SELENIDE ISOTOPE GENERATOR

for the GALILEO MISSION

PROGRAM FINAL REPORT

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FOREWORD

This final report for the Selenide Isotope Generator for the Galileo Mission (SIG/GM) documents the work performed by Teledyne Energy Systems (TES) under U. S. Department of Energy (DOE) Contract No. DE-AC01-78ET33009 (formerly ET-78-C-01-2865) during the period April 10, 1978 to June 30, 1979. Because of technical difficulties with the thermoelectric converter being developed by the 3M Company under separate DOE contract, a Stop Work Order, dated January 29, 1979, was issued by DOE. Reported herein is the TES effort up to the receipt of the Stop Work Order as well as limited technical activities up to the contract conclusion on June 30, 1979.

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I. INTRODUCTION

The Galileo mission will continue the scientific exploration of the Jovian system initiated in the 1970's by its predecessors, the Pioneer and Voyager Jupiter flyby missions. It is distinguished from these two earlier flyby missions in that the spacecraft will separate into two parts on arrival at Jupiter, one part being an instrumented probe which will penetrate into the planet's atmosphere, and the other being an orbiter to permit a detailed study of the planet and its satellites and magnetosphere. Like the spacecraft of these prior missions, the Galileo spacecraft will be electrically powered by radioisotope thermoelectric generators (RTG's) which use the decay of plutonium-238 to generate thermal energy that is converted directly to electrical energy by a thermoelectric converter.

The Selenide Isotope Thermoelectric Generator for the Galileo Mission (SIG/GM) combines selenide thermoelectric materials technology and copper-water heat pipe radiator technology with a Voyager multi-hundred watt (MHW)-type radioisotopic heat source. Predicted thermoelectric conversion efficiencies in the 11% range, resulting in RTG system efficiencies of ~ 9% and power-to-weight ratios greater than 2 watts/pound, offered a potential for substantial improvement over prior silicon germanium (Voyager) and TAGS/lead telluride (Pioneer) flight units which exhibited system efficiencies of less than 7% and power-to-weight ratios less than 2 watts/pound. To realize this potential performance improvement in an RTG designed for integration with the Galileo spacecraft was the objective of the SIG/GM program.

The SIG/GM program milestones are shown in Fig. I-1. As of the receipt of the Stop Work Order, dated January 29, 1979, the first three milestones had been completed with fabrication of the S/N 2 housing progressing at Teledyne Energy Systems (TES). Major hardware delivery milestones scheduled on the SIG/GM program include:

FIG. I-1. SIG/GM PROGRAM MILESTONES

- an Engineering ETG (S/N 1) for test at TES
- a Qualification RTG (S/N 2)
- two ETG's (S/N's 3 and 4) for delivery to JPL
- two flight RTG's and one backup flight RTG (S/N's 5, 6 and 7)

As shown in Fig. I-1, the flight units were scheduled for delivery to Cape Canaveral in late 1981. Fig. I-2 shows the detail schedule for ETG/RTG design, fabrication, test and delivery.

Numerous organizations have participated in the SIG/GM program, providing both hardware and technical support. TES and the 3M Company are the two prime co-contractors for DOE, TES being responsible for the RTG system design, fabrication/assembly, test and integration with the Galileo spacecraft, and 3M Company the selenide thermoelectric converter design, fabrication/assembly and test. The thermoelectric converter and MHW-type radioisotopic heat source, the latter furnished to DOE by Mound Research Corporation (MRC), are provided to TES as government-furnished equipment (GFE) for subsequent RTG assembly, test and delivery.

Table I-1 lists the major participating organizations and their principal roles.

FIG. I-2. SIG/GM ETG SCHEDULE

TABLE I-1

SIG/GM PROGRAM PARTICIPATING ORGANIZATIONS

<u>Item</u>	<u>Organization</u>
Government RTG Supplier	Department of Energy
User/Agency/Spacecraft Integrator	NASA/Jet Propulsion Laboratory
RTG System Contractor	Teledyne Energy Systems
Thermoelectric Converter Contractor	3M Company
Heat Source Assembly	Monsanto Research Corporation
Fuel and Cladding	Savannah River Plant
Heat Pipes	B & K Engineering, Inc
Insulations and Technology	Oak Ridge National Laboratory
Fibrous Insulations Testing	Dynatech R/D Company
Multifoil Insulation	Thermo Electron Corporation
Electroplating and Chemistry	Battelle Columbus Laboratories
Graphite Characterization	Southern Research Institute Battelle Columbus Laboratories
Safety Testing	
• Blast overpressure	Los Alamos Scientific Laboratory
• Flyer plate	Air Force Weapons Laboratory
• Plasma arc	NASA/Ames Air Force Dynamics Laboratory/ Wright-Patterson Air Force Base
Dynamic Testing	Naval Surface Weapons Center
Technology Development, Test and Assessment	General Atomic Company Jet Propulsion Laboratory Fairchild Space & Electronics Company Battelle Columbus Laboratories
Electrical Heat Source	General Electric Company
Quality Assurance	Sandia Corporation

II. MISSION DESCRIPTION

The objectives of NASA's Galileo 1982 mission are to conduct comprehensive investigations of the Jovian system, including the planet, its environment and its satellites. Primary emphasis is on direct entry measurements and the characterization of the magnetosphere of the planet and its interaction with the satellites.

Launch opportunities to Jupiter occur every 13 months, the more favorable geometrics occurring on approximately 6-year centers, at which time Jupiter crosses the ecliptic plane. Such a favorable opportunity occurs in December 1981/January 1982. A Mars fly-by will be used, taking advantage of that planet's favorable alignment for gravity assist to minimize launch energy requirements. Launch is scheduled during a 10-day period in early January 1982 from Cape Canaveral, Florida using the Space Transportation System (STS-consisting of the Space Shuttle and Inertial Upper Stage) to carry the Galileo spacecraft into Earth orbit. After deployment from the Shuttle in Earth orbit, the three-stage IUS (Inertial Upper Stage) will provide the necessary thrust for injection into the interplanetary cruise trajectory. Cruise time to Jupiter is approximately 3.5 years. Fig. II-1 shows the Galileo spacecraft in the cruise configuration. Table II-1 summarizes the RTG-related and major events during the launch sequence.

The Galileo spacecraft consists of an Orbiter and an Atmospheric Entry Probe. The Entry Probe is released from the Orbiter approximately 150 days prior to Jupiter encounter and subsequently plunges into Jupiter's atmosphere behind a heat shield. After the Probe has slowed to subsonic speeds, the shield is ejected, and the Probe falls by parachute to a preselected depth in the atmosphere. At this point the parachute is cut away and the Probe falls freely until the increasing pressure crushes the hull, about 45 minutes after entry. During the descent, the Probe examines basic properties of the Jovian atmosphere

FIG. II-1. GALILEO SPACECRAFT CRUISE CONFIGURATION

TABLE II-1

SIG/GM LAUNCH SEQUENCE*

<u>Time (hrs:min:sec)</u>	<u>Event</u>
-005:00:00	Initiate water cooling of RTG's in Shuttle with GSE.
-000:00:10	Switch from GSE to internal Shuttle RTG water cooling system.
-000:00:05	Switch to Shuttle evaporator RTG water cooling system.
000:00:00	Lift off.
000:50:00	Verify safe orbit.
001:02:30	Open Shuttle payload bay doors.
001:06:30	Switch to internal Shuttle RTG water cooling system.
005:30:00	Perform predeployment checkout.
TBD	Release payload forward IUS/SC attachment fitting and tilt IUS/SC.
006:09:00	Communicate go/no go decision.
006:10:00	Disconnect RTG water cooling system.
006:11:18	Maneuver Shuttle to IUS/SC deployment attitude.
006:31:28	Release aft IUS/SC held down attachments and eject IUS/SC.
006:45:00	End of uncontrolled IUS/SC coast.
007:33:00	IUS first stage ignition and burn.
010:18:46	IUS first stage separation.
010:21:03	IUS second stage ignition and burn.

*Extracted from Galileo Launch Sequence, dated 9/25/78.

TABLE II-1 (CONT.)

SIG/GM LAUNCH SEQUENCE

<u>Time (hrs:min:sec)</u>	<u>Event</u>
010:25:06	IUS second stage spins up IUS third stage and SC to 70 rpm.
010:26:49	IUS second stage separation.
010:27:06	IUS third stage ignition and burn.
010:28:53	IUS third stage burn complete.
010:29:35	CDS starts AACS spin down.
010:44:05	Spin down complete to 1 rpm.
010:45:36	RTG booms released.
010:45:47	RTG booms deployed.
010:47:05	IUS third stage separation.
010:47:15	SC spin up to 7 rpm.
010:48:45	Magnetometer boom deployed.
011:10:00	SC spin down to 3 rpm.
TBD	Interplanetary cruise.

including: composition, density opacity and structure. The data are transmitted to the Orbiter for eventual relay back to Earth.

After release of the Atmospheric Entry Probe, the Orbiter fires its braking engine and goes into a long elliptical orbit around Jupiter. Shortly after arrival, the Orbiter adjusts its initial orbit to a distance from Jupiter approximately equal to that of Ganymede's orbit. For approximately 20 months the Orbiter will examine Jupiter's environment, using the gravitational attraction of the satellites Ganymede and Callisto to alter the orbit slightly on each pass (the Orbiter will make approximately 11 encounters with these satellites). The Orbiter will examine the surface compositions and atmospheres of Jupiter's satellites, the charged particle densities and magnetic fields around Jupiter as well as perform synoptic studies of the Jovian atmosphere.

Table II-2 lists in greater detail the specific objectives of the various aspects of the Galileo mission.

Two radioisotope thermoelectric generators (RTG's) provide the necessary electrical power for the Galileo spacecraft. The dual spin-stabilized spacecraft deploys the RTG's on extendable booms shortly after the completion of the IUS third stage burn. The RTG design must assure that spacecraft electrical power requirements are satisfied for a minimum of 50,000 hours after launch.

Project management for the Galileo mission is by the Jet Propulsion Laboratory (JPL) with program direction in the NASA Office of Space Science. JPL also manages the orbiter design and development and is responsible for the mission design and spacecraft integration. The NASA Ames Research Center (ARC) manages the Probe System. The NASA Space Transportation System (Space Shuttle and Inertial Upper Stage) is managed by the NASA Lyndon B. Johnson Space Center (JSC). JPL is designated by the Office of Space Tracking and Data System as the lead center responsible for all tracking and data acquisition

TABLE II-2

OBJECTIVES OF THE GALILEO MISSION

Planned Science Objectives

- Characterize the chemical composition of the atmosphere.
- Determine the structure (temperature, pressure and density) of the atmosphere to a pressure depth of at least 10 bars.
- Determine the nature and extent of cloud particles, and the location and structure of the Jovian clouds.
- Determine the radiative energy balance in the atmosphere.
- Investigate the circulation and dynamics of the atmosphere.
- Characterize the upper atmosphere and ionosphere.

Satellite Science Objective

- Characterize the morphology, geology and physical state of satellite surfaces.
- Investigate the surface mineralogy of satellites and determine the distribution of components.
- Determine the gravitational fields, magnetic fields, and dynamic properties of the satellites.
- Study satellite atmospheres and ionospheres, extended gas clouds arising from the satellites, and interactions with the magnetosphere.

Magnetosphere Science Objectives

- Characterize absolute energy spectra, composition and angular distribution of energetic charged particles as a function of position (to 150 Jupiter radii) and time.
- Characterize the vector magnetic fields as a function of position (to 150 Jupiter radii) and time.

TABLE II-2 (CONT.)

OBJECTIVES OF THE GALILEO MISSION

- Characterize the absolute energy spectra, composition, and angular distribution of plasma, including plasma wave phenomena, as a function of position (to 150 Jupiter radii) and time.
- Investigate satellite-magnetosphere interactions.

support for the Galileo project. The data collecting and processing activity uses the NASA Spaceflight Tracking and Data Network (STDN), the Deep Space Network, the NASA Communications Network, and Mission Control and Compatibility Center facilities. The Department of Energy will develop and supply the RTG's to satisfy the Galileo mission requirements. Galileo programmatic responsibilities are summarized in Table II-3.

TABLE II-3

GALILEO PROGRAMMATIC RESPONSIBILITIES

DOE

NASA

BMFT¹

Office of Assistant
Secretary of
Energy Technology

Space Transportation
System Operations Office

Office of Space Science

Office of Space Tracking
& Data System

Space Research
Division

- Program Management
- Science Objectives

ANS&P

- RTG Design & Development
- Delivery of RTG Flight Hardware

JSC

- Space Transportation System (STS)
- Spacecraft/STS Integration
- Crew Operations

MSFC

- IUS Development

KSC

- Launch Operation

JPL

- Project Management
- Project Scientist
- Orbiter Design & Development
- Mission Design
- Spacecraft Integration
- Systems Engineering
- Spacecraft Assy & Verification
- Mission Operations
- Orbiter Science Management

ARC

- Probe Design Development
- Science Management
- Probe Science Management

JSC

- Near Earth Tracking & Data Acquisition Reqmt's & Coordination

JPL

- Deep Space Tracking & Data Acquisition

DFVLR²

- RPM Design & Development
- Selected Science
- Test & Mission Operations Support

¹ Bundesminister für Forschung und Technologie

² Deutsche Forschungs- und Versuchsanstalt für Luft- und Raumfahrt e. V.

III. RTG/SPACECRAFT INTERFACE

Substantive RTG/spacecraft integration activities began with a major meeting held at JPL in January 1978. These activities, involving TES, DOE and JPL, continued at a steadily increased pace until the DOE stop work order in January 1979. At the January stop-work-point, agreement on the electrical and mechanical interfaces was about 90% complete. This relative agreement is the result of detailed design efforts conducted by both TES and JPL to resolve specific interface problems.

A. ELECTRICAL INTERFACE

1. Power and Voltage

Electrical power at end of mission is the overriding power requirement to be satisfied. RTG minimum electrical power at acceptance must be 214 watts at an average heat pipe radiator temperature of $123 \pm 3^\circ\text{C}$. The RTG must provide 185 watts at 30 ± 0.5 volts dc at a minimum of 50,000 hours following spacecraft launch.

2. Instrumentation

No RTG internal instrumentation is provided. Five resistance type temperature transducers (RTD's) are used to measure RTG external temperatures. Locations of the five RTD's are as follows:

- a. two on radiator - telemetry
- b. two on housing - telemetry
- c. one in vicinity of cooling loop for use with ground temperature control of the RTG while in the Shuttle bay - landline.

3. Surface Charge Buildup

With a Jovian charged particle environment of 5×10^{-10} A/cm², the potential between any area on the surface of the RTG greater than 0.5 cm² and chassis ground, must be less than 10 volts. This interface requirement on the RTG is met with the aid of a conductive emissive coating described in Section VII-G.

4. Magnetics

The RTG is designed to minimize the magnetic field it produces while operating at 30 volts. Specifically the RTG must produce:

- a. a maximum radial dipolar field less than 10 nano tesla
- b. a quadrapole and higher order field less than 40 nano tesla

Adherence to these magnetic interface requirements is accomplished without the use of compensation loops within the RTG.

5. Receptacles/Cabling

Separate receptacles and cabling are provided for the instrumentation and power outputs of the RTG. Connectors are controlled so as not to be interchangeable. Power connector pins are redundant. All cables meet the surface charge requirements of preceding paragraph A.3. Details of the receptacle/cable interfaces are found on the JPL Interface Control Drawing, No. 10086760.

B. MECHANICAL INTERFACE

1. Envelope Dimensions

Envelope restrictions for the RTG are described by JPL drawing 10086760. Initially the RTG was constrained to be within certain values of length and diameter. As detailed designs progressed at JPL and TES, specific RTG dimensions were fixed at the values

described in the JPL interface drawing and the TES housing assembly drawing, SIG 110050. These dimensions include thermal growth and interface tolerance effects.

2. Mass Properties

The RTG center of mass must be within a 2.54 cm diameter sphere centered at fixed coordinates relative to the RTG. The center of mass for any one RTG must be known to lie within a .05 cm diameter sphere. Variation of the center of mass location among all delivered RTG's must be within ± 0.5 cm both axially and radially.

3. Mounting Provisions

The RTG is mounted to the spacecraft structure by mechanical attachments at three RTG locations.

These locations, described by Figures III-1, III-2, and III-3, serve the following functions:

<u>Location</u>	<u>Figure No.</u>	<u>Function</u>
Power section cover at vent system end	III-1	Primary RTG/spacecraft interface during launch... Mounting includes provision for RTG gas vent actuation at RTG deployment.
Power section (on side adjacent to radiator interface)	III-2	Secondary RTG/spacecraft interface during launch to provide lateral support. Monoball type interface at juncture of RTG and the spacecraft truss.
Radiator end	III-3	Attaches RTG to spacecraft boom and serves as only support during mission after RTG is deployed,

4. Fill Gas Venting

As of the end of January 1979, the RTG/spacecraft interface for the vent device was the only unresolved mechanical interface. The TES configuration is described in

FIG. III-1. RTG SPACECRAFT MOUNTING VENT SYSTEM END

FIG. III-2 RTG SPACECRAFT MOUNTING POWER SECTION-POWER OUTPUT END

detail in Section IV.B. The vent actuates as the RTG is deployed but RTG venting must not perturb the stability of the spacecraft.

5. Structural Design Environments

Structural design environments for the RTG are presented in JPL Specification ES 512281 dated October 1978. Pertinent values have been extracted from the JPL Specification and are contained in Appendix E for reference.

6. Micrometeoroids

RTG performance at end-of-mission is quoted exclusive of meteoroid effects. However, RTG design does account for meteoroid effects in order to maximize RTG reliability within weight constraints. The meteoroid fluences specified by JPL for RTG design, are shown in Table III-1.

C. THERMAL INTERFACES

1. Thermal Environment

During the sequence of events leading to deployment of the RTG's, it is necessary to maintain the RTG housing temperature between approximately 100°C and 140-160°C. The lower limit is to prohibit the phase transition of the P-leg thermoelectric material. The upper limit is to assure the structural integrity of the heat pipe/saddle solder joint. While in the Shuttle, but prior to lift off, the cargo bay will be air conditioned and the RTG's will be water cooled (or heated) by the GSE cooling system to maintain the RTG housing temperature within the above range. (A resistance temperature measurement device will measure the housing temperature.)

