0.57	1.56	0.97	0.54	1.69	1.03	0.50
2.00	0.97	0.67	0.75	1.13	0.72	0.69	1.31	0.79	0.59	1.44	0.84	0.54	1.51	0.89	0.52	1.64	0.95	0.47

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TABLE VIII-3

SCALING FACTOR FOR Bi_2Te_3 N/P Module at Various Hot Junction
 Temperatures Reference Conditions: $T_C = 170^\circ\text{F}$, 1 watt (e)
 net power output, 3 Volts Load Voltage and a Mismatch Ratio = 1.

Load Voltage, Volts	$T_H, ^\circ\text{F}$											
	450			425			400			375		
	θ	L/A	N	θ	L/A	N	θ	L/A	N	θ	L/A	N
3	1	1	1	1	1	1	1	1	1	1	1	1
3.25	0.96	0.71	0.70	0.95	0.72	0.79	0.97	0.76	0.74	0.97	0.76	0.74
1.75	0.92	0.51	0.52	0.92	0.55	0.65	0.94	0.57	0.57	0.95	0.58	0.57
1.0	0.86	0.27	0.34	0.86	0.30	0.37	0.86	0.31	0.32	0.87	0.31	0.32
0.5	0.74	0.12	0.17	0.76	0.13	0.17	0.75	0.14	0.16	0.78	0.16	0.16

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10. Determine mismatch ratio RMIS compatible with high module efficiency. Table VIII-2 shows that module efficiencies increase in the range RMIS = 1 to 1.3 and decrease (but insignificantly) in the range RMIS = 1.3 to 2.0. The L/A ratios and the number of couples in series decrease markedly with increasing RMIS.

11. Determine L/A ratio for the mismatch ratio RMIS chosen and for the load voltage specified.

$$(L/A)_1 = (L/A)_{\text{Ref.}} (L/A)_{\text{VIII-2}} (L/A)_{\text{VIII-3}} \cdot \frac{1}{\text{PSYST(BOL)}}$$

where

$(L/A)_{\text{Ref.}}$ = reference L/A ratio for 1 watt(e) net power output
3 volts load voltage, and RMIS = 1. Figure II-11
value of L/A multiplied by (1-0.065) yields a zero-
degradation reference value)

$(L/A)_{\text{VIII-2}} (L/A)_{\text{VIII-3}}$ = scaling factors from Tables VIII-2 and VIII-3,
respectively.

$(L/A)_1$ = required L/A ratio for zero-fold couple redundancy.

12. If one-fold couple redundancy is required for reasons of module reliability, multiply (L/A) by two.

$$(L/A)_2 = 2 \cdot (L/A)_1$$

where

$(L/A)_2$ = L/A ratio for one-fold couple redundancy

13. Determine the element length ELGTH as a function of the heat flux density QH at the couple hot junction interface.

$$\text{ELGTH} = \frac{C}{QH}$$

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where

C = a factor dependent upon the thermoelectric properties and the temperature conditions

$$C = \frac{S^2 \cdot (T_H - T_C)^2}{2 \cdot RHO \cdot ETA} \cdot \frac{RMIS}{(RMIS+1)^2}$$

S = total average seeback coefficient of the couple

RHO = total average electrical resistivity of the couple

$$ETA = \frac{T_H - T_C}{T_H} \cdot \frac{\left(\frac{RMIS}{RMIS+1}\right)}{1 + \frac{RMIS+1}{Z \cdot T_H} - \frac{T_H - T_C}{Z \cdot T_H} \frac{1}{(RMIS+1)}}$$

ETA = thermodynamic efficiency of the module

Z = total average figure-of-merit of the couple

14. The element cross-sectional area $ACRO_{1,2}$ is

$$ACRO_1 = ELGTH/(L/A)_1$$

or

$$ACRO_2 = ELGTH/(L/A)_2$$

Adjust QH such that $ACRO_1$ or $ACRO_2$ are compatible with Bi_2Te_3 N/P couple and module technology.

15. Determine the number of couples in series N* for the mismatch ratio chosen and for the load voltage specified.

$$N = (N)_{REF} \cdot (N)_{VIII-2} \cdot (N)_{VIII-3}$$

* NOTE: The symbol N for number of couples in series in this section corresponds to the symbol SERY in Appendix II-A.

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where

$(N)_{\text{REF}}$ = number of couples in series for 3-volt-load
voltage and $RMIS = 1$

$(N)_{\text{VIII-2}}, (N)_{\text{VIII-3}}$ = scaling factors from Tables VIII-2
and VIII-3 respectively

Practical ranges for the critical design parameters for Bi_2Te_3
modules are:

L/A ratios: $\leq 0.040 \times 0.040$ in.