A few seconds before lift off the GSE cooling system will be disconnected and replaced by the Shuttle's cooling system (see Galileo launch sequence, Figure II-1).

TABLE III-1
METEOROID DESIGN ENVIRONMENTS

<u>Particle Mass (grams)</u>	<u>Integral Fluence Received During Transit (Particles m^{-2} of mass greater than M)</u>	<u>Integral Fluence Received During Orbit (Particles m^{-2} of mass greater than M)</u>	<u>Mission Integral Fluence (Particles m^{-2} of mass greater than M)</u>
10^{-10}	2.56×10^3	7.46×10^3	1.00×10^4
10^{-8}	3.2×10^2	9.3×10^2	1.25×10^3
10^{-6}	13	37.9	51
10^{-5}	8.1×10^{-1}	2.4	3.2
10^{-4}	4.9×10^{-2}	1.4×10^{-1}	1.9×10^{-1}
10^{-3}	3.0×10^{-3}	8.7×10^{-3}	1.2×10^{-2}
10^{-2}	1.8×10^{-4}	5.2×10^{-4}	7.0×10^{-4}
10^{-1}	1.1×10^{-5}	3.2×10^{-5}	4.3×10^{-5}
10^0	6.8×10^{-7}	2.0×10^{-6}	2.7×10^{-6}
Mean relative speed (km/s)	14	15.5	15
Particle mass density (g/cm ³)	0.5	0.5	0.5

It will be necessary to attain an altitude of 50,000 ft before the Shuttle's evaporative cooling system is turned on. During the interval of a minute or less, heat can be removed at the rate of 5,000 Btu/hr. While less than desired, this rate should not present a problem.

The cargo bay doors are opened at 1:02:30 into the mission and the IUS/SC system is tilted out at 5:30:00. The Shuttle's cooling system is disconnected from the RTG at 6:10:00 after which heat can only be rejected from the RTG's via radiation.

The IUS/SC is released from the Shuttle at 6:31:28 and the RTG's are deployed at 10:45:47. During this period the RTG's are in a stowed position and their view of space is restricted. Also, the spacecraft undergoes a spin-up period of approximately 4 1/2 minutes during the third stage burn, causing the heat pipes to operate off-optimum. Based on experimental data, a housing temperature rise of 6-8°C is anticipated during the spin period.

Studies made of the RTG temperature after the water cooling is removed are not conclusive regarding whether the housing can be maintained at less than its allowable temperature limit (140-160°C). One concern is the 25 minutes immediately after the Shuttle's cooling system is disconnected and while the RTG's have little or no view of space. Also the ten minute interval thereafter may be a problem since the system will not be oriented to provide minimal solar and earth IR heat loads to the RTG's.

If the decision is made to abort before the IUS/SC is released, then the evaporator is started up, the cargo bay doors are closed and the Shuttle is programmed to land. The evaporators remain on until either the GSE RTG cooling water is connected at the landing strip or the water tank exhausts its supply.

2. Heat Pipe Orientation

One of the fundamental elements of the heat pipes are the internal axial grooves that transport the liquid working fluid (water) from the condenser region of the pipe to the evaporator where it absorbs the heat load and evaporates. The capillary pumping head of the grooves must be sufficient to overcome viscous and dynamic losses of the system and it must compensate for adverse gravity effects. In most heat pipe installations there is a preferred pipe orientation in which the body forces augment the pumping head. In the case of the SIG design, the preferred orientation is realized after the RTG's are deployed. In this orientation, the direction of the centrifugal forces, resulting from spacecraft spin, will coincide with the direction of the heat pipe's capillary forces.

Prior to the RTG deployment, the heat pipes are in a stowed position and their performance will be adversely affected by spacecraft spin. Based on spin tests it is anticipated that a 6-8°C temperature rise will occur in the RTG as a result of the 50-70 RPM spin during the third stage burn maneuvers.

3. Ground/Launch Auxiliary Cooling

Auxiliary cooling (or heating) is provided to the SIG RTG until the time the IUS/SC is released from the Shuttle. Until a few seconds before lift off, cooling (or heating) is provided by GSE cooling kits which consist of a heat exchanger, pump and accumulator. The GSE takes heated water from the RTG and pumps it through the cooling coils and back to the RTG's in a closed circuit.

Cooling is also provided on the trailer - transporter that is used in moving the IUS/SC into the cargo bay of the Shuttle. The cooling unit on the trailer-transporter consists of a fan cooled radiator, a water pump and a motor-generator set that provides power for the fan and both the water pumps.

After lift off, the RTG's are cooled by the Shuttle's cooling system which operates in several different modes. The mode changes with mission time. Initially a payload heat exchanger is used that can remove only 5,000 Btu/hr from the RTG's and stores the heat elsewhere within the Shuttle. When an altitude of 50,000 ft is reached, an evaporator cooling system is started increasing the heat removal to approximately 15,000 Btu/hr. The evaporated water is dumped overboard. The evaporators are kept on at least until the payload bay cargo door are opened after lift off plus 62 minutes. After the doors are opened, 20,000 - 30,000 Btu/hr can be removed via radiation from the inner surfaces of the doors which are part of the Shuttle's cooling system when in orbit.

IV. GENERATOR DESIGN

The SIG design was initiated in 1973 from a general study contract to evaluate various RTG concepts, Refs. 1-3, with the emphasis on Cm-244 fuel. Additional design studies were performed in 1974 and 1975 with the Pu-238 fuel form, Refs. 4-7. Then in 1976, a major redirection was given to use the developed MHW heat source. Studies with this constraint were presented in Refs. 8-10 and formed the basis for the design proposed for the Galileo mission contract. These studies also were used to formulate the contractual performance objectives for the Galileo mission.

A. PERFORMANCE OBJECTIVES

Performance requirements for the SIG Galileo mission were established in early 1978 based on selenide technology activity at 3M and performance studies conducted at TES during 1977. These requirements, summarized in Table IV-1, were incorporated into the 3M and TES contracts. A brief discussion of these numbers follows.

Beginning of Life (BOL) is defined for a selenide system as after 1000 hours of operating time because of a seat-in (improvement) phenomenon associated with the thermoelectrics, on the order of a 10-20% power increase. The 860°C hot junction operating temperature is one of the major design parameters in determining system performance. Hot junction temperature previously had been specified at 900°C but it appeared prudent to reduce it 40°C based on 3M Company test data. The internal vacuum requirement was established to enhance system efficiency and reduce the weight/reliability dependence associated with meteoroid puncture.

TABLE IV-1
SIG RTG PERFORMANCE REQUIREMENTS

	<u>RTG (TES)</u>	<u>Converter (3M)</u>
<u>BOL (1000 operating hours)</u>		
Power output (watts)	≥ 214	≥ 217
Efficiency (%)	---	≥ 10.1
Voltage (volts, dc)	30.0 ± 0.5	30.0 ± 0.5
Environment	Vacuum	Vacuum
Temperature - Radiator fin root ($^{\circ}\text{C}$)	123 ± 3	---
- Cold frame ($^{\circ}\text{C}$)	---	≥ 135
- Hot junction ($^{\circ}\text{C}$)	---	860 ± 3
- Cold junction ($^{\circ}\text{C}$)	---	160 ± 3
<u>EOM (58,760 operating hours)</u>		
Power output (watts)	≥ 185	≥ 190
Efficiency (%)	---	9.6
Reliability and confidence	0.95 - 50%	0.95 - 70%
Voltage (volts, dc)	30.0	30.0
Environment	Vacuum	Vacuum
Temperature - Radiator fin root ($^{\circ}\text{C}$)	100	---
- Cold frame ($^{\circ}\text{C}$)	---	≥ 112
- Cold junction ($^{\circ}\text{C}$)	---	137

NOTES:

- Heat source at delivery to TES = 2460 ± 10 thermal watts.
- BOL power - measured or projected to 1,000 hours.
- EOM power - predicted by analytical techniques.
- EOM reliability is defined to be exclusive of meteoroid penetration.

The specified cold junction value of 160°C represents a good compromise on system efficiency and weight; supporting studies show this value to maximize specific power. The power requirements, ≥ 214 W(e) and ≥ 217 W(e) for TES and 3M, follow directly from the thermoelectric converter efficiency assessment, $\geq 10.1\%$, and the minimum system fuel inventory of 2450 watts(t). The 3 W(e) difference between the 3M and TES values is an allowance for the effects of a hot-to-cold ΔT less than 860-160°C (due to insulation conductivities possibly being more than nominal) and internal generator wiring losses between the converter and the generator power output receptacle.

The End of Mission (EOM) time is defined as 50,000 hours after launch with launch occurring 1 year after heatup. Fuel decay over this total time span would account for an electrical power loss of 17 watts. Additionally, an allowance of 12 watts was assigned for all degradation effects. Ten of the twelve watts were allocated to 3M for thermoelectric effects and the remaining two watts(e) to TES for other system effects, primarily insulation conductivity change. End of mission temperatures on Table IV-1 were established from preliminary thermal analyses performed on the system taking into account fuel decay and diminishing solar effects.

The EOM reliability/confidence requirement of 0.95%/50% has also been considered in establishing EOM power requirements. Specifically, all power output values in Table IV-1 were estimated to be 10 watts below average power at the time the power requirements were set. Statistics from prior RTG programs indicated the required standard deviation on power to be feasible.

In summary, the thermoelectric converter performance values in Table IV-1 were considered to be achievable goals based upon basic thermoelectric test data. The RTG system values were based on a system assessment using these converter performance

values. It should be recognized that the Table IV-1 requirements did not stem from demonstration of prototypical generator hardware; they were deemed achievable, however.

B. DESIGN DESCRIPTION

1. Summary

The SIG/GM RTG flight configuration shown in Figs. IV-1 and IV-2 is a finned, right circular cylinder 50 in. long and 24.3 in. across the fin tips; nominal system weight is 102.5 lbs. The 50 in.-long finned cylindrical housing consists of two approximately equal length cylindrical sections: (1) the power section housing, containing the thermoelectric converter and radioisotopic heat source, and (2) the radiator extension, a thin-wall finned, hollow cylinder sized to increase the effective heat rejection area of the power section housing to that required to maintain the nominal 125 °C radiator fin root operating temperature. The two cylindrical sections are joined by riveting. Twenty longitudinal copper-water heat pipes provide the means for high conductance of the unconverted heat from the thermoelectric conversion process to the heat rejection surfaces of the power section housing and radiator extension; ten pipes extend the full length of the generator while ten are the length of the power section housing. Two circumferential water coolant lines, located on the power section housing in the area of the two thermoelectric rings, provide a means for heat removal during certain ground handling operations and prelaunch/launch operations up to time of spacecraft removal from the Shuttle in earth orbit.

The RTG attachment to the spacecraft is at three points on the cylindrical housing structure: mounting lugs located at the top and bottom of the power section housing for primary support during launch dynamics, and a mounting flange at the upper end of the radiator extension for spacecraft boom attachment. After spacecraft release from the Shuttle in earth orbit and termination of spacecraft engine burns that put the spacecraft in the interplanetary cruise mode, pyrotechnic devices "blow" the attachments at the two points on the RTG power section housing, allowing the boom with attached RTG to deploy.

FIG. IV-1. SIG/GM RTG FLIGHT CONFIGURATION

FIG. IV-2. SIG/GM RTG OVERALL DIMENSIONS

Figure IV-3 shows a cross section of the RTG power section while Fig. IV-4 shows an exploded view. The centrally-located MHW-type heat source is supported at its ends by CBCF-3 graphite and Min-K TE 1400 load bearing-type thermal insulations. The graphite insulation permits direct load bearing on the $\sim 1000^{\circ}\text{C}$ heat source and reduces the temperature to $\sim 810^{\circ}\text{C}$, a safe level for operation of the more thermally efficient Min-K TE 1400 insulation.

Two selenide-based thermoelectric converter rings encircle the radioisotopic heat source. Each of the two rings contains 28 six-couple modules, for a total of 336 couples per generator. The couples are connected in a parallel-series arrangement (168 pairs of parallel couples are electrically connected in series). The thermoelectric couples in each ring are retained in position between the generator housing and a circular graphite hot frame by springs in the module cold end hardware which load the individual N and P couple legs.

Thermal insulation adjacent to the two thermoelectric converter rings is made up of 60 layers of 0.3 mil molybdenum multifoil insulation held in position by springs located in the cold end of the multifoil pack. Fiberfrax insulating material is used both to pack crevices at the edges of the multifoil to minimize thermal losses and to provide the thermal insulation within the thermoelectric module. A non-load bearing Min-K TE 1800 insulation ring surrounds the aforementioned heat source load bearing insulation at each end of the generator.

Within the multifoil sleeve assemblies are interlayered zirconium foils that function as hot side and cold side "getters." The "getter" location (and hence temperature) is dictated by the type of contaminant gas that is being "gettered."

FIG. IV-3. SIG/GM CROSS SECTION

FIG. IV-4. SIG/GM RTG POWER SECTION(EXPLODED VIEW)

The electrical power generated by the two series-connected converter rings exits the power section housing through the power output receptacle located in the top cover (radiator end). A short cable within the radiator extension cylinder routes the power output to a receptacle at the boom end of the radiator extension to facilitate mating with the spacecraft power cable.

On the opposite (or lower) end cover of the power section housing are located the generator vent system and gas maintenance valve. The vent system, consisting of a spring-loaded, sharply-honed spear assembly and a thin metal diaphragm in the end cover, is mechanically actuated when the booms are deployed on the initiation of interplanetary cruise; the mechanically-constrained spear is freed upon boom deployment and spring-driven through the diaphragm to vent the cover gas and heat source-generator helium to space vacuum. Prior to this venting action, the hermetically-sealed power section housing contains an inert fill gas to minimize hot side temperatures during the ground handling and launch operations; during ground operation, the cover gas is maintained and monitored, as required, through the gas maintenance valve.

Table IV-2 summarizes the significant configuration/material aspects of the flight RTG. The overall design discussion above, as well as this summary table, are amplified in subsequent sections in this chapter.

TABLE IV-2

SIG GALILEO FLIGHT CONFIGURATION/MATERIAL SUMMARY

Item	Material	Configuration
Improved Heat Source	MHW, with 3D aeroshell	Right circular cylinder, 7.10" O.D. x 16.5" high
Heat Source Support	Min-K TE 1400/CBCF-3	20/16 pcf, load bearing; 2.0" total thickness per end
Thermoelectric P-leg	Copper-silver Selenide	Length - 0.3" No. - 336 Diameter - 0.254"
Thermoelectric N-leg	Gadolinium selenide	Length - 0.3" No. - 336 Diameter - 0.30"
Module Cold End Current Straps	Copper	0.010" thick; parallel - series circuit
Module Cold End Hardware	Copper, BeO dielectric, steel spring, solders	3M multi-layer follower design
Module Insulation	Fiberfrax 660 AH paper	Stuffed
Module Hot Side Dielectric	Al_2O_3	Sprayed on Mo straps; 0.015" thick
Module Hot End Current Straps	Molybdenum	Sculptured, H configuration; .062" thick
Module Hot Frame	POCO AXF-Q1 Graphite	Hollow cylinder 3.0" high x 7.66" I.D. 0.23" min. thick. 28 flats
Non-Module Radial Insulation	Multifoil-Mo/ ZrO_2	58 pcf, 0.3" thick, 60 layers
Power Section Housing	Aluminum 6061-T6	Cylindrical; 10.3" O.D. x 24.1" high; 0.045" min. thickness
Radiator Extension	Aluminum 6061-T6	Cylindrical; 10.3" I.D. x 25.9" high; .020 min. thickness
Heat Pipes	Copper-water	Axially-grooved; 0.307" O.D., 8 mil min. wall; No. = 10 long, 10 short

2. Radioisotope Heat Source

The improved Multi-Hundred Watt (MHW) heat source for SIG/GM as shown in Fig. IV-5 is a modified version with respect to that used for the Mariner-Jupiter/Saturn mission. For that mission the aeroshell, designed to provide protection from reentry heating, was fabricated from POCO AXF-5Q, a polycrystalline graphite. In addition, a thin (0.130 in.) cylindrical shell of Pyrocarb 406 surrounded the POCO aeroshell and was designed to withstand the initially high thermal stress associated with the first few seconds of reentry following separation from the spacecraft and RTG.

The modified heat source incorporates two major changes:

- a. The POCO AXF-5Q aeroshell is replaced by an aeroshell fabricated from 3D carbon-carbon (3DCC).
- b. The Pyrocarb 406 ablation sleeve has been eliminated.

The incentive to change aeroshell material for Galileo is a result of the need to withstand more severe potential launch pad accidents. Since the Galileo spacecraft will be launched aboard the Shuttle, a blast overpressure would be associated with a yield in excess of 1-1/2 million lbs of liquid propellant. This represents a far greater magnitude of propellant than that used in past missions, i.e. Titan/Centaur, etc.

The 3D carbon-carbon also has a significant capability to withstand thermal stress. The stresses generated do not become adversely significant since the strain redistributes; also cracks, if they are initiated, do not readily propagate as in brittle polycrystalline graphites. For this reason the ablation sleeve is not required and therefore was removed. These two changes also provide a two-pound weight reduction per heat source.

Two 3D carbon-carbons are employed. The two end closures will be fabricated

FIG. IV-5. MHW HEAT SOURCE FOR SIG/GM

from AVCO's Fine Weave Pierced Fabric (FWPF) and the cylindrical shell will be made from a carbon-carbon from General Electric where fiber bundles are in the R, θ , Z coordinate directions. Both carbon-carbons utilize the high modulus PAN fiber and both are carbonized with the Hi PIC process with pressure to 15000 psi. The more detailed data for the two materials are provided in Table IV-3.

The PuO_2 fuel as a solid pressed ceramic contained within the heat source in the form of twenty-four spheres providing a thermal inventory of approximately 2460 watts. Each fuel sphere is contained within an iridium alloy shell that is designed to provide post impact containment of the isotope. The metallic shell is surrounded by a graphite impact shell (GIS) designed to provide impact protection to the inner two components. The GIS, consisting of a body and closure cap, is fabricated from resin-impregnated wound spheres of Thornel-50 yarn and pyrolyzed at 2500°F. The closure cap is threaded into the body in the fueling operation to confine the clad fuel sphere. The nominal thickness of the GIS is 0.46 inch.

Each of three sets of eight fuel sphere assemblies (FSA) is held within a cylindrical shell (retainer ring) by a tie bolt and a pair of conical sphere locks.