$ACRO_{1,2}$: ≤ 175

- A. SAMPLE PROBLEM I. Design of a one watt(e) (EOL), 425°F (BOL)
Module for a fully-shielded SrTiO_3 RTG

Input Data:

$TH(\text{BOL}) = 425^\circ\text{F}$

$TC = 75^\circ\text{F}$

Load voltage (EOL) $VLD = 3$ volts

Linear degradation rate $EMVC = 0.003/1000$ hr

Mission length $TIME = 43,000$ hr (5 years)

EOL net power output $PSYST(\text{EOL}) = 1.0$ watt(e)

Thermal decay ratio $Q(\text{BOL})/Q(\text{EOL}) = 1.14$

One-fold couple redundancy required

Step 1

$TH(\text{EOL}) = 375^\circ\text{F}$

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Step 2

From Figure II-10:

$$\text{ETA, (BOL)} = 2.20\%$$

$$\text{ETA, (EOL)} = 2.08\%$$

Step 3

$$\text{ETA}_2(\text{BOL}) = 2.35\%$$

$$\text{ETA}_2(\text{EOL}) = 2.21\%$$

Step 4

From Table VIII-2 (interpolated) for RMIS = 1.5:

$$= 1.55 \text{ (BOL)}$$

$$= 1.48 \text{ (EOL)}$$

$$\text{ETA}_3(\text{BOL}) = 3.66\%$$

$$\text{ETA}_3(\text{EOL}) = 3.31\%$$

Step 5

From Table VIII-3:

$$\theta^* = 1 \text{ (BOL)}$$

$$\theta = 1 \text{ (EOL)}$$

$$\text{ETA}_4(\text{BOL}) = 3.66\%$$

$$\text{ETA}_4(\text{EOL}) = 3.31\%$$

Step 6

$$\text{PSYST(BOL)} = 1.41 \text{ watts}$$

Step 7

$$Q(\text{BOL}) = 38.0 \text{ watts}$$

* NOTE: Assumed is that the effect of the increased load voltage at BOL (3.4 volts) is negligible.

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Step 8

$$Q(\text{EOL}) = 33.4 \text{ watts}$$

Step 9

$$\text{ETA}_5(\text{EOL}) = 2.97\%$$

Step 10

$$\text{RMIS} = 15$$

Step 11

From Figure II-11:

$$(\text{L/A})_{\text{REF}} = 51 \text{ in.}^{-1} \text{ (adjusted for zero-degradation)}$$

From Tables VIII-2 and VIII-3:

$$(\text{L/A})_{\text{VIII-2}} = 1.10 \text{ (interpolated)}$$

$$(\text{L/A})_{\text{VIII-3}} = 1.0$$

$$(\text{L/A})_1 = 40 \text{ in.}^{-1}$$

Step 12

One-fold couple redundancy required.

$$(\text{L/A})_2 = 80 \text{ in.}^{-1}$$

Step 13

$$\text{ELGTH} = \frac{1.522}{\text{QH}} \text{ in.}^1$$

Step 14

$$\text{For QH} = 7.5 \text{ watts/cm}^2$$

$$\text{ELGTH} = 0.204 \text{ in.}$$

$$\text{and ACRO}_2 = 0.0025 \text{ in.}^2$$

The square cross-sectional element would be 0.050 in. on the side. Such an element size is compatible with today's

Bi_2Te_3 N/P couple and module fabrication technology.

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Step 15

From Figure II-10:

$$(N)_{\text{REF}} = 1.52 \text{ (adjusted to 3.4 volts BOL)}$$

From Tables VIII-2 and VIII-3:

$$(N)_{\text{VIII-2}} = 0.57 \text{ (interpolated)}$$

$$(N)_{\text{VIII-3}} = 1.0$$

$$N = 88 \text{ couples in series}$$

- B. SAMPLE PROBLEM II. Design of a 500 mw (EOL), 425°F (BOL) Module for a fully shielded SrTiO_3 RTG

Input Data:

$$\text{TH(BOL)} = 425^\circ\text{F}$$

$$\text{TC} = 100^\circ\text{F}$$

$$\text{Load voltage (EOL) VLD} = 3 \text{ volts}$$

$$\text{Linear degradation rate EMUC} = 0.003/1000 \text{ hr}$$

$$\text{Mission length} = 43,000 \text{ hr}$$

$$\text{EOL net power output PSYST(EOL)} = 500 \text{ mw}$$

$$\text{Thermal decay ratio } Q(\text{BOL})/Q(\text{EOL}) = 1.14$$

One-fold couple redundancy required.