During heat source fueling the three eight-packs are loaded into the aeroshell with a compliant pad between each eight-pack.

To insure that the heat source maintains structural integrity with respect to launch loads, shock, random vibration, etc., the twenty-four FSA's are preloaded within the aeroshell by sets of disc springs (Pyrocarb 508) at each end of the heat source. The final step in the assembly is the installation of the end closure that is mated to the cylindrical aeroshell by means of POCO snap rings (one ring at each end).

TABLE IV-3
3D CARBON-CARBON MATERIAL DEFINITIONS

	<u>GE 3DCC</u>	<u>AVCO FWPF</u>
Weaving:	General Electric	Textile Products, Inc.
Preform:	General Electric	AVCO
Densification:	15000 psi HiPIC (4 cycles)	15000 psi HiPIC (5 cycles)
Fiber:	HM 3000 PAN (Polyacrylonitrile)	HM 3000 PAN
Fibers/bundle:	10,000	3000/1000
Fiber Orientation:	R, Θ , Z	X, Y, Z
Pitch:	CP-277-15V Vacuum coal tar	CP-277-15V Vacuum coal tar
Density:	1.89 gm/cc	1.98 gm/cc

Additional detail on the heat source may be found in Ref. 11. A drawing of the new aeroshell is given in SIG 125008.

3. Thermoelectric Converter

The thermoelectric converter for the SIG was to be supplied by the 3M Company as GFE to TES. The converter consists of two rings with each ring containing twenty-eight segments, and each segment containing six couples. The basic subassembly of the converter is the segment described by Figure IV-6. Each of the 336 couples within the converter consists of a P-leg and N-leg stack constrained between a cold end subassembly and a hot frame.

A parallel series wiring arrangement is used as shown by Figure IV-7. Under normal operating conditions, current flows in parallel through each set of legs. Figures IV-6 and IV-7 are representative of the configuration existent at the Preliminary Design Review (PDR), November 30, 1978. Following the PDR, a number of changes were incorporated by 3M into a proposed new reference design defined by Ref. 12. These changes include a skewed and lower hot junction temperature, a segmented N-leg, unpartitioned P-leg, reduced P-leg length, and lower power output.

Table IV-4 shows a brief chronological comparison of S/N 1 thermoelectric converter parameters using data supplied by the 3M Company at three key review points during the SIG/Galileo program.

The basic interface document between 3M Co. and TES is TES drawing SIG 116001. This released drawing defined all the mechanical and electrical interfaces between TES and 3M, including processes for handling and checkout.

FIG. IV-6. SEGMENT COMPONENTS

FIG. IV-7. CIRCUIT DIAGRAM FOR TWO RING SIG CONVERTER

TABLE IV-4
CHRONOLOGICAL COMPARISON OF S/N 1
THERMOELECTRIC CONVERTER PARAMETERS

<u>Parameter</u>	<u>Conceptual* Design Update 6/22/78</u>	<u>PDR ** 11/30/78</u>	<u>Technical *** Review Meeting Status 1/22/79</u>
T_{HJ}/T_{CJ} , °C	860/160	860/160	815(P)/160 845(N)/160
DN/DP, inches	.304/.254	.315/.260	.200/.260
LN/LP, inches	.300/.300	.300/.300	.300/.261
Voltage, volts	30	30	30
Couples	336	336	336
Weight, lbs	16.0	14.2	16
Converter efficiency, BOL	10.1	10.2****	8.4****
RTG power output, watts, BOL	229	230	188

*Reference 3.

**Reference 4.

***Reference 2.

****Calculated from given 3M data.

4. Thermal Insulation/Getter

The SIG insulation system is comprised of two types - fibrous and multifoil. Each is described below along with the getter which is a part of the multifoil system.

a. Fibrous insulation

Fibrous insulation is used primarily in the ends of the power section housing in both load and non-load bearing modes. As a load carrier, a composite system of CBCF-3 graphite⁽¹⁾ and Johns Manville silica-based Min-K TE 1400 insulations are used with an initial preload of about 100 psi. These insulations are in the shape of right circular cylinders (see Fig. IV-3), slightly larger in diameter than the MHW heat source. The CBCF-3 insulation is 1.3 inches thick and of density 15.6 to 18.1 pounds/ft³ while the Min-K TE 1400 is 0.7 inch and of density 20 pounds/ft³. This total thickness of 2.0 inches represents a good compromise between heat losses, system weight, Min-K fabricability and Min-K dynamic survivability. The CBCF-3 insulation is a high temperature material capable of being used next to the heat source at ~1000°C while the Min-K TE 1400 is used in series with it, below ~810°C, in order to take advantage of its lower thermal conductivity.

Min-K TE 1800 insulation at 20 lbs/ft³ surrounds the load bearing discs at each end of the generator and is used instead of Min-K TE 1400 because of its somewhat higher temperature capability to ~850°C. The Min-K TE 1800 rings are essentially non-load bearing.

Fiberfrax 660AH paper (Carborundum, Inc.) is used by the 3M Company within the T/E module and at one end of the center multifoil section.

All of the fibrous insulations are outgassed in detail prior to use. The

⁽¹⁾Custom fabricated by ORNL; CBCF ≡ carbon bonded carbon fiber (rayon based);
Ref. Section VII-J.

schedules⁽¹⁾ for the Min-K's and CBCF-3 are as follows:

<u>Insulation</u>	<u>Hold Point</u>
Min-K TE 1400	168 hours, $\leq 10^{-6}$ torr, 870°C
Min-K TE 1800	168 hours, $\leq 10^{-6}$ torr, 910°C
CBCF-3	64 hours, $\leq 5 \times 10^{-6}$ torr, 1350°C

After this primary outgassing, the insulation pieces are constrained environmentally to < 50 ppm H_2O and O_2 and furthermore undergo secondary outgassing when the entire power section is processed.

b. Multifoil insulation/getter

Multifoil insulation was selected for insulating the lateral surfaces of the heat source adjacent to the thermoelectric modules. Studies showed that for the SIG internal vacuum design environment multifoil yielded both reduced heat losses and reduced weight as compared to a composite fibrous system of CBCF-3/Min-K.

The multifoil design is defined in drawing SIG 110073. The hardware consists of three annular subassemblies as depicted on Fig. IV-3. Each is totally fabricated by Thermo Electron Corporation (TECO) under subcontract to TES. Each subassembly consists of an inner 0.003 in. molybdenum cylinder, an outer 0.010 in. titanium alloy cylinder and 60 layers of 0.0003 in. molybdenum foil in between. Each foil is coated on one side with electrostatically sprayed zirconia particles of density 0.06 mg/cm^2 to reduce the contact thermal conduction component. Spacing between foils within each of the three-sleeve assemblies is nominally 0.005 in. from the initial winding operation. Each assembly contains four axial titanium alloy brackets with spring-loaded pins. These brackets are EB welded to the outer titanium shell. The contained pins

⁽¹⁾TES detail outgassing procedures were in preparation at the conclusion of the SIG Program, but not released.

engage mating pins at the edges of the thermoelectric module to constrain the sleeves both axially and circumferentially within the generator.

Another common feature in the sleeves is that the 0.0003 in. multifoil is perforated with 0.250 in. diameter randomly spaced holes, six per layer, in order to improve gaseous conductance and thereby enhance the getter function (as discussed below). Additionally, axial movement (telescoping) within a sleeve is minimized by the use of 0.010 in. molybdenum wire ties through each stack at three places near each end.

The center section contains some additional hardware at one end not found on the other two sleeves. This hardware consists of a fixed cold aluminum angle and a movable hot side molybdenum angle, both functioning to retain Fiberfrax insulation which is stuffed in place after the multifoil and thermoelectric modules are assembled. The stuffing is required to fill a gap that results from assembly tooling used to install the thermoelectric module.

Each of the three sleeves contains zirconium getters intended as a sink for various gaseous sources. All sleeves contain a hot side getter for the following out-gassing products: H_2O , O_2 , CO and CO_2 . The two end sleeves additionally contain a cold side getter intended as a sink for hydrogen, the by-product of the reaction of water vapor with the zirconium hot side getter. Getter activation is accomplished by vacuum annealing, i.e. $\leq 8 \times 10^{-7}$ torr, at 1025°C for two hours.

The hot getter consists of one full wrap of .007 in. zirconium foil inside each sleeve assembly and is retained by the molybdenum tie wires mentioned previously. The hot getter is spaced from the inner can 0.010 in. to 0.030 in. by nodes formed in the getter. Temperature at this location is ~ 925 to 950°C .

The cold getter in each end sleeve assembly consists of one full wrap of 0.004" zirconium foil located within the multifoil stack 10 layers in from the outer titanium alloy can. This wrap is interleaved with the continuous molybdenum multifoil but is separated from it on each side by 12 equally spaced .005 in. diameter molybdenum wires permanently attached to the adjacent multifoil in the axial direction. The cold getter would operate at $\sim 550^{\circ}\text{C}$ during normal system operation based on recent temperature profile testing at TECO. This test data indicates a need to locate the cold side getter somewhat nearer to the titanium alloy outer can. A more optimal temperature range for this getter is 300 to 400°C .

5. Power Section Housing

The power section housing is the main containment structure for the generator. It is essentially a thin-wall 6061 aluminum hollow cylinder with ten staggered radial fins to augment the heat rejection. Figure IV-8 is a photograph of a power section housing after fin welding. Approximate dimensions are 24.1 in. long x 10.2 in. ID x 24.3 in. fin tip-to-tip. Wall thickness varies from a minimum of 0.045 in. near the center to about 1/4 in. near the ends. On the exterior of the housing are twenty evenly-spaced longitudinal approximately 5/16 in. diameter saddles into which heat pipes are soldered. Additionally, there are two discontinuous circumferential saddles, approximately 1/4 in. diameter, on the exterior into which copper-water auxiliary cooling loops are soldered. All saddles are electroplated at Battelle Columbus Labs. with copper (zincate, Cu strike, Cu plate) and then with 60 Sn • 40 Pb to provide a solderable attachment.

Near the radiator end of the housing is a circumferential flange with two lugs for attachment to the spacecraft (+ x axis) and two lugs for handling of the completed generator

FIG. IV-8. POWER SECTION HOUSING FOLLOWING FIN WELDING

(-x axis). This flange also contains eight holes to aid heat pipe installation into the vertical saddles, otherwise impossible because of the circumferential cooling loops which are installed first.

Near the fueling end of the housing are two circumferential flanges with several purposes. First, they contain four lugs near the +x axis for spacecraft attachment. Second, two other lugs near the -x axis are used for handling in conjunction with the two at the radiator end of the housing. Finally, the flange nearest the fueling end provides a sealing surface for processing the generator and for leak checks after the end cover is welded to the housing. To ensure an adequate seal closure on this surface, two additional bolt holes are provided for a total of eight.

The housing also exhibits low RMS finishes in several areas. Internally in the two regions where the thermoelectric modules seat, the finish is 40-80 μ -in to enhance heat transfer from the modules to the housing. Additionally, near both ends of the housing are regions used for O-ring seals used during generator processing requiring finishes of 32 μ -in or better.

The fabrication and assembly of the power section housing involves a number of steps including two-stages of machining, electroplating, cooling tube soldering and leak checks. A summary of these steps in the order performed is given in Table IV-5 along with selected remarks and a list of the key engineering drawings.

These fabrication steps were employed to produce the power section housing for ETG S/N 1. The completed housing was delivered to the 3M Company in December 1978 for further assembly steps as described in Section IV.C. Additionally, a second housing was processed through the final machining step.

TABLE IV-5

POWER SECTION HOUSING FABRICATION/ASSEMBLY STEPS

<u>Step</u>	<u>Drawing</u>	<u>Remarks</u>
Preliminary machining	SIG 110051	Lathe and vertical mill work; all saddles formed.
Electroplating of Cu and Sn • Pb in all saddles	PS 0300037 PS 0300038 SIG 110050	Performed at BCL. Extensive masking required.
Final machining	SIG 110050	Also removes any plating inadvertently leaked under masking.
Leak check	SIG 110021	$\leq 1 \times 10^{-9}$ scc He/sec-atm at room temperature.
Cooling loop soldering	SIG 110050 SIG 110015 SIG 110018	Radiography, aluminum hard- ness check, and tube leak check required.
Fin welding	SIG 110050 SIG 110054 SIG 110053	TIG weld.
Cooling tubes, brazing	SIG 110050 SIG 110018	Entrance and exit lines with flared tube fittings.
Leak check	SIG 110021	$\leq 1 \times 10^{-9}$ scc He/sec-atm at room temperature.

6. Radiator Extension

The function of the radiator extension (refer to Figs. IV-1 and IV-2) is to increase the effective heat transfer area of the power section housing to that required for operation of the housing fin root temperature at the 125°C nominal design point. The extension is a thin wall 6061 aluminum hollow cylinder approximately 25.9 in. long x 10.3 in. ID x 24.3 in. across the fin tips. The cylinder wall thickness is 0.025 in. over most of the cylinder length and thickens to 0.055 in. at the boom and power section mounting ends. Ten staggered radial fins augment the heat rejection capability of the cylinder. Due to the heat transport characteristics of the heat pipes, the five fins on the radiator extension farthest from the power section housing are smaller than the five fins nearer the housing to balance the heat transport among the ten heat pipes.

On the exterior of the radiator extension are ten integrally-machined, evenly-spaced, longitudinal saddles into which the ten long heat pipes emanating from the power section housing are soldered. All saddles are electroplated at Battelle Columbus Laboratories with copper (zircate, Cu strike, Cu plate) and then with 60 Sn - 40 Pb to provide a solderable attachment. At the boom end of the saddle is a provision to tightly secure each heat pipe to minimize stresses in the heat pipe solder bond due to boom-induced loading in the radiator extension skin.

Also at the boom end of the radiator extension is a circumferential stiffening flange with three evenly-spaced pads for attachment to the spacecraft boom; boom attachment is by means of two 1/4 in. bolts at each pad location (see Fig. IV-9). Two circumferential ring frames support the thin-wall cylinder on the ID at two locations along the cylinder length. The frames are attached to the cylinder wall by riveting (see Fig. IV-10).

At the power section housing end, the radiator skin thickens to 0.055 in. and is

FIG. IV-9. RADIATOR EXTENSION CROSS SECTION

FIG. IV-10. RADIATOR ASSY END VIEW (AS VIEWED FROM HOUSING END)

slotted to form ten evenly-spaced, 1 in. long mounting tabs (see Fig. IV-10). Each of the ten tabs is located at a heat pipe saddle and is riveted to the power section housing to provide the mechanical tie of the radiator extension to the housing. Angular orientation of the extension relative to the housing is such that the heat pipe saddles for the ten long heat pipes line up to permit soldering to both sections simultaneously.

To facilitate cable mating during spacecraft integration, two electrical receptacles are located at the boom end of the radiator extension, one for radiator/housing surface temperature instrumentation (RTD's) and the other for RTG electrical power output (see Fig. IV- 9). The receptacles are located at approximately 45° (midway) between the +x and +y axes of the RTG. The instrumentation receptacle is located on the radiator extension cylinder OD, while the power output receptacle is on the ID. A cable inside the radiator cylinder connects the radiator power output receptacle to the power output receptacle in the cover of the power section housing.

Fabrication and assembly of the radiator extension includes steps similar to those previously described for the power section housing. These include:

- a. Preliminary machining
- b. Electroplating of Cu and Sn • Pb in all saddles
- c. Final machining
- d. Ring frame riveting
- e. Fin welding

7. Heat Pipes

The high heat transport capability of copper-water heat pipes makes possible the relatively light heat rejection radiator of the SIG/GM RTG when compared to conventional conductive fins. Weight-wise, the most attractive heat pipe arrangement for the Galileo

application was determined to be twenty heat pipes, ten 47.1 in. long and ten 16.6 in. long. The shorter heat pipes are attached (soldered) only to the power section housing, while the longer pipes traverse the length of the generator. Each heat pipe picks up unconverted heat from the two thermoelectric converter rings over a total length of 6 in. (i.e., each ring is 3 in. high). Generally, each heat pipe may be thought of as transporting unconverted heat to its associated fin and adjacent housing/radiator extension heat rejection surfaces. Thus heat from the long heat pipes is rejected primarily by the radiator extension, while that from the short heat pipes is rejected primarily by the power section housing. The staggered fin arrangement, with each fin being fed by a heat pipe at its root, minimizes housing thermal gradients and hence gradients in the thermoelectric converter.

Since heat pipes operate more efficiently when given gravity assist, excess water will be present in the heat pipe during the various thrust and spin dynamic conditions encountered during the mission sequence. Since this excess water, which collects in the evaporator end of the heat pipe, could result in water blockage of the lower converter ring (ring towards fueling end) and hinder heat transfer from the ring into the pipe, a water reservoir is provided on each heat pipe. The reservoir is simply an extension of the heat pipe beyond the lower converter ring towards the fueling end to accommodate any excess water in the pipe. Since there is less excess water in the short heat pipes, their reservoir extension is shorter than that for the long pipes (see Figs. IV-1 and IV-2).

The heat pipes are axially-grooved internally (including the reservoir) to provide the necessary wicking action for operation in a zero g environment. Figure IV-11 is a cross section of the heat pipe showing the groove geometry. The pipe outside diameter

-009 ASSY

-019 ASSY

ASSY	A DIM $\begin{smallmatrix} +.00 \\ -.25 \end{smallmatrix}$	B DIM REF	C DIM REF
-009	47.08	7.0	25.442
-019	16.59	—	4.955

TAKEN FROM:

SIG 110040

SIG 110044

FIG. IV-11. HEAT PIPE ASSEMBLY

is 0.307 in. with a wall thickness of approximately 0.016 in. (0.008 min.). Heat pipe end closure is by welding (see Fig. IV- 11). Details of the heat pipe are shown in SIG 110040, Heat Pipe Assembly.

After fabrication, the heat pipes are electroplated by Battelle Columbus Labs with 60 Sn • 40 Pb to facilitate soldering to the housing/radiator extension heat pipe saddles.

Heat pipe development and testing is further discussed in Chapter VII.

8. Auxiliary Cooling System

The auxiliary cooling system consists of water coolant lines soldered to the power section housing primarily to provide a means for heat removal during certain operations. Specifically, the system is used as a heat sink during converter processing at 3M converter outgassing operations at TES, (before the heat pipes and radiator extension are attached), ground handling operations when the generator is inverted, and launch operations while the generator is within the Shuttle bay. Additionally, the auxiliary coolant system can be used as a temperature control unit for the generator during normal operation on the ground.

The system consists of two circumferential lines in parallel-one over each thermoelectric region-plus a common entrance and exit line (see Figs. IV-1 and IV-4). All tubing is 1/4 in. OD Type L deoxidized copper alloy 122. The entrance/exit tubing ends are flared per MS33584 and utilize Parker-Hannifin brass sleeves and nuts for attachment to the external cooling source (e.g., outgassing station, Shuttle heat exchanger).

The circumferential loop assembly, SIG 110018-019, is electroplated at Battelle Columbus Labs. with 60 Sn • 40 Pb solder per TES PS 0300038, and subsequently soldered with the same alloy to the power section housing per SIG 110015, Auxiliary Cooling Tube

Installation Procedure. Subsequently, after the fins on the power section are welded, the flared tube entrance and exit lines are attached to the circumferential loop assembly with Easy-Flo 45 brazing alloy and Handy flux.

Additional manufacturing details (dimensions, cleaning notes, annealing operation, assembly steps) may be found on SIG 110018, Cooling Tubes-Auxiliary Generator.

9. Gas Management System

The Gas Management System (GMS) is the subassembly that permits a change of gas within the RTG at anytime prior to launch. An inert cover gas for the SIG RTG is a requirement during launch to prevent over temperature on the selenide thermoelectrics during the RTG stowed configuration operations. Additionally, a low conductivity inert gas, e.g. xenon, is required to prevent too low a temperature on the iridium heat source PICS during launch, that would result from the presence of fuel-generated helium alone. Finally, the GMS permits evacuation of the RTG either to obtain a direct flight condition power check or to rid the RTG of excess helium because of the iridium low temperature concern.

The GMS is shown in Fig. IV-12. It is intended that this system be used in conjunction with a portable vacuum maintenance unit. The GMS consists simply of a valve, welded plumbing for attachment of the valve to the fueling end cover, and a fitting for joining the valve to the vacuum maintenance unit.

The valve is an all stainless steel bellows Nupro valve, Model SS-4H-TSW, identical to that used and qualified for the Voyager RTG. This valve is cleaned in Freon, baked out in vacuum at elevated temperature and leak checked to $\leq 4 \times 10^{-9}$ scc He/sec-atm. The handle on the valve is custom fabricated from 304 passivated stainless steel also identical to that used for Voyager. Its threads are lubricated with molybdenum disulfide.

FIG. IV-12. GAS MANAGEMENT SYSTEM

The valve is joined to the end cover through all welded connections. Specifically, a 1/4 in. stainless tube is socket welded to the valve body and to a bimetal (304 stainless/6061 aluminum) ring similar to that used for the electrical receptacle attachment. On the downstream side of the valve a male connector body is bevel welded to it. On the extreme end of the connector body a removable blind gland is employed using a replaceable copper gasket. This terminal gland provides a backup seal to the valve seat. This closure can be made while locally flooding this region with argon to minimize oxygen entrapment in the small volume between the valve seat and the gland.

The GMS may also be fitted with an in-line filter to prevent Min-K particles (or other dust) from inhibiting seating of the valve. Testing was planned to examine the need for a filter before including it in the design.

Additional detail on the GMS for the RTG may be found on the following drawings:

SIG 120012	RTG End Cover Assemblies
SIG 110065	Transition Fitting, Bimetal
SIG 110083	Fitting (Male Connector Body and Blind Gland)
SIG 110067	Handle

10. Gas Vent System

The Gas Vent System (GVS) as shown in Fig. IV-13 is designed to vent the internal inert gas from the RTG after the spacecraft is in the interplanetary cruise trajectory. The GVS consists of a two-spring assembly used to drive a lance through a puncture diaphragm mounted in the RTG fueling end cover. The activation is accomplished by release of a cam triggered by the deployment of the booms on which the RTG's are mounted.

During ground operations the selenide thermoelectrics, multifoil insulation, and isotopic heat source must be isolated from the air atmosphere to avoid damage. After

FIG. IV-13. GAS VENT SYSTEM (GVS)

launch ascent, the inert cover gas and fuel-generated helium must be vented to space to achieve design power.

The GVS consists of three assemblies identified and described in the following sections. In general, maximum use was made of MHW-developed technology, primarily on the lance, the puncture diaphragm and its attachment by electron beam welding.

a. Diaphragm/bimetal ring seal assembly

This unit, which is detailed in SIG 120008, Bimetal Ring/Diaphragm Assembly-GVS, is welded to the fueling end cover. It consists of a thin-wall diaphragm through which a lance is driven to release the internal gas and a steel/aluminum bimetal transition ring to permit welded closures to the steel diaphragm and aluminum end cover.

This diaphragm, SIG 120002, is made from 304 stainless steel precision shim stock, 0.005 in. thick. The part is hand formed with a tool to a 0.75 in. spherical radius (concave to the lance), ground to a circular shape, 0.735 in. diameter, and bright annealed at 1950°F. Radiographic and 20X inspections are made for flaws. Maximum diaphragm puncture force is 12.5 lbs. at 350°F and 15 lbs. at room temperature.

The diaphragm on SIG/GM will experience up to 1 atmosphere Δp , externally applied, and up to 1.2 atmospheres Δp , internally applied. This application allows for substantial design margins. For example, on MHW, acceptance tests were routinely performed at 38 psid for 30 minutes at 550°F with an allowable deformation of ≤ 0.002 in.; a qualification test was successfully executed with an internal pressure of 120 psid for 24 hours at 550°F; also a life test was performed at 38 psid for 60 days.

The bimetal seal ring, SIG 120004, is made from Dupont explosive clad plate. This is the same material and technique used elsewhere on the RTG for attachment of the electrical receptacle, outgassing tubes and gas management valve to the aluminum end covers.

The diaphragm is EB welded to the bimetal ring and inspected with dye penetrant, radiographs, and a room temperature helium leak check, $\leq 1 \times 10^{-9}$ scc He/sec-atm. This subassembly is subsequently TIG welded to the end cover.

b. Lance drive assembly

This unit, attached to the bimetal ring seal assembly by threads, contains the lance, provides its energy source (springs) and interfaces with the cam-trigger. It is installed shortly prior to launch. Design details are given in SIG 120005 for the assembly and a few of its parts, in SIG 120003 for the lance, and in SIG 120023 and SIG 120024 for the inner and outer springs, respectively.

The lance is a three-edged member with 8 rms forward surfaces, made from 440 stainless steel and subsequently given a heat treat at 1850°F for 25 minutes followed by a stress relief at 600°F for 3 hours. Hardness is 54 Rockwell, minimum. The lance has a 0.090 in. neck to allow gas flow at full penetration. (The system is designed to yield a Δp of 0.5 micron with a flow of 10^{-4} scc He/sec.) The lance is screwed into a 304 stainless steel piston with a torque of 21 inch-lbs. This piston also retains the compressed springs within the 6061 aluminum body as shown in Fig. IV-13. The piston has two cross holes, one for a ball-lock safety pin and one for pinning a steel collar to it that in turn engages the trigger. The aluminum body is hardcoated in the region where the piston passes through it to eliminate possible galling. Piston/lance travel from the armed position to full release is one-half inch.

Two springs are used to obtain a force about a factor of two over the required ~13 lbs. puncture force. Both are made of Inconel X-750. The outer spring is a right helix, 13 active coils, 0.080 in. wire diameter, 0.845 in. outer diameter, and provides 19.7 lbs. force at compressed length. The inner spring is

a left helix, 20 active coils, 0.045 in. wire diameter, 0.480 in. inner diameter, and provides 6.4 lbs. force at compressed length.

c. Release mechanism assembly

This unit, attached to the RTG fueling end cover by screws, serves as the trigger for activation of the lance drive assembly. The trigger is armed when the RTG and its supporting boom is placed in the stowed position prior to launch. The trigger is activated when the RTG is deployed from its stowed position on the spacecraft.

Details on the interface with the spacecraft were still being worked out with JPL at conclusion of the contract. Therefore this assembly was not designed in detail with production drawings.

C. RTG ASSEMBLY SEQUENCE

A block diagram of the major assembly steps for an RTG is shown in Fig. IV-14. The assembly consists of operations at both the 3M Company and TES with the minor exception that the gas vent system lance assembly is not installed until RTG delivery to Cape Canaveral.

Initial power section assembly operations occur at 3M after the power section housing and center multifoil section are delivered from TES. These operations involve the in-air installation of the two ring thermoelectric modules and the center multifoil section. The partially assembled power section is then installed in an hermetically sealed steel container used for initial heated processing power check and subsequent shipping to TES.

The remaining assembly steps on the power section are performed at TES in environmentally protected facilities capable of maintaining < 50 ppm of water and oxygen. These assembly steps involve the removal of the power section from the 3M container, installation of fibrous and additional multifoil insulation, insertion of the GFE electrical heat source and attachment of end covers and outgassing valves. At this point, the end cover at the radiator end (power output end) is welded to the housing body while the other cover (fueling end) is temporarily sealed with Viton O-rings to allow for outgassing and radiator installation prior to fueling operations, at which time the final cover is welded into place. The power section with its electrical heat source is next installed on the outgassing station. There the unit is heated under vacuum and taken to normal operating conditions for a power check. After removal of the outgassing valves, the power section is riveted to the radiator section, heat pipes are bonded to

FIG. IV-14. RTG ASSRMBLY SEQUENCE

the assembly and the Z-93 emissive coating is applied. The generator is then returned to the fueling facility for radioisotope heat source installation (fueling) and welding of the permanent end cover. RTG testing operations follow.

D. MASS PROPERTIES SUMMARY

The predicted nominal generator weight, with the 3DCC heat source is 102.5 lbs. The POCO heat source increases the weight to 104.5 lbs. These weights are comparable to the specification maximum allowable weight of 104.7 lbs. Weights are based upon all detail dimensions being at their nominal values. The addition of the radiator power cable adds an additional 1.01 lbs. resulting in a total weight of 103.5 lbs. (3DCC heat source) which is comparable to the specification limit of 107.0 lbs. A detail weight breakdown is presented in Table IV-6 .

The nominal center of gravity location is predicted at $Z = -14.87$ inches, as measured from the face of the fueling end mounting lugs.

TABLE IV-6
WEIGHT SUMMARY

	Weight (lbs.)	
	<u>Flight</u>	<u>ETG S/N-1</u>
Improved heat source assembly ⁽¹⁾	40.4 (42.4) ⁽²⁾ (E)	42.4 (E)
Converter assembly ⁽¹⁾	16.0 (E)	16.0 (E)
Insulation system	7.02 (L)	7.02 (L)
Multifoil ⁽³⁾	3.502 (D)	
End insulation	2.17 (L)	
Corner insulation	1.11 (L)	
Fiberfrax stuffings, retaining ring	0.24 (L)	
Housing assembly	22.52 (E)	24.48
Housing shell	11.838 (D)	
Copper strike/solder plate	0.521 (D)	
Fins (bays 3 and 4)	3.255 (D)	
Fueling end cover detail	1.74 (L)	2.20 (E)
Radiator end cover detail	1.52 (L)	2.00 (E)
Emissive coating	0.96 (L)	
Cooling tubes/fittings	0.740 (D)	
Electrical wiring (internal)	0.50 (E)	
Instrumentation and wiring (internal)	-	0.40 (E)
Connector(s)	0.08 (E)	0.40 (E)
Bimetallic rings for connectors	0.19 (L)	0.97 (L)

TABLE IV-6 (Continued)

	Weight (lbs)	
	<u>Flight</u>	<u>ETG S/N-1</u>
Gas management valve system	0.30 (E)	
Generator vent system	0.37 (L)	No vent system
Generator vent system bimetallic ring	0.11 (L)	
RTD's and connector	0.40	
Radiator assembly	11.02 (L)	11.02 (L)
Radiator shell	4.91 (L)	
Copper strike/solder plate	0.33 (L)	
Fins (bays #1 and #2)	4.42 (L)	
Ring frames	0.29 (L)	
Emissive coating	1.07 (L)	
Miscellaneous (solder, crimps, rivets, etc.)	0.20 (E)	0.20 (E)
Heat pipes	5.13 (L)	5.13 (L)
Short	1.33 (L)	
Long	3.80 (L)	
Solder additions	0.22 (L)	0.22 (L)
TOTAL	102.5 (104.5) ⁽²⁾	106.5
Power cable and 2 connectors	1.01 (E)	1.01 (E)

(1) Maximum values only available.

(2) Existing MHW heat source.

(3) Getters included.

Weights Legend:

(D) = Detail drawing.

(L) = Layout (preliminary)

(E) = Estimated

E. ELECTRICALLY HEATED GENERATOR (ETG)

The first full-scale SIG/GM flight generator was to be an electrically heated unit. In general, it simulates the RTG mechanically, electrically, thermally and weight-wise (within 4%). The major differences in design from an RTG are as follows:

1. The heat source is electrically powered. The heat source subassembly was Government Furnished Equipment (GFE) built by General Electric Co. Requirements for this subassembly are specified on an interface drawing, SIG 116002, between TES and GE.
2. The ETG is highly instrumented internally with thermocouples. This feature, along with the electrical input leads, requires three connectors in the fueling end cover and two in the radiator end cover (versus one in the radiator end cover on RTG flight units).
3. The ETG does not incorporate the gas vent system in order to avoid complexity and minimize puncture risk to the ETG.
4. The CBCF/Min-K insulation end pieces on the ETG have considerable additional slotting to accommodate the additional power and instrumentation leads. The Min-K TE 1800 rings at each end are altered enough that an additional aluminum sleeve is required at each end to preload the end multifoil sleeves. These aluminum sleeves nest against the housing inside diameter and are scalloped for weight reduction.
5. The ETG end covers would be of somewhat different design to accommodate the extra required connectors and item (3) above.

Also, all RTG's are initially powered with an electrical heat source for outgassing

and checkout. In this configuration, the GFE electrical heat source and an alternate temporary end cover at the fueling end are required. All fibrous insulation at the same end is subsequently replaced during fueling with the isotopic heat source.

F. REFERENCES

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V. GENERATOR PERFORMANCE/THERMAL ANALYSIS

The previous chapter described the design of the RTG. This chapter describes the operating characteristics of the generator from an electrical/thermal energy viewpoint. Coverage includes a discussion of radiator design considerations, the thermal performance of the heat rejection systems, RTG internal temperature profiles for various operating and design conditions, system performance (power, efficiency, heat flows), and the analytical technique formulated for predicting RTG power with time. Also discussed is the study that led to the selection of multifoil insulation in part of the RTG.

A. HEAT PIPE RADIATOR

1. Configuration Selection

The 10° conical fin design for the SIG/GM RTG as presented in Reference 1 was the result of a series of optimization studies that maximized the generator's power to weight ratio for deep space, deployed operation. Unfortunately, this radiator design had two major disadvantages: 1) JPL found it extremely difficult to integrate the generators with the spacecraft, and 2) heat pipe dryout could occur during certain spacecraft maneuvers (e.g. burns, thrusts, etc.). A 45° cone and a rectangular fin design were rejected for similar reasons. The selection of a cylindrical radiator design was made because it eliminated, or greatly reduced, problems of spacecraft integration and heat pipe operation (see Table V-1) with a relatively small (~2 lb) RTG weight penalty compared to the 10° cone design. In fact, it was estimated by JPL that

TABLE V-1

REASONS FOR CHANGE FROM CONICAL TO CYLINDRICAL RTG RADIATOR CONFIGURATION

- Spacecraft integration
 - Reduces interference of the RTG radiator with spacecraft structure in stowed position.
 - Reduces mechanical complexity of boom required to assure correct heat pipe orientation during the various mission events.
 - Improves accessibility to RTG housing for structural mounting points in the stowed position.
 - Reduces adapter ballast required and percent consumed of asymmetric tank fill capacity.
 - Reduces prelaunch auxiliary cooling system requirements.
 - Small system weight penalty (~ 2 lb)
- Heat pipe fabrication/operation
 - Straight pipes
 - All heat pipes function during transport, ground operation, and mission maneuvers.

TES-33009-46

V-2.

the overall system weight would be lower with the cylindrical radiator.

The choice of five rectangular fins in the circumferential direction was the result of a weight optimization study, and a staggered fin arrangement was found to be preferable to continuous fins in the Z-direction because of the reduction in circumferential gradients in the housing. The use of 20 heat pipes, 10 covering both the power section and radiator, and 10 covering only the power section, was found to be the most weight effective approach, subject to limits on heat pipe heat transport capabilities. The unequal axial fin lengths on the radiator resulted from an effort to equalize the heat transport ($Q L_{\text{eff}}$) of all heat pipes feeding the radiator.

2. Design Inputs

The design of the heat pipe radiator is based upon a number of assumptions that preceded any thermal optimization. A list of these ground rules is given in Table V-2, and a discussion of some of them follows.

The 140-150°C⁽¹⁾ limit on the heat pipe solder temperature indirectly limits the cold junction temperature to a maximum of 165-175°C, since the ΔT from the cold junction to the solder is ~25°C. Also, holding the cold junction above 135°C at EOM (as specified by 3M) requires a BOL cold junction of at least 150°C. A nominal BOL cold junction temperature of 160°C was used throughout the analysis.

Because of the above mentioned limits on cold junction temperature, a tolerance of $\pm 7^\circ\text{C}$ on the nominal value was specified. A variation in the temperature of the housing around the module of $\pm 7^\circ\text{C}$ or less would show that this goal was satisfied, since housing temperatures can be assumed to closely follow cold junction temperatures.

(1) A more precise maximum design temperature for solder will have to be determined by strength tests.

The use of an effective space sink temperature instead of direct solar heat flux assumes that all sides of the generator receive an equal solar heat input. For BOL temperature predictions, this is a valid assumption because early in the mission the spinning of the spacecraft distributes the incident solar flux over the entire generator surface. Under EOM conditions, the effects of non-uniform solar heat input are minimal, since total solar flux is small. A maximum to minimum variation in housing temperature has been estimated to be about 4°C at EOM (ref. 2). Maximum and minimum mission temperatures for key components (solder, heat pipes, cold junction, etc.) are obtained by assuming maximum solar input at BOL and minimum solar input at EOM.