Step 1

$$\text{TH(EOL)} = 380^\circ\text{F}$$

Step 2

From Figure II-10:

$$\text{ETA, (BOL)} = 2.20\%$$

$$\text{ETA, (EOL)} = 2.08\%$$

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Step 3

$$\text{ETA}_2(\text{BOL}) = 2.35\%$$

$$\text{ETA}_3(\text{EOL}) = 2.21\%$$

Step 4

From Table VIII-2 for RMIS = 1.5:

$$\eta = 1.47 (\text{BOL})$$

$$\eta = 1.37 (\text{EOL}) (\text{interpolated})$$

$$\text{ETA}_3(\text{BOL}) = 3.45\%$$

$$\text{ETA}_3(\text{EOL}) = 3.05\%$$

Step 5

From Table VIII-3:

$$\theta^* = 1 (\text{BOL})$$

$$\theta = 1 (\text{EOL})$$

$$\text{ETA}_4(\text{BOL}) = 3.45\%$$

$$\text{ETA}_4(\text{EOL}) = 3.05\%$$

Step 6

$$\text{PSYST}(\text{BOL}) = 0.710 \text{ watts}$$

Step 7

$$Q(\text{BOL}) = 20.6 \text{ watts}$$

Step 8

$$Q(\text{EOL}) = 18.2 \text{ watts}$$

Step 9

$$\text{ETA}_5(\text{EOL}) = 2.75\%$$

* NOTE: Assumed is that the effect of the increased load voltage at BOL (3.4 volts) is negligible.

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Step 10

RMIS = 1.5, compatible with the efficiencies computed above

Step 11

From Figure II-11 (interpolated between 600 mw and 300 mw curves):

$$(L/A)_{\text{REF}} = 109 \text{ in.}^{-1} \text{ (adjusted for zero degradation)}$$

From Tables VIII-2 and VIII-3:

$$(L/A)_{\text{VIII-2}} = 1.02$$

$$(L/A)_{\text{VIII-3}} = 1.0$$

$$(L/A)_1 = 77 \text{ in.}^{-1}$$

Step 12

One-fold redundancy required and thus

$$(L/A)_2 = 154 \text{ in.}^{-1}$$

Step 13

$$\text{ELGTH} = \frac{1.410}{\text{QH}} \text{ in.}^{-1}$$

Step 14

$$\text{For QH} = 5 \text{ watts/cm}^2$$

$$\text{ELGTH} = 0.280 \text{ in.}$$

and $\text{ACRO}_2 = 0.0018 \text{ in.}^2$ which corresponds to a 0.042×0.042 in. square cross section of the element. Above dimensions are compatible with today's Bi_2Te_3 module technology.

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Step 15

From Figure II-10:

$$(N)_{\text{REF}} = 152 \text{ (adjusted to 3.4 volts BOL)}$$

From Tables VIII-2 and VIII-3:

$$(N)_{\text{VIII-2}} = 0.62$$

$$(N)_{\text{VIII-3}} = 1.0$$

$$N = 94 \text{ couples in series}$$

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The most promising and best established thermoelectric materials for use in thermoelectric modules are Bi_2Te_3 N/P and PbTe 2N/2P. Of the two materials, Bi_2Te_3 N/P yields the highest performances. This fabrication technology is also more advanced than that of PbTe 2N/2P and it lends itself to a much wider range of couple and module configurations.

Bi_2Te_3 N/P

Pertinent curves are: Figure II-10

Figure II-11

Pertinent tables are: Table VIII-2

Table VIII-3

Practical design parameters values are:

L/A ratio: $\leq 1500 \text{ in.}^{-1}$

Element cross-sectional area: $\geq 0.040 \times 0.040 \text{ in.}$

Number of couples in series: ≤ 175

PbTe 2N/2P

Pertinent curves are: Figure II-8

Figure II-9

These plots are set up for a cold junction temperature of 170°F , a degradation rate of 0.005/1000 hr, a mission length of 13,000 hr and a mismatch ratio of 1. Designs for different operating conditions can be scaled from the ones represented in Figures II-8 and II-9 by the use of the appropriate formulae in Appendix II-A.

Practical design parameter values are:

Element cross-sectional area: $\geq 0.055 \times 0.0055 \text{ in.}$

Number of couples in a series: ≤ 175

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