3. Heat Pipe Characteristics

The heat pipes selected for use on the SIG/GM RTG use water as the working fluid and are made from copper tubing with a 5/16 inch OD and internal axial grooves. There are 10 short pipes (~15") on the power section only and 10 long pipes (~45") on both the power section and the radiator section. All pipes are straight, parallel to the Z-axis of the generator, and equally spaced around the circumference of the generator.

In designing the heat pipe radiator, the heat transport limit ($QL_{\max}$) of the pipes had to be considered. The design value for $QL_{\max}$ was arrived at by calculating a heat transport capability which included the following conservatisms:

- (1) an adverse elevation (evaporator above condenser) of 0.05 inches was used instead of 0-g conditions,
- (2) the worst case (lowest) heat pipe mission temperature of 105°C was used, and

- (3) a safety factor of 0.9 was applied to the predicted $QL_{\max}$ to arrive at the design value.

Design limits on $QL_{\max}$ are given in Table V-2. Another conservatism in the radiator design is that $QL_{\max}$ cannot be exceeded even if three pipes fail. It is assumed, however, that no pipe failures occur until after the first several days of the mission when the generators are spinning at 3-5 rpm and $QL_{\max}$ increases by a factor of two because of the spin assist.

The orientation of the heat pipes and generator with respect to the spacecraft is such that the centrifugal force caused by the spinning of the spacecraft provides an assist to the capillary forces that drive the fluid from the condenser end to the evaporator end of the heat pipes. However, this causes an excess of fluid to collect in the end of the heat pipes (beyond the evaporator regions) causing the heat pipes in those regions to be inactive. This effect is known as heat pipe blockage - when the fluid is restrained from cyclically returning to the evaporator. The 10 short pipes are assumed to be blocked up to the edge of the first evaporator (module #1), and the 10 long pipes are assumed to be blocked halfway into the first evaporator.

The accumulation of noncondensable gases at the condenser end of each pipe creates another inactive heat pipe segment. The inactive condenser length, measured from the tip of the pinch-off tube, was specified as 1.5 in. for the long pipes, and 1.0 in. for the short pipes.

The value for h_{HP} (2466 BTU/hr ft²°F) was an experimentally derived result. It determines the evaporator to condenser ΔT since the fluid ΔT is small.

TABLE V-2
Heat Pipe Radiator Thermal Design Inputs

● Temperature/Thermal Inputs

EOM Cold Junction temperature (T_c)

- T_c (EOM) $> 135^\circ\text{C} \rightarrow T_c$ (BOL) $> 150^\circ\text{C}$
- Long term operation with radiator temperature above $140\text{--}150^\circ\text{C}$ is forbidden because of solder limitations
- Circumferential variations in T_c must be minimized ($< 15^\circ\text{C}$)
- P (BOL) = 230 w(e); Q_{inv} (BOL) = 2460 w(t)
 $\rightarrow Q_{rej}$ (BOL) = 2230 w(t)
- Sink temperature methodology with maximum solar input is used for BOL temperatures. ($T_{sink} = 390^\circ\text{R}$)
- Cold end hardware thermal resistance = 7°C/W
- Cold frame - housing $\Delta T = 5^\circ\text{C}$

● Heat Pipe Inputs

- Heat transport (QL_{eff}) must be less than QL_{max} , where:

$$QL_{max} = \left. \begin{array}{l} 3500 \text{ w-in. with no spin assist (BOL)} \\ 7000 \text{ w-in. at 3 RPM} \\ 12600 \text{ w-in. at 5 RPM} \end{array} \right\} \text{ (EOM)}$$

- The wall to fluid convective heat transfer coefficient, h_{HP} , is 2466 BTU/hr ft² °F
- Inactive heat pipe lengths (in.) are as follows:

	Evaporator	Condenser
Long pipes	5.7	1.5
Short pipes	1.5	1.0

TABLE V-2 (Continued)
Heat Pipe Radiator Thermal Design Inputs

Manufacturing/Material Inputs

The power section housing radiator and fins are all made of Al 6061 because of its good conductivity ($k = 100 \text{ BTU/hr-ft-}^\circ\text{F}$), weldability, and ease of handling and machining.

The fin tip thickness cannot be less than 0.020 in. for feasibility of machining.

Emissive coating is Z-93 with

$$\epsilon \text{ (Z-93)} = 0.95$$

$$\alpha \text{ (Z-93)} = 0.2$$

Envelope Inputs

Diameter 24"

Length 60"

4. Ambient Air Operation

A calculation of housing and cold junction temperatures for the SIG operating in an air environment was performed to determine whether auxiliary cooling (or heating) would be required to maintain the solder and cold junction temperatures within their respective limits (Ref. 3). Keeping a solder temperature below 140-150°C and the cold junction above 135°C is roughly equivalent to maintaining the housing temperature around the modules in the 110°C to 145°C range. Fig. V-1 shows the maximum and minimum housing temperatures around the modules, where gradients due to heat pipe blockage and circumferential gradients have been included. Fig. V-1 predicts that the 145°C limit will be reached in still air at a temperature of ~ 102°F, and the 110°C limit will be reached in still air at a temperature of ~ 78°F. Thus, auxiliary cooling would be required in still air over ~ 100°F, and heating (or insulation blankets on the fins) would be required in still air under ~ 80°F.

5. Results

The parameter studies described herein were performed to determine (1) the best way to trade off weight vs. length by varying fin root thickness and/or radiator skin thickness, and (2) the sensitivity of weight and either fin root thickness or length to changes in the value of ϵ (Z-93). From Figs. V-2 and V-3, which depict the results of these parametric studies, it can be concluded that:

- 1) The best way to reduce the weight of the generator is to:
 - a. Minimize radiator skin thickness, and then
 - b. Reduce fin root thickness (and increase radiator length)
- 2) The best way to reduce the radiator length is by increasing fin root thickness, not radiator skin thickness.

- 3) A decrease of 0.01 in the value of ϵ (Z-93) can increase weight by as much as 1.3 lb (if root thickness is raised by 13 mils) or as little as 0.4 lb (if radiator length is increased by 0.5 in.).
- 4) A precise, accurate value for ϵ (Z-93) is mandatory for an optimum radiator design.

It can be seen from the temperature map presented in Fig. V-4 that the housing temperature around the module vary from a low of 122°C to a high of 136°C. thus satisfying the $\pm 7^\circ\text{C}$ tolerance on the nominal cold junction.

Fig. V-5 presents relevant thermal data regarding the heat pipes at BOL. It should be noted that there is a safety margin of 530 w-in. between the maximum QL_{eff} (=2970 w-in.) and QL_{max} (=3500 w-in.) at BOL. At EOM, the maximum QL_{eff} is 2970 w-in., but the value of QL_{max} is 7000 w-in., due to the spin assist. Finally a detailed study showing the thermal effects of two or three adjacent heat pipe failures would be required to accurately predict the effects of such an event.

FIG. V-1. SIG TEMPERATURES IN STILL AIR

FIG. V-2. GENERATOR WEIGHT AND LENGTH AS FUNCTIONS OF FIN ROOT AND RADIATOR SKIN THICKNESSES

NOTE:

- $Q_{\text{reject}} = 2230 \text{ w}$
- COLD JUNCTION = 160°C
- 20 HEAT PIPES - 10 LONG, 10 SHORT
 - SHORT PIPES BLOCKED UP TO THE EDGE OF MODULE #1
 - LONG PIPES BLOCKED MID-WAY INTO MODULE #1
- RADIATOR SKIN THICKNESS = 0.025 IN.
- 7 IN. FIN HEIGHT (ROOT TO TIP)

FIG. V-3. GENERATOR WEIGHT, LENGTH, AND FIN ROOT THICKNESS OF Z-93 COATING EMISSIVITY

NOTES:

- 1/5 SYMMETRY ASSUMED
- $T_{\text{SINK}} = 390^{\circ}\text{R}$ (1.0 α . μ .)
- 50" TOTAL LENGTH
- 0.110" FIN ROOT THICKNESS
- ϵ (Z-93) = 0.95 (AEROJET VALVE)
- 0.025" RADIATOR SKIN

- SHORT PIPES BLOCKED UP TO THE EDGE OF MODULE #1
- LONG PIPES BLOCKED MIDWAY INTO MODULE #1
- COLD JUNCTION TEMPERATURE = 160°C
- $Q_{\text{REJECT}} = 2230$ WATTS
- 7 IN. FIN HEIGHT
- HEAT PIPE i FEEDS FIN i , WHERE $i = 1-4$

FIG. V-4. POWER SECTION/RADIATOR TEMPERATURES ($^{\circ}\text{C}$) DEPLOYED, BOL

HEAT PIPE NO. (= FIN BAY NO.)	MAX. EVAP. TEMP (°C) WALL/FLUID	MIN. COND. TEMP.(°C) WALL/FLUID	$Q_{TR}^{(1)}$ (W)	$L_{EFF}^{(2)}$ (IN.)	Q_{LEFF}	MAX. BLOCKED WALL TEMP. (°C)
1	127/122	119/121	105	27.6	2894	137
3	133/130	128/130	49	4.8	237	133
2	122/117	115/116	131	22.7	2970	135
4	133/130	129/130	58	3.9	224	132

NOTE:

QL REQUIRED BY H. P.
ACCEPTANCE SPECIFICATION
IS:

3500 W-IN FOR LONG PIPES
1000 W-IN FOR SHORT PIPES

AT 105°C (MINIMUM HEAT PIPE TEMPERATURE DURING MISSION)

(1) Q_{TR} = HEAT INTO HEAT PIPE EVAPORATORS

$$(2) Q_{LEFF} = \int_{X=0}^{X=L} Q(x) dx$$

$$\text{IN GENERAL } L_{EFF} = \frac{L_{evap} + L_{cond}}{2} + L_{adiabatic}$$

FIG. V-5. BOL HEAT PIPE DATA

B. AUXILIARY COOLING LOOP

1. Design Requirements

The auxiliary cooling system design is dictated by the requirement that the heat pipe solder temperature should not exceed a certain limit to maintain adequate strength during the anticipated dynamic environments. The solder temperature upper limit is estimated to be in the range of 140 - 160°C. Also, the housing is permitted to undergo only a few thermal cycles which lower its temperature below 110°C (corresponding to a cold junction temperature of 135°C), to prevent a phase transition in the P-leg.

When the rate of heat rejection from the SIG external surfaces is not sufficient to maintain the heat pipe solder temperature below its upper limit, the auxiliary cooling system must remove the excess heat. On the other hand, if the housing temperature at the modules falls below 110°C due to a high rate of heat rejection from the external surfaces (e.g., due to too much air conditioning in the shuttle cargo bay), the housing temperature may have to be heated to 110°C by circulating hot coolant through the coolant lines⁽¹⁾.

The coolant selected is water. A line pressure is provided by the shuttle coolant system that is high enough to prohibit vaporization of the water. The required line pressure is well below the bursting pressure of the tubes utilized (1/4" OD x 0.025" wall).

(1) An alternate method proposed is to use a shield around the SIG to keep the housing temperature above 110°C.

The cooling tube layout chosen ensures that the flow rates in the loop around both modules are nearly equal. The coolant flow rate and inlet temperature are selected such that the housing temperature is maintained within the above mentioned limits.

2. Parametric Studies

Temperature distribution in the housing section around the modules is influenced by the following factors.

- Quantity of heat removed by the coolant
- Coolant flow rate and inlet temperature
- Whether the heat pipes are working or not

The housing temperatures are also dependent on such design factors as the size and location of the cooling tube saddles and thickness of the housing section at the modules.

A parametric study was made to determine the relative influence of the coolant flow rate and inlet temperature on the housing temperatures. The maximum and minimum housing temperatures were calculated using the following simplifying assumptions:

- (1) The total heat load is spread uniformly over the two 3 inch wide bands of housing section around the thermoelectric modules.
- (2) Radiative losses from the housing surface and heat conducted to the housing section off the modules do not affect the housing temperature at the modules.
- (3) The effective thickness of the thermoelectric module cold frame is 0.050" (i.e., the resistance for heat conduction through the frame is calculated neglecting the presence of slots).

Assumption (2) does not significantly affect the temperature distribution in the housing.

A detailed thermal model was planned to estimate the extent to which assumptions (1)

and (3) can affect the housing temperature gradients.

Two sets of conditions were considered which normally represent extreme cases with respect to heat loads.

- (1) 90% of the total heat generated is carried away by the cooling water while the heat pipes are not functioning
- (2) 20% of the total heat generated is carried away by the cooling water while the heat pipes are functioning

When the heat pipes are not functioning, the heat transferred to a housing panel between an adjacent pair of heat pipes is conducted in the axial direction to the cooling tube saddles. While the heat pipes are functioning, heat transfer from housing to water is enhanced by the heat pipes. Hence, for set (2) the heat transferred to the housing panel was assumed to flow first to the heat pipes, and hence to the cooling tube saddles without further significant temperature drop.

Temperature distribution in the housing panel was obtained by solving a differential equation that expresses a heat balance at an arbitrary plane parallel to the panel boundary with a uniform temperature. For set (1), the housing sections at the module edge and at the cooling tube saddle were taken as the uniform temperature boundaries. For set (2), the housing sections at the heat pipes were used as the constant temperature boundaries. The maximum panel temperature derived in this manner is the following:

$$T_{\max} = T_o + \frac{q l^2}{2 k t}$$

where,

T_o = cooling tube saddle base temperature ($^{\circ}\text{F}$)

q = heat flux into the panel ($\text{BTU}/\text{in}^2\text{-hr}$)

k = thermal conductivity of the panel ($8.3 \text{ BTU}/\text{inch-hr-}^{\circ}\text{F}$)

l = conduction length *

= 1.29" for set 1

= 0.44" for set 2

t = combined effective thickness of housing and the module cold frame (0.10")

The SIG design initially under consideration during the utilized 25 heat pipes.

Hence, the temperature drop across the cooling tube saddles were calculated assuming 25 cooling tube saddles, each having dimensions 0.32" (width) x 0.5" (length) x 0.45" (height). The present design has only 20 cooling loop saddles and, hence, the temperature drop across the saddles would be slightly higher than those calculated initially.

The temperature drop between the water and cooling tube wall was computed using the following expressions for heat transfer coefficients (Ref. 4). For laminar flow, Reynolds number $(\text{Re}) \leq 2100$,

$$\text{Nu}_d = \frac{h_d d}{k} = 3.65 \text{ (constant wall temperature)} \quad (\text{V-2})$$

For transition flow $(2100 < \text{Re} < 10,000)$,

$$\text{Nu}_d = 0.116 (\text{Re}^{2/3} - 125) \text{Pr}^{1/3} \left(\frac{\mu}{\mu_w} \right)^{0.14} \left(1 + \left(\frac{d}{L} \right)^{2/3} \right) \quad (\text{V-3})$$

* Corresponds to an earlier SIG design with 25 heat pipes

For fully developed turbulent flow ($Re \geq 10,000$),

$$Nu_d = 0.023 Re^{0.8} Pr^{0.4} \quad (\text{Dittus-Boelter equation}) \quad (V-4)$$

where:

Nu_d = Nusselt number based on tube ID

$$= \frac{h d}{k}$$

h = heat transfer coefficient (BTU/inch²-hr-°F)

d = tube ID (0.2 inch)

k = thermal conductivity of water (BTU/inch-hr-°F)

Re = Reynolds number

$$= \frac{V d}{\nu}$$

V = water velocity (inch/sec)

ν = kinematic viscosity of water (inch²/sec)

Pr = Prandtl number

$$= \frac{C \mu}{k}$$

μ = dynamic viscosity of water at the bulk fluid temperature (lbm/inch-hr)

c = specific heat of water (BTU/lbm-°F)

μ_w = dynamic viscosity of water at the tube wall temperature (lbm/inch-hr)

L = extended length of tube loops (inches)

The minimum housing temperature occurs at the base of a cooling tube saddle near the water inlet. The peak housing temperature occurs at the thermoelectric module edge in the vicinity of the water outlet. Circumferential heat conduction tends to reduce the housing temperature gradients in the vicinity of the water inlet and

outlet and perhaps even decreases the housing temperature spread. However, the maximum and minimum housing temperatures were conservatively estimated by neglecting circumferential heat conduction.

The results are summarized in Figs. V-6 and V-7 where the housing temperature is plotted against cooling water velocity within each loop for several inlet water temperatures (43, 66 and 100°C). The bracketed temperature bands indicate the spatial range of temperature due to longitudinal and circumferential temperature variations. Based on these results, the minimum coolant flow rate required was estimated to be 200 lbm/hr. When the water has to remove 20% to 90% of the total heat generated, the inlet temperature should be in the range of 66°C to 110°C. If under certain conditions it becomes necessary to raise the housing temperature to 110°C by circulating hot water, the inlet temperature will have to be above 130°C. A pressure drop of 1.5 psi was computed for the TES portion of the auxiliary cooling system for a total flow rate of 200 lbm/hr.

The results shown in Figs. V-6 and V-7 are for a generator with 25 heat pipes. The present design has only 20 heat pipes. * The housing temperature spread for the present design is expected to be about 5°C larger than those shown in Figs. V-6 and V-7.

* The S/N 1 generator has 19 saddles. One of the 20 spaces between the heat pipes is void of a saddle. A detailed analysis was planned to estimate temperature gradients in the housing panel without a saddle.

FIG. V-6. HOUSING TEMPERATURE AT THERMOELECTRIC MODULES
VERSUS FLOW VELOCITY - AUXILIARY COOLING

FIG.V-7. HOUSING TEMPERATURE AT THERMOELECTRIC MODULES
 VERSUS FLOW VELOCITY AUXILIARY COOLING WATERS

C. POWER SECTION PERFORMANCE

1. Design Inputs

As described in the following section (Temperature Profiles), a multi node thermal model was constructed to evaluate SIG internal temperatures, heat losses and power output. A summary of ground rules upon which this model is based is found in Table V-3.

TABLE V-3
SIG Thermal Model Ground Rules

- MHW heat source, thermal inventory -2460 w(t)
- Thermoelectric converter
 - ring design
 - $T_{\text{hot junction}} = 860^{\circ}\text{C}$ (BOL)
 - $T_{\text{cold junction}} = 160^{\circ}\text{C}$ (BOL)
 - T/E Extraneous Resistance = 12 1/2%
 - Cold end hardware resistance = $7^{\circ}\text{C}/\text{watt}$
 - $D_n/D_p = .315/.260$ (in)
 - Fiberfrax insulation
 - $\eta_{\text{T/E}} = 11.14\%$
- Converter - housing $\Delta T = 5^{\circ}\text{C}$
- Insulation
 - multifoil (60 layer Mo/ZrO_2) on sides
 - MinK TE 1400/CBCF-3 on ends (1.5" thick); interface temperature to be $\leq 810^{\circ}\text{C}$
 - MinK TE 1800 annular ring end corner; temperature limit = 850°C

2. Temperature Profiles

Temperature profiles of the SIG are computed using the Thermal Analyzer Program (TAP-3) which is described in Ref. 5. Details of these studies may be obtained from Refs. 6 and 7.

The temperature responses of 5 versions of the SIG design are presented in Table V-4. As noted, they represent the SIG under operational (evacuated) conditions with the original Multihundred Watt (MHW) heat source (case 1) and with the improved MHW (case 2). The primary changes in case 2 are changes in materials and design of the heat source to improve its probability of not releasing fuel during conveyable accidents. (The improved version of the MHW has officially replaced the original MHW design, subsequent to completion of these thermal analyses).

Case 3 predicted the temperature response of the SIG with an electrical heat source which is used to verify the performance of the generator prior to loading the nuclear fuel. Slight differences in temperature between this case and the two previous ones are due to a redistribution of the heat load.

Cases 4 and 5 SIG temperature profiles apply to helium and xenon gas fills, respectively. These cases represent the extremes of temperatures that can be realized within the SIG prior to piercing the housing at the time the SIGs are deployed. Preliminary studies show a helium fill will cause the iridium Post Impact Containment Shells (PICS) to be at $\approx 830^{\circ}\text{C}$ at which temperature the iridium is believed to be too brittle to survive impact. For this reason a mixture of helium and xenon will probably be required as a cover gas. These cases also have lower housing temperature than the previous 3 cases which assumes the generator will be convectively cooled in air during most of the time the SIG has a gas fill.

A more complete description of the case 2 temperature profile is shown in Fig. V-8. Also shown is the nodal break-up of the thermal model. The heat load is applied to the interior surfaces of the model while the housing temperatures are fixed at predetermined levels. The electrical power is removed from the cold junctions of the thermoelectrics and the remaining heat is rejected to space via the cylinder/heat pipe/fin radiator.

3. System Performance

Table V-5 summarizes system performance for the flight RTG and ETG S/N-1 designs. Both sets of data are based upon optimum thermoelectric couple geometry. In late January, 1979, 3M made a prediction of 188 watts(e) for S/N 1 output power. This was based upon the following changes and assumptions.

- P-leg hot junction temperature = 815°C
- N-leg - PbTe and orthorhombic GdSe_x segmented design
- 0.2" diameter

The P-leg hot junction temperature is reduced to 815°C through use of a gimbal and this reduces P-leg sublimation. The purpose of a segmented N-leg is to achieve a higher power output than would otherwise be attained. Finally, the N-leg cracking problem should be relieved by stabilizing the N-leg in the orthorhombic state.

A total nominal power loss of 29 w(e) from BOL to EOM is anticipated (see Ch. IV), 17 w(e) due to fuel decay and 12 w(e) due to system degradation. This reduces the original flight design nominal power output shown in Table V-5 from 230 to 201 w(e) at EOM. This prediction provides a good margin over the requirement of 185 w(e) EOM minimum shown in Table IV-1.

TABLE V-4Summary of SIG Steady State Temperatures

Case	1 ⁽²⁾	2	3	4	5
Heat Source	original MHW	improved MHW	electrical	improved MHW	improved MHW
Gas Fill	vacuum	vacuum	vacuum	helium	xenon
Thermal Inventory per Generator	2460	2460	2460	2460	2460
T (Hot Junction)-°C	860	860	860	483	771
T (Cold Junction)-°C	160	160	160	119	126
T (Hsg-side)-°C	124-131	124-131	124-131	101-103	101-103
T (Hsg-end)-°C	119	119	119	101	101
Max. Temp. of Ins. -°C					
CBCF -3	1040	1003	992	593	903
Min-K 1400	807	801	803	472	745
Min-K 1800	835	836	796	444	702
Multifoil	998	985	975	458	871
Thermoelectric eff. (%)	11.14	11.14	11.14	8.68	11.14
Thermal Eff. (%)	84.7	84.4	85.5	44.3	78.2
Power (w(e)), gross	237.5 ⁽²⁾	231.3	234.3	~ 95.0	~ 214.3
Power (w(e)), net ⁽¹⁾	236.3 ⁽²⁾	230.1	233.1	~ 93.8	~ 213.1

(1) Power loss of 1.2 w(e) for wiring and connector pins subtracted

(2) Results for Case 1 are not up-to-date with results for the other cases. If they were updated, the power output (net for Case 1 would be approximately 228 W(e).

TABLE V-5
BOL Nominal Performance

Heat Losses, W(t)	Flight	ETG S/N-1
● Ends CBCF-3Min-K 1400	45.4	45.4
● Corners (Min-K 1800)	58.7	58.7
● Multifoil	90.2	90.2
● Fiberfrax Stuffings	12.7	12.7
● Module (Fiberfrax & P-Leg Wrap)	<u>177.7</u>	<u>173.7</u>
Total	384.7	380.7
Total Inventory, W(t)	2460	2497
Module Heat Input, W(t)	2253.0	2290.0
Efficiencies, % ⁽¹⁾		
● Module	10.26	8.83
● T/E	11.14	9.40
● Thermal	84.4	84.8
● Overall	9.4	8.0
Power Output, W(e)	230 ⁽²⁾ ⁽³⁾	199 ⁽³⁾
Extraneous Resistance, %	12.5	55
Load Voltage, Volts	30	30

(1) Based on $T_h T_c = 860/160^\circ\text{C}$

(2) Improved heat source; existing heat source yields 228 W(e)

(3) Power at connector; includes 1.2 watt loss for wiring and connector pins

D. MULTIFOIL INSULATION OPTIMIZATION STUDY

The objective of this study is to evaluate the performance and the weight of the SIG with multifoil insulation. Two generator configurations are considered:

- Generator with multifoil insulation in the sides and fibrous insulation in the ends.
- Generator with multifoil insulation all around the heat source (except in the T/E modules).

Two types of multifoil insulation are evaluated.

- TECO type of insulation, which consists of moly foils or moly/titanium composite foils coated with fine zirconia particles having a density of 0.06 mg/cm^2 .
- Linde type of multifoil which consists of moly foils separated by Astroquartz or zirconia cloth spacers.

The effects on the generator performance and weight of the total number of foils, the foil spacing, the edge losses, and the hot side temperature are determined parametrically.

The following major assumptions are used in the analysis:

- Axial heat conduction through the multifoil insulation can be neglected.
- Fiberfrax paper is used for the T/E module insulation.
- The hot and cold junction temperatures are 860°C and 160°C , respectively.

Support frame temperature	=	910°C
Extraneous resistance	=	12.5%
Thermoelectric efficiency	=	11.14%

- With TECO type of multifoil blankets, the thicknesses of the moly and the titanium foils are 0.0003" and 0.0005", respectively.
- The moly-titanium interface temperature is 500°C

- The average foil spacing for the Linde type multifoil is 0.01".
- The total thickness of CBCF-3/Min-K 1400 composite end insulation is 2" and the interface temperature is 810°C,
- The thickness of the Min-K 1800 ring in the corner is equal to the thickness of the radial gap between the cold side support sleeve and the housing.

Heat losses through the insulation are calculated using the TINCYL code, which utilizes one dimensional heat transfer equation. The heat losses through the TECO type multifoil insulation are calculated with the following equation obtained from TECO:

$$\phi = \frac{7.42 \times 10^{-13} \left(T_h^4 - T_c^4 \right)}{0.778N + (1.11 \times 10^{-2}) N^2} \quad (\text{See Footnote 1}) \quad (\text{V-5})$$

where

$$\begin{aligned} \phi &= \text{heat flux perpendicular to foils} \left(\frac{\text{watts}}{\text{cm}^2} \right) \\ T_h &= \text{hot temperature (}^\circ\text{K)} \\ T_c &= \text{cold temperature (}^\circ\text{K)} \\ N &= \text{total number of foils} \end{aligned}$$

Equation V-5 is taken to be valid for all moly foil packets as well as Mo-Ti foil packets.

Based on the predictions of a detailed thermal model, the nominal hot end temperature of the multifoil packet is taken equal to 1050°C. The end insulation hot end temperature is set equal to 1100°C. The cold end temperature of each multifoil stack is calculated by equating the heat flux through the multifoil stack to that through the gap between the multifoil cold end support sleeve and the housing.

(1) Heat fluxes calculated by this equation should be increased by 1.43 to be consistent with test data that has since been obtained.

For the moly-titanium composite system the number of moly and titanium foils which yield an interface temperature of 500°C are calculated as follows:

- It is assumed that for a given set of hot and cold junction temperatures, the foil temperature as a function of the fractional foil number is independent of the total number of foils in the stack.
- The foil temperature as a function of fractional foil number is deduced from the experimentally measured temperature distribution data in Figure VIII-3 of Ref. 8.

The heat losses through the CBCF-4/Min-K 1800 rings are calculated by assuming that their hot side temperature is equal to the average temperature of the CBCF-8 slab. The losses through the corner formed by the intersection of the Min-K 1800 ring and Min-K 1400 slab are calculated using the shape factor for an infinite corner adjoining two semi-infinite planes.

The losses through the multifoil insulation in the ends are calculated in the same manner as for the foil blankets in the sides. The losses through the end support structures are calculated by using one dimensional heat flow equations for composite structures in series. Based on the TINCYL runs for the fibrous end insulation case, the end cover temperature was taken to 95°C.

As a result of this study, the following conclusions have been reached.

- Multifoil insulation is recommended for sides only
- A 60 layer all moly foil blanket of TECO type with a foil spacing of 0.005" is recommended. A SIG generator with this foil blanket on its sides produces about 12.3 watts additional power when compared to the generator with fibrous insulation (CBCF-4/Min-K 1800).
- With insulation blankets having more than 60 foils, the generator power is relatively insensitive to the total number of foils. For an increase from 60 to 120 foils, the power increases from 228.8 watts to 231.2 watts.
- TECO multifoil insulation is preferred. A 60 foil Linde system is about 2 lbs heavier than a 60 foil TECO system.

- An all moly foil blanket is recommended. The weight advantage for a Mo/Ti composite system (0 to 0.3 lbs) is small. The Mo/Ti system would require additional testing.
- A generator with a 60 all moly foil blanket (TECO type) on the sides is about 0.54 lbs lighter than that with fibrous insulation.
- Power is relatively insensitive to the hot side foil temperature.
- Heat losses through the foil edges can amount to 100% to 200% of the losses through the foil blanket.
- Power and weight are relatively insensitive to the foil spacing in the range of 0.003" to 0.005".
- For a 60 all moly foil blanket, the weight penalty in using 0.4 mil thick foils instead of 0.3 mil thick foils is about 0.65 lbs. The 0.3 mil system is recommended.
- Fibrous insulation is recommended for the ends. A generator with multifoil insulation and GE type end closures in the ends weighs about 3 lbs more than the generator with fibrous insulation.
- The uncertainties in the multifoil cold end temperatures due to uncertainties in the housing and the support sleeve emissivities does not significantly affect the heat losses through the multifoil.

E: POWER PREDICTION TECHNIQUE

A power prediction technique was prepared for predicting SIG RTG mean EOM power and its distribution (Ref. 9). Using data generated during 3M's module power degradation demonstration test program, 3M plans to predict the variations of heat to the thermoelectric module (Q_{module}) and mean T/E module output power degradation ($\gamma P(t)/P_o$) and their distribution with mission time. Since the hot junction (HJ) and cold end hardware (CEH) temperatures are affected by the degradation of RTG components other than the T/E modules, 3M will consider the HJ and CEH frame base temperatures parametrically. TES will initially perform analyses to predict the variation with mission time of Q_{module} due to degradation of all RTG components except the T/E modules. TES will consider the HJ and CEH frame base temperatures parametrically. Finally, TES will iterate between TES' and 3M's predictions of total module heat flow history and determine, for the total system, the variations of total module heat flow, and the HJ and CEH frame base temperatures as functions of mission time. Using these HJ and CEH frame base histories and 3M's parametric prediction of γ , the mean EOM power can be predicted.

F. REFERENCES

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VI. MISSION INTEGRATION STUDIES

Three significant areas of integration were considered in the design of the SIG/GLL system: the thermal impact of the spacecraft on the RTG heat rejection system prior to generator deployment, micrometeoroid impact on the RTG housing and heat pipes, and magnetic fields and their influence on the RTG circuit design. These items are discussed in the following sections.

A. STOWED THERMAL ANALYSIS

As shown in Table II-1, after the IUS/spacecraft is deployed from the Shuttle Orbiter cargo bay, the SIGs will be in the stowed position, for about 4 hours. During this period, the SIGs may receive direct solar radiation, earth IR and reflected solar energy from a number of surfaces including the earth, the Shuttle Orbiter and the spacecraft. Also, the proximity of the spacecraft/IUS substantially decreases the SIGs view of space. Hence SIG temperatures while stowed may exceed their normal space operation temperatures. The heat pipe solder temperature should not exceed a certain limit in order to maintain adequate strength. The solder temperature limit is not yet clearly defined, but is estimated to be in the range of 140-160°C.

An analysis was initiated to estimate the maximum heat pipe solder temperature at different times while the SIG is stowed. Hot junction temperature is not of concern because of the presence of a cover gas inside the RTG until activation of the Gas Vent System at the time of RTG deployment. Another object of this analysis is to estimate the housing and the cold junction temperatures when there is no solar or

earth IR heat input on the SIG due to the spacecraft/IUS occlusion. The cold junction temperature should not fall below 135°C in order to prevent a phase transition in the P-leg material.

The total heat input on the SIG depends on the spacecraft/IUS orientation relative to the earth and sun, and the orbit attitude. During the initial phase of this analysis, the orbit geometry and the spacecraft orientations during the launch phase were not clearly defined. Consequently, the analysis assumed orientations that yield maximum external heat input. Later JPL supplied a preliminary description of the orbital trajectories and the spacecraft orientations at the different points in the orbits.

1. Spacecraft Orientation and Orbit Altitude

Fig. VI-1 shows the approximate geometry for the pre-injection orbits recommended by JPL. The spacecraft orientation at different times in the orbit and other assumptions recommended by JPL are the following:

- (1) After the cargo bay doors are opened, the Orbiter is oriented such that the Orbiter always shadows the spacecraft from direct sunlight and from earth IR to the extent possible at each point in the orbit.
- (2) The IUS deployment occurs in the shadow of the earth at 100 n. miles altitude. After deployment, the IUS altitude control motors cannot be activated until the distance between the Orbiter and the IUS is at least 200 feet. Hence the IUS has an unspecified orientation for a period of approximately 15 minutes beginning with the deployment. In order to consider the worst case, the IUS orientation during the above period is taken to be such that the SIG is in a broadside attitude relative to the earth.

VIEW NORMAL TO ORBIT PLANE

VIEW PARALLEL TO ECLIPTIC

FIG. VI-1. APPROXIMATE GEOMETRY FOR PRE - INJECTION ORBITS

- (3) During the coast orbits, the IUS orientation is continuously adjusted to keep the total heat load on the spacecraft and the SIG to a minimum. The external heat input is minimum when the spacecraft +Z- axis is oriented towards the sun and the spacecraft +Y-axis towards the earth.
- (4) All IUS motor burns, including the reorientation burns, occur in the earth's shadow. The maximum external heat load on the spacecraft and SIGs during this period is equal to the maximum earth IR.
- (5) Until completion of the IUS second stage motor burn, the IUS/spacecraft spin rate is zero.
- (6) Prior to the IUS third stage ignition, the spacecraft is spun up to 50-70 RPM. After the third stage burn, the spacecraft spin-rate is decreased linearly to 1-2 RPM over a 20 minute period and then the SIGs are deployed. The spacecraft spin-down is assumed to occur in the sun with the solar vector normal to the spacecraft Z-axis. The earth inputs are assumed to be negligible.

The spacecraft/SIG orientations chosen for analysis are illustrated in Fig. VI-2. Cases 1-3 respectively show the orientations, consistent with the above assumptions, in the earth's shadow, at the subsolar point¹ and at the terminator.² Case 9 illustrates the spacecraft Z-axis orientation during the spin-down. In addition to the orientation implied by the above assumptions, several other orientations, cases 4-8, which yield larger external heat loads are included to study the SIG response in the event the spacecraft is not maintained in the preferred orientation.

¹That point on the earth at which the sun is vertically overhead.

²The line separating the sun-illuminated and dark portions of the earth.

FIG. VI-2. ORIENTATION CONSIDERED IN THE STOWED SIG THERMAL ANALYSIS

In calculating the earth IR heat load, the effective black body temperature of the earth is taken as 256°K. The earth albedo is taken as 0.41. The solar constant is taken to be 430BTU/ft.²-hr.

2. Thermal Model

The stowed SIG temperatures are affected by the spacecraft temperatures which in turn are driven by the SIG temperatures. Hence, the stowed SIG temperatures were iteratively calculated as follows. JPL developed a combined spacecraft and SIG thermal model in which the SIG is represented by a simple model. TES developed a combined thermal model in which the SIG is represented by a simple model. TES developed a combined thermal model in which the SIG is represented by a detailed model and the spacecraft by constant temperature boundary nodes. The spacecraft temperatures predicted by the JPL model are utilized in the TES model and the SIG temperatures are calculated. These SIG temperatures are fed into the JPL model and an improved estimate is made of spacecraft temperatures, to complete one step of the iteration. This iteration between the two thermal models is continued until the SIG temperatures predicted by both models are nearly equal.

During the initial phase of the analysis, JPL developed a 12 node model for the spacecraft and SIG. The model considered only the spacecraft section directly behind the SIG. The spacecraft was represented by one-quarter of a cylinder divided into 9 nodes of 10 degrees each. The length of the model was assumed equal to the SIG length. The SIG was represented by one node. The spacecraft temperatures were estimated by fixing the SIG node temperature at 140°C. To estimate the SIG temperatures, a combined spacecraft and SIG model having 27 nodes was developed by TES. The SIG was represented by 17 nodes. The spacecraft was treated as

one-half of a cylinder with a length equal to the sum of the spacecraft and IUS adaptor cone lengths. The spacecraft was divided into 9 nodes in the circumferential direction. The JPL spacecraft temperature estimates were assumed valid over the entire length of the spacecraft. The SIG temperatures in the TES model were calculated by treating the nodes representing the spacecraft as constant temperature nodes. The calculated SIG temperatures were found to exceed the upper limit allowed on the heat pipe solder temperature. However, the spacecraft thermal model and the simplifying assumptions utilized in the analysis were such that the calculated SIG temperatures were substantially overestimated. Hence, it was decided to develop a more detailed thermal model.

Later JPL developed an expanded spacecraft and SIG thermal model with 54 nodes. The model considers the entire length of the spacecraft and SIG. The SIG is treated as a cylinder with a diameter equal to the SIG outer envelope diameter. The cylinder representing the SIG is divided into 22 nodes; however, conduction between the nodes is not considered. The heat generation rate in the cylinder is set equal to that necessary to hold the cylinder surface at 115°C , the average SIG temperature predicted by the TES thermal model when there is no external heat input. The spacecraft outer surface is assumed to be black with an emissivity of 0.9. The IUS adapter cone surface is assumed to be white with a solar absorptivity of 0.2 and an IR emissivity of 0.85.

The combined spacecraft and SIG thermal model used by TES for estimating the SIG temperatures is shown in Fig. VI-3. The spacecraft nodal network is the same as in the JPL model. The cylinder representing the SIG in the JPL model is replaced by a more realistic model that includes the SIG design details. Heat

NOTES:

- (1) THE INCLUDED ANGLE BETWEEN THE LINE JOINING THE SPACECRAFT AND SIG CENTERS (IN THE END VIEW ALONG +Z - AXIS) + AND THE +X - AXIS IS 157°
- (2) NODE NUMBERS 18, 19, 20, 27 AND 28 NOT ASSIGNED.
NODES 32 AND 33 REPRESENT THE READY ANTENNA

FIG.VI-3. DETAILED TES SPACECRAFT/SIG THERMAL MODEL

rejection from the SIG external surfaces is calculated considering the geometry of the housing, fins and radiator. Heat transfer between the SIG nodes is calculated by considering the presence of the heat pipes. The view factors between the spacecraft nodes and the SIG nodes calculated for the JPL model were used after making adjustments to account for differences between the two SIG models.

For each spacecraft/IUS orientation considered, JPL provided TES the following information:

- spacecraft and IUS adapter cone temperatures
- SIG temperatures
- solar and earth IR heat inputs on the SIG nodes, including the radiation reflected by the spacecraft surface.

The SIG nodal heat input data provided by JPL were adjusted to account for differences in the SIG nodal layout between the JPL and TES thermal models.

The SIG steady state temperatures were then calculated by TES using the Thermal Analyzer Program (TAP-3) code.

The analysis assumes that the SIGs instantly reach their equilibrium temperatures at each point in the orbit. However, due to their thermal inertia, the SIGs may not reach a steady state at every point in the orbit. Hence the worst case housing temperature predicted here is conservative.

3. Results

Results for the runs completed are shown in Table VI-1. Both TES and JPL models calculate temperatures that are averaged over a 20 degree section of the generator. However a comparison of normal space operation temperatures predicted by the TES model of Fdg. VI-3 with those of a detailed radiator thermal model indicated

TABLE VI-1
STOWED SIG THERMAL ANALYSIS RESULTS

Case No.	Spacecraft/SIG Orientation (relative to the earth)	Altitude (nautical miles)	Maximum SIG Temperature (°C) (1)	
			JPL Model (Assumes cylindrical SIG)	TES Model (Uses JPL S/C Temperatures)
1	Broadside, shadow	100	140	143
2	End on, subsolar	350 (350) ⁽³⁾	TBD (144) ⁽³⁾	TBD -
3	Broadside, Terminator	220 (150)	TBD (139)	TBD -
4	Broadside, Subsolar, No Solar Input	350	TBD	TBD
5	Broadside, Subsolar, Maximum Direct Solar Input	350	160	164
6	End On, Terminator, Maximum Direct Solar Input	220	160	161
7	Broadside, Subsolar, Maximum External Heat Load	350	TBD	TBD
8	Broadside, Terminator, Maximum External Heat Load	220	TBD	TBD
9	Broadside to solar vector; spinning at 70-1 RPM	-	TBD	TBD
10	No external input	-	115 ⁽²⁾	115 ⁽²⁾

(1) 5°C added to account for temperature variation in a 20 degree section of the SIG.

(2) Minimum housing temperature at the thermoelectric modules.

(3) The effective black body of earth (297°K) used is above the nominal value assumed for other cases (256°K).

that the temperature averaged over a 20 degree housing section is about 5°C below the peak temperature in that section. Hence, the maximum SIG temperatures in Table VI-1 have been adjusted to 5°C above those predicted by the Fig. VI-3 thermal model.

The steady state temperatures predicted by the JPL and TES models are within about 10°C . The spacecraft temperatures and the SIG external heat load predicted by the JPL model are not substantially affected by a 10°C change in the SIG temperatures. Hence, no iterations were made between the two thermal models.

The minimum housing temperature at the modules predicted by the TES model for the zero external heat load case is 115°C . Thus, during the pre-injection orbits, the cold junction temperature will be above 135°C .

Results for Case 1 in Table VI-1 indicate that the peak SIG temperature in the earth's shadow is about 143°C . Thus if the uncontrolled IUS coast and the IUS motor burns occur in the earth's shadow, the peak heat pipe solder temperature would be within its allowable limits.

The external heat loads on the spacecraft and SIG during the 100×3493 n. miles coast orbit would be less than or equal to those during the 100×350 n. miles orbit. Hence, herein only those altitudes relevant to 100×350 n. miles orbit are considered.

Cases 2 and 3 respectively represent conditions when the spacecraft is in the preferred orientation at the subsolar point and the terminator. The JPL SIG temperature estimate shown in parenthesis for case 2 is obtained using an effective black body temperature of earth (297°K) that is above the nominal value assumed here (256°K). The result in parenthesis for case 3 is obtained using an altitude (150 n. miles) that is less than the altitude at the terminator (220 n. miles) during

the 100 x 350 n. miles orbit. However, the total external heat inputs in the above two cases are higher than they would be if the right altitude and earth temperature are used. Thus, it is concluded that if the spacecraft +Z-axis is oriented towards the sun, the peak heat pipe solder temperature at the subsolar point and terminator should be below 140°C.

The maximum external heat load due to the earth IR and reflected solar radiation, when the spacecraft's +Z-axis is oriented towards the sun, may occur at a point in the orbit between the terminator and the subsolar point. Case 4 considers an orientation with maximum combined earth IR and reflected solar inputs at 350 n. miles altitude. The worst case earth IR and reflected solar inputs for altitudes in the range of 220 (terminator) to 350 (subsolar) n. miles do not exceed those for case 4 by more than 10%. Hence the maximum housing temperature predicted for case 4 should be close to the worst case housing temperatures for points between the terminator and the subsolar point.

The results for cases 5 and 6 (SIG broadside to sun) show that if the spacecraft and SIG receive maximum direct solar radiation, the peak SIG temperature exceeds 160°C. Hence, when the spacecraft is in the sunlight, its +Z-axis should be oriented toward the sun.

During the spacecraft spin-down after the IUS third stage burn, the spacecraft +Z-axis will be normal to the solar vector (case 9). Since the spacecraft and SIGs during this period receive direct solar radiation only intermittently, the total input will not be as large as for cases 5 and 6. However, during spinning the acceleration forces cause the working fluid in the heat pipes to build up in the evaporator section and thereby increase the evaporator temperature. Test data show that while the SIG

is spinning at 70 RPM in the stowed configuration, the temperature of the heat pipe first evaporator section is 7 - 10°C above its normal operation temperature. Because of the heat pipe blockage, the effectiveness of the fin sections over the blocked heat pipe section also decreases which causes a further increase in the temperature of that section. Further analysis is needed to estimate the SIG temperatures during the spin-down.

The launch sequence shown in Table II-1 indicates that the SIG cooling lines are severed 20 minutes prior to IUS/spacecraft release from the shuttle. There is concern that the external heat load on the spacecraft and SIGs may be sufficiently large to cause the heat pipe solder temperature to exceed its upper limit. Also, if the uncontrolled IUS coast occur in the sun side of the earth, the SIGs may overtemperature. The stowed SIG temperatures while the IUS spacecraft is on the sun side of the earth can be significantly reduced if the spacecraft exterior surface is changed from black to white.

In summary, while the spacecraft/SIG is in the earth's shadow, and while at the subsolar point and the terminator with the spacecraft +Z - axis oriented towards the sun, the peak housing temperature will be below 143°C. If the SIG receives maximum direct solar radiation, the peak heat pipe solder temperature will be about 160°C. Further analysis is needed to estimate the peak solder temperature prior to the IUS deployment (after the cooling lines are severed), at a point in the orbit between the terminator and the subsolar point, and during the spacecraft spin-down. Due to their thermal inertia, the SIGs may not reach a steady state at each point in the orbit, as assumed here. Hence, the worst case housing temperature predicted here is conservative. A transient analysis must be made to estimate the SIG temperatures more accurately.

B. MICROMETEOROID ANALYSIS

Micrometeoroid penetration and consequent damage to key SIG components during the Galileo mission is a significant concern. Analyses were performed on two key components - the radiator heat pipes and the power section housing.

The study of the micrometeoroid penetration of the heat pipes (Ref. 1) evaluates the probability that no more than three heat pipes will be lost to the system via micrometeoroid penetration over the 50000 hour flight mission. The SIG radiator is sized to accommodate the failure of three heat pipes; however, more analysis is required to determine whether the failure of two or three adjacent heat pipes is permissible. A mitigating effect is that failure would likely occur while the spacecraft is spinning which would at least double the heat pipes transport capability, providing a large margin. Results of the analysis show that a 15 mil exposed heat pipe wall thickness yields four-nines reliability (at 50% confidence) that no more than three heat pipes will be lost. By proper orientation of heat pipes in their saddles, such an exposed wall thickness is achievable.

Micrometeoroid penetration of the RTG housing was also evaluated. The expected result is no penetrations. The probabilities of one or more penetrations in thin wall sections adjacent to the T/E's and other thin wall sections are 0.02 and 0.09, respectively. Penetration prior to nominal RTG cover gas release is extremely unlikely. Subsequent to cover gas release, penetration of the RTG housing may have no effect on RTG performance, if the only effect is puncture of the housing. Further study is required to evaluate the probability of damage to the thermoelectric circuit and/or other internals critical to producing power.

C. MAGNETIC FIELD ANALYSIS

The magnetic field generated by the SIG electric circuit needs to be calculated for comparison with the JPL requirement whose purpose is to minimize interference with sensitive Galileo mission scientific instruments.

An analysis was performed using the MAGFIELD computer code for the detailed electrical circuit of 2190 line segments. Fig. VI-4 is a representation of the overall electrical circuit including power leads between the two module rings.

Fig. VI-5 summarizes the computed values of magnetic field intensity. Results are qualified as "vacuum" values to indicate omission of any contribution from magnetic induction. An analysis of the effects of permeable materials (stainless steel connectors, bi-metal transition rings, etc.) needs to be performed; it is felt, however, that these effects will be small due to the small amount of these materials and symmetry considerations.

Table VI-2 summarizes results of the analysis and compares them with the JPL Galileo requirements. Both dipole and quadrupole requirements are met with an uncompensated design. As indicated by note 2 in Table VI-2, careful attention is given to the design of the power lead routing to minimize magnetic effects. Axial power leads are spaced such that dipole moments are self cancelling as shown in Fig. VI-6. The power lead service loop in the end of the generator is designed with twists or with one loop placed on top of the other (in the Z-direction).

FIG.VI-4. SIG FLIGHT CIRCUIT

FIG. VI-5. MAGNETIC FIELD FROM SELF-COMPENSATED
SIG FLIGHT SYSTEM

TABLE VI-2
MAGNETIC FIELD AT ONE METER FROM SIG RTG FOLLOWING DEMAGNETIZATION FROM
40 GAUSS

<u>Moment</u>	<u>Spec (Gamma)</u>	<u>Calculated (Gamma)⁽¹⁾</u>
Dipole (Radial)	10	~ 2.0 ⁽²⁾
Quadrupole & Higher	40	15 (90°) 29 (0, 180°)

(1) Quadrupole values are given at indicated angle from RTG axis.

(2) Assumes full cancellation of radial dipole moment due to axial power lead routing from modules to end.

Assumes final loop of $\geq 270^\circ$ with one loop on top of the other (in the Z direction). Alternately, an even number of twists with any length loop is acceptable.

$$m \cong m^1$$

FIG. VI-6. SIG END CONNECTOR MAGNETIC CIRCUIT

D. REFERENCES

1. "A Study of the Micrometeoroid Penetration of Heat Pipes of the SIG Radioisotope Thermoelectric Generator for the Galileo Mission," TES-2865-25, November, 1978.

VII. COMPONENT DEVELOPMENT

The component development efforts for the SIG/GLL program were primarily a continuation of the tasks initiated on the prior SIG development program. These efforts include:

- Performance testing of the GDS-1 generator.
- Heat pipe development and testing.
- Thermal insulation characterization.
- Weld and bimetal transition development.
- Thermoelectric module-to-housing ΔT tests.
- Emissive coating characterization.
- Electrical receptacle testing.
- Heat source support vibration tests.
- Heat pipe and cooling tube solder bond development.

A discussion of the above tasks is presented in the following sections. Where detail topical reports are available for a particular area, only a brief summary is presented with reference to the appropriate report for a more comprehensive discussion.

A. GROUND DEMONSTRATION SYSTEM (GDS-1)

1. Background

The GDS-1 was the first attempt to design, fabricate and assemble a large-scale thermoelectric generator employing the 3M Company selenide thermoelectric materials technology. The generator was designed and built under the SIG development program

and was tested under the subsequent SIG/Galileo flight program. Table VII-1 summarizes the generator design features. Figure VII-1 shows a cutaway of the GDS (note that the GDS-1 heat pipe fin was not installed on the GDS, but is being tested separately).

The selenide thermoelectric converter ring was assembled into the TES-supplied housing by the 3M Company and subsequently outgassed and power checked at 3M prior to shipment to TES for the final assembly operations. These final operations consisted of installation of: non-module thermal insulation, the isolation hot frame, the electrical heater, miscellaneous thermocouple instrumentation, welded end covers containing receptacles for the instrumentation and power output wiring, and outgassing valves. These operations were performed at TES in a controlled inert gas environment. Figure VII-2 shows the generator housing with the thermoelectric converter ring installed as received from 3M. Figure VII-3 depicts the GDS-1 after completion of assembly at TES prior to outgassing.

Subsequent to final assembly in the inert gas environment, the GDS-1 was installed on the outgassing station for secondary outgassing of internal generator components and initial performance testing. The GDS-1 is shown installed on the outgassing station in Fig. VII-4.

2. Test Results

The original plan for GDS-1 called for outgassing and then seat-in of the thermoelectric module after which performance characteristics of the generator would be determined. This was to be followed by a life test and finally a random vibration test. However, during the seat-in operation it was observed that the normalized power output peaked at 86.7 watts on July 26, 1978 and then began to decrease uniformly at about 0.2 watts/day. This rate of degradation continued for about 25 days; then the decrease

TABLE VII-1
GDS-1 DESIGN SUMMARY

Output power objective W(e)	108 at 13.5 volts
Number of thermoelectric couples	156
D_n/D_p , inch	0.315/0.260
Hot junction temperature, °C	850
Cold junction temperature, °C	150
Housing/Radiator temperature, °C	125
Gas fill	Xe/He mixture
Extraneous thermoelectric module resistance, %	25
Spring pressure, psi, N/P	300/150
Element length, inch	0.3
Circuit	2 strings, series/parallel
P-leg	Unwrapped, one partition
Cold end hardware	Sliding follower
Heat source	Electrical

1. ELECTRICAL HEATER ELEMENT.	13. I. H. F. BELLOWS.
2. ELECTRICAL HEATER BODY.	14. I. H. F. BI-METAL SEAL RING.
3. INSULATION RETAINER BARRIER SLEEVE.	15. I. H. F. TRANSITION RING.
4. SIDE INSULATION RINGS	16. HOUSING SHELL.
5. END INSULATION HIGH TEMP.	17. MOUNTING LUGS.
6. END INSULATION LOW TEMP.	18. AUXILIARY COOLING SYSTEM.
7. END COVER WITH HEATER POWER INPUT CONNECTOR	19. HEAT PIPE.
8. OUTGASSING PORT.	20. FIN SUPPORT GUSSET
9. END COVER WITH POWER OUTPUT & INSTRUMENTATION CONNECTORS.	21. FIN STRUCTURE.
10. T/E MODULE.	22. INSTRUMENTATION CONNECTOR
11. T/E MODULE SUPPORT RING.	23. POWER OUTPUT CONNECTOR
12. ISOLATION HOT FRAME (I. H. F.) LINER.	

FIG.VII-1. SIG GROUND DEMONSTRATION SYSTEM

FIG. VII-2. INSTALLED GDS-1 CONVERTER RING

FIG. VII-3. GDS-1 PREPARED FOR OUTGASSING

FIG. VII-4. GDS-1 INSTALLED ON OUTGASSING STATION

became greater and at the same time more erratic. Finally on September 12, 1978 the generator open circuited. By manipulating the electrical load conditions, the generator could be brought back on load for intermittent periods. On September 18, 1978 the load voltage was adjusted to provide an output current of 0.5 ampere in order to eliminate the thermal cycles resulting when the generator went from near-matched load to open circuit. Figure VII-5 summarizes the output power, open circuit voltage and internal resistance profiles during the period of time while the generator was at near-matched load and operating at nominal hot and cold junction temperatures of 850°C and 150°C, respectively.

The most reasonable explanation for the observed behavior of the generator at this time is believed to be the hangup of the followers in the cold end hardware, especially those of the P-legs where it appears that the hot end element-to-WRe cap resistance is pressure-sensitive. Hangup of this type could cause large contact resistances between the elements and the shoes. Why the P-legs should be affected more than the N-legs is not yet understood, but it could be related to a high rate of sublimation of selenium from the leg and to deformation of the leg due to creep.

Instrumentation on four N and P-legs confirms the general predictions of 3M that the N-leg performance is affected by the hot side gadolinium foil causing the resistance and Seebeck voltage to decrease, and the P-leg resistance increases due to follower hang-up. These trends were observed during the early degradation when the output power was decreasing smoothly. Figures VII-6 and VII-7 present the resistance and average Seebeck voltage of the instrumented N and P-legs, respectively, during this period of time. The trends of Figs. VII-6 and VII-7 agree well with the observed changes in output power of the generator.

FIG.VII-5. GDS PERFORMANCE SUMMARY AT 850°C/150°C

FIG.VII-6. AVERAGE SEEBECK AND RESISTANCE OF P-LEGS IN GDS

FIG.VII-7. AVERAGE SEEBECK AND RESISTANCE OF N-LEGS IN GDS

These observations led to the decision to diagnostically disassemble GDS in order to determine the cause of the degradation. A plan was formulated and reviewed both internally and externally by members of DOE, FI, JPL and Sandia. Based on the work plan and the reviewers comments, two procedures were prepared. The first procedure detailed cooldown of the generator to room temperature and the second procedure described the method of disassembly.

3. Cooldown and Disassembly

On September 27, 1978 cooldown of GDS-1 began. Before this time the generator was operating at 850/150°C hot and cold junction temperature at a load current of 0.5 ampere. Gas samples were taken using a gas sampling manifold.

The samples indicated that the only significant constituents of the gas are helium and xenon. No measurable amounts of oxygen, nitrogen, water vapor and carbon dioxide were present in the samples. The generator was cooled down by reducing heater power and the circulating bath which flowed water through the external cooling loop on the housing. Resistance of the thermoelectric module and the insulation resistance was monitored continuously. The insulation resistance (thermoelectric circuit-to-housing) remained low (< 1 ohm) and the thermoelectric circuit resistance fluctuated from 1.6 ohms to several thousand ohms. The low resistance to the housing was present throughout the test and was noted during initial heatup.

On October 12, 1978 the generator was placed on the jig bore machine to remove the weld on the upper (heater) cover to initiate disassembly. Resistance of the thermoelectric module and insulation resistance were monitored continuously during all operations. The module resistance was about 2-3 ohms during most of the disassembly and increased to 11-12 ohms after removal of the isolation hot frame (IHF). The insulation

resistance remained less than 10 ohms until removal of the IHF which electrically is connected to the generator housing. At that time the resistance of the thermoelectric circuit to the graphite hot frame increased to 50-100 K ohms. The resistance to the housing remained at about 10 ohms. These observations indicate that the low insulation resistance is at the module cold end. The housing was partially machined up to the converter ring in preparation for removal of the modules. Module removal, subsequent testing of the module and division of the module, will be carried out at 3M.

4. Conclusions

The basic reason for disassembly of GDS-1 was to determine the cause for the rapid degradation of the output power. Unfortunately, at this time, it is not possible to relate the observations to direct causes for the degradation. However, some observations can be made:

- The outgassing and gas management techniques were shown to be adequate to maintain clean conditions within the generator.
- The non-modular components within the generator, including the receptacles on the housing, were not affected by the thermal environment during operation of GDS-1.
- A significant amount of sublimation of the P-legs has occurred during the relatively short life of 2000+ hours as shown by the bullet nosing of the legs and deposits on the cold end hardware.
- All exposed N-legs display cracks and/or chips.
- A great deal of misalignment of both N and P-legs was seen both visually and with radiographs.

Table VII-2 lists other observations made during disassembly.

TABLE VII-2

OBSERVATIONS ON EXPOSED LEGS OF GDS MODULE

Misalignment with hot/cold shoes

Legs tilted away from the radius

Deposits on cold straps and followers

Orange ring around N-legs (one ring appeared white)

Cracks/chips in N-legs (longitudinal and transverse)

Copper extrusion at cold end of P-legs

Bridging around P-leg partition

Deposits on edges of BeO discs

Dark crystalline deposits on hot end of P-leg

Bullet nosing of hot end of both P-leg segments

Mushrooming of P-leg just below partition

Although no definite conclusions can be made at this time concerning the cause for the rapid degradation of performance, several of the observed conditions within the module listed in Table VII-2 could possibly contribute to that fact. They are:

- Cracks in N-legs (increased resistance).
- Deposits on edges of BeO disc (shorting of thermoelectric circuit).
- Bullet nosing of P-legs (increased resistance).

It remains to be shown if any of these effects in conjunction with the follower hangup described earlier contributed to the poor performance of GDS-1 or if another effect as yet unknown is the important factor.

Reference (1) is the final TES disassembly report for GDS-1 containing more detailed data and pictures.

B. HEAT PIPE DEVELOPMENT

1. Background

Production quantities of copper/water heat pipes were fabricated in 1972 to evaluate their performance for application on the radiator of an enlarged SNAP 19 type generator. These pipes consisted of 1/4-inch diameter alloy 122 phosphorous de-oxidized copper tubes, bent to an 'L' shape to thermally connect the cylindrical generator housing to the radiator fins.

The inner wall surface of the heat pipe tubes were threaded with about 32 circumferential grooves per cm, approximately 0.127 mm deep. These grooves provided the flow paths for water, selected as the working fluid for the designed operating temperature range of 60° - 175°C (140°F - 347°F). The wick of the pipe was a 100-30-100 mesh electrolytic (99.9% pure) copper screen held against the inner tube wall with a beryllium-copper alloy 25 spring. Both the internal tube surface and the wick were chemically treated with an Ebonol-C coating to facilitate proper wetting by the working fluid.

In November 1972 TES subcontracted the fabrication of 56 heat pipes for the "High Performance Generator" (HPG) Program which utilized then current heat pipe fabrication technology. Forty-eight (48) were soldered to a prototype HPG generator and the remaining pipes were subjected to life testing. After 8,700 hours of life testing, one test pipe failed due to a perforation in its evaporator section in the vicinity of the solder attachment to the test fixture. Another test pipe, developed a similar failure after 14,200 hours of life testing. Twelve (12) of the 48 pipes on the prototype HPG no longer met performance requirements after 6000 operational hours at the Jet Propulsion Laboratory (JPL) where this generator was on life test. Initial failure

analysis indicated that corrosion within the heat pipes caused the perforation failures and requirements for new cleanliness and internal coating procedures would have to be established.

2. SIG/Galileo Heat Pipe Development

In January 1976, TES reinstituted heat pipe development efforts for the SIG/Galileo Generator Program. The objectives in this copper/water heat pipe development program were a long-term (at least 7 year life), high reliability, and high temperature (up to 225°C, 437°F) capability.

A total of 146 copper/water heat pipes were manufactured during the SIG/Galileo program. Of these, 60 units were of the sintered wick type (manufactured by Hughes Co., California). The remaining 86 units were manufactured by B&K Engineering Co., Baltimore, Md. with axial grooves replacing the wick. Copper tubing, alloy CDA-101 per ASTM B-170 requirements, which was cold forged to produce an optimized axially - grooved configuration, was fabricated into heat pipes in accordance with procedures specified by TES.

Fabrication procedures specified that the tubing first be cut into required lengths, identified, and then degreased in trichloroethane. Next, the interior surfaces of each heat pipe tube were cleaned by passing a bore brush in and out of the tube followed by an abrasive treatment with a slurry mixture of aluminum oxide. Then, each tube was submersed in a sodium cyanide solution, followed by a water rinse and subsequent alcohol rinse. End fittings were fabricated, chemically cleaned in sodium cyanide, water and alcohol rinsed and welded to each end of the tubes. Prior to bend forming, each tube was followed by a hydrostatic pressure test at 555 psia to qualify each unit for pressure containment during the solder attachment to the generator and radiator.

The tube subassemblies were then installed into a cleaning and coating station where a solution of sodium cyanide followed by a water rinse was passed through the bore of the units to clean the interior surfaces. Subsequently, the subassemblies were internally coated with cupric oxide by a sodium hydroxide/hydrogen peroxide solution followed by water rinsing and drying with an inert gas. After removal from the coating station, the hole in the evaporator-end fitting of each tube was TIG seal welded. Each tube assembly was then bent to the required configuration (90° bend) followed by a helium leak test to verify a leak rate of $\leq 10^{-8}$ std cc He/sec at an approximate gas pressure of 400 psia. Finally, each tube subassembly was attached to a charging station, evacuated to $\leq 10^{-6}$ torr, charged with the calculated volume of water (water electrical resistivity of ≥ 5 kilo-ohm-cm), pinched off at the condenser end fitting, then TIG seal welded. The water charge of each heat pipe tube was then verified gravimetrically.

3. Heat Pipe Fabrication Facilities

Heat pipes for the SIG/Galileo Program were fabricated for TES by two sub-contractors. Sixty units of the sintered wick type were fabricated by Hughes Co., California and 86 units were fabricated by B&K Engineering Co., Baltimore, Md. In April 1976, Battelle Columbus Laboratories, Columbus, Ohio, were subcontracted by TES to develop an optimum joining technique for attaching copper/water heat pipes to 6061-T6 aluminum alloy radiator cooling fins and generator housing to meet with the SIG/Galileo design requirements.

TES specifications required that any joint between the copper heat pipes and the aluminum structure of the generator housing or the radiator cooling fins must not exceed a 2°C temperature differential across the bond at a fin root temperature of 125°C (257°F). In addition, the resultant joint had to be capable of withstanding a minimum shear stress of 1500 psi. This value was based on a structural analysis conducted by TES. These development activities are detailed in TES topical reports, Refs. 2 and 3.

4. Research and Development Activities

As a result of the failures experienced with wicked copper/water heat pipes during the LES 8 and 9 backup study and development programs, TES initiated a research and development program to derive data and remedial solutions for: (1) the cause of pitting failure inside the Ebonol-C coated heat pipes, (2) the potential and nature of any electrochemical reaction that could occur between the various heat pipe components, (3) the requirement for the long-term stability of the coating on the wicking surfaces, and (4) future avoidance of similar failures.

Results from this study concluded that: (a) hydronium ion concentrations resulting in pHs of ≤ 3 in combination with high concentrations of chlorides (≥ 355 ppm) must be eliminated from the intervals of the heat pipes as these substances together cause the corrosion of copper, (b) beryllium-copper springs must be eliminated to preclude galvanic effects since considerable quantities of hydrogen were found in one of the HPG test pipes), (c) the Ebonol-C coating technology must be eliminated because it was the source of chloride-ion which resulted in the failure of the HPG heat pipes, and (d) reduction of the oxygen levels in the charge water to a level of 0.1 - 10 ppm should reduce any possibility of oxygen-induced corrosion to negligible values.

In order to pursue the requirements for a suitable inner surface coating, TES investigated different surface treatments on copper ranging from simple abrasion to sophisticated electrochemically applied (CuO) coatings. Surface treatment which appeared to be adequate from a wettability standpoint (contact angle $\leq 30^\circ$) were investigated electrochemically to determine their stability under operational conditions. Another study was conducted to determine the effect of organics on the wetting angle of the working fluid (water) and tolerable levels of oxygen/pH to avoid internal heat pipe corrosion.

Only hydrogen peroxide/sodium hydroxide solutions were found to produce the desired coating, satisfied the contact angle requirements, and left no detrimental components and/or residues, therefore led to its optimization by Battelle Columbus Laboratories (BCL). Electrochemical studies conducted by BCL on $\text{H}_2\text{O}_2/\text{NaOH}$ -treated copper surfaces revealed that the CuO coating may be reduced to substoichiometric levels (Cu_xO_y) but this effect should not degrade heat pipe thermal performance.

Another phase of the research and development activity which was also conducted by BCL was to select and screen candidate materials for the joining of copper/water heat pipes to a 6061-T6 aluminum structure. This latter effort included the optimization of suitable processes which included the selection of the solder, solder flux, electroplating copper on aluminum, electroplating solder on copper and engineering procedures to fabricate SIG/Galileo hardware. The foregoing activities are detailed in TES Topical Reports, Refs. 2 and 3.

C. HEAT PIPE TESTING

Axially-grooved copper/water heat pipes have been tested in an accelerated life test program to determine pipe performance and reliability as a function of time (0-7000 hours) and temperature (125 - 225°C). The tests were conducted at Teledyne Energy Systems. Principal measurements were concerned with the accumulation of non-condensable gases within the pipes and with heat transport/transfer capability. A report which details the test setup and hardware, describes the individual tests and presents the results, is listed in Ref. 4.

D. THERMAL INSULATIONS

This subject is treated in detail in the referenced topical report (Ref. 5).

The overall thermal insulation study was broken into four major areas:

(1) experimental thermal conductivity determinations, (2) response to static and dynamic loads, (3) compatibility with other systems components, and (4) fabrication and assembly techniques (including effects of outgassing). All of the most promising fibrous and multi-layer type insulations were evaluated extensively in each of these areas.

From the results of these tests Min-K T/E 1400 and CBCF-3 were chosen as load bearing insulation materials for use in the ends of the generator and Min-K T/E 1800 and molybdenum foil coated with ZrO_2 particles were selected for the side insulations (non-load bearing). Fiberfrax 660 Hi Fi paper was chosen by 3M for module insulation, and was used at the multifoil edges as a compliant radiation seal.

Analysis of the experimental results indicates that these insulations will meet all thermal and load bearing requirements imposed by mission parameters.

E. BIMETAL WELD TRANSITION JOINT DEVELOPMENT

An explosive clad weld transition joint material, 6061-T651/0.035" Silver/304L, has been successfully demonstrated for SIG flight applications requiring hermetic fusion weld attachments between aluminum alloy and stainless steel components. Transition joints are machined from explosive clad 20" x 40" plate stock procured from E.I. duPont in either of the following thicknesses:

	Total Thickness (in.)	Component Thickness - inch		
		6061-T651	Silver Interlayer	304L
Type I	2.13 - 2.28	1.1 - 1.25	0.035	1.0
Type II	3.9 - 4.0	1.9 - 2.0	0.035	2.0

Manufacturing and inspection procedures have been established for the production of the SIG receptacle bimetal weld transition joint, as shown in Figure VII-8. Weld procedures for attachment of the transition joint to the stainless steel receptacle shell (automatic TIG seal type weld) and to an aluminum alloy boss (manual TIG using 4043 filler) were developed and documented. The electrical receptacle installation employed in the GDS assembly, as illustrated in Figure VII-9, represents the first generator application of this technology.

Life testing of receptacle/bimetal weld transition joint/simulated aluminum alloy cover boss assemblies (cyclic - 2 cycles 475°F to 100°F plus isothermal aging at 400°F) have successfully demonstrated the potential reliability of this connector attachment system for the SIG application - receptacle temperature approximately 250°F. Tests of three "dummy stainless steel" and three Deutsch (Viking vintage) receptacle assemblies were 100% successful (i.e. leak rate typically $\leq 1 \times 10^{-9}$ scc He/sec-atm) through 8100 hours thermal aging at 400°F.

TELEDYNE ENERGY SYSTEMS

LCP 10014

BIMETAL RECEPTACLE RING

INCHES

FIG. VII-8. BIMETAL WELD TRANSITION JOINT FOR ELECTRICAL RECEPTACLE ATTACHMENT

FIG. VII-9 APPLICATION OF BIMETAL WELD TRANSITION JOINT FOR
DEUTSH ELECTRICAL RECEPTACLE INSTALLATION IN GDS
COVER

The following three additional applications of this bimetal weld transition joint technology to the SIG flight generator were at various stages of development at the time of issuance of the "stop work order:"

- a. Stainless steel evaluation tube/6061 Aluminum cover
- b. Stainless steel gas management tube/6061 Aluminum cover
- c. Stainless steel gas vent system GVS/6061 Aluminum cover

Also, manufacture of additional Deutsch receptacle/bimetal weld transition joint reliability test hardware was terminated.

A more detailed summary of this facet of SIG technology is presented in a topical report. (Ref. 6).

F. MODULE COLD FRAME -TO-HOUSING ΔT TESTS

1. Background

In early 1977 the 3M Company indicated that they were unable to assemble the modules on integral ribs inside the generator housing. They also had experienced trouble with follower hangups on the cold ring ribs. They felt that they had solved this problem with a nickel coating on 2024 Al. Since they had no experience with plating 6061-T6 Al, they were reluctant to switch to it at this stage of the program. This insistence on the use of 2024 Al for the cold ring assembly precluded welding the ring to the housing. TES therefore initiated a program to measure the ΔT penalty associated with the non-integral cold ring.

A thorough review of the literature was undertaken in the hope of finding a good analytical model which could be used without requiring a large scale test effort.

Thermal conductance through interfaces is by means of very small contact areas of normal solid conductance. The number and size of these contact areas are determined by the microscopic asperities of the surface (RMS finish) superimposed on macroscopic asperities (waviness), as is indicated in Fig. VII-10. Thus, the actual contact surface area is much smaller than the apparent interface surface. Better contact and therefore heat transfer is obtained with increasing pressure, which causes plastic deformation of some of the microscopic asperities, thus increasing their actual area of contact. This deformation also allows a closer approach of the two surfaces resulting in more contacts between other microasperities. In gas filled systems some additional heat transfer naturally occurs through conduction and convection in the gas contained in the gaps.

Several reasonably good mathematical models have been developed for solid specimens with well defined contact geometries and uniform pressure application. However,

FIG.VII-10. SURFACE CONTACTS

the rather unusual geometry and load distribution in the 3M cold frame design would not allow analytical calculations to be made with any confidence.

A series of discussions with Prof. B.B. Mikic of MIT lead to the conclusion that the only way to get a good estimate of the interface ΔT for this geometry sample was to design an experiment closely approximating it.

Some rough calculations made for rigid samples of these materials indicated that we could expect the best interface conductance below 20 rms and a secondary peak in conductance between 100 and 300 rms.

2. Test Plan

A flat 12 couple segment of the cold ring was designed and instrumented to measure heat flow across the interface. This size sample was large enough to prevent edge effects from overpowering the normal heat flow.

From the analysis mentioned above, it seemed as if surface finishes of 32 rms or better would give the best thermal properties to the interface. However, because of the difficulty of machining low rms finishes we decided to investigate a wider range in case we could get by with a coarser finish in the actual module design.

Accordingly, surface finishes of 16, 32, 63, 80, and 125 rms were included in the studies. The bare surfaces were measured against each other in various combinations in both vacuum and inert gas environments. A series of soft metallic foils was also investigated for use in filling some of the macroscopic asperities within the allowed waviness criteria of .0005"/1.000".

3. Test Hardware

The basic test fixture (Dwg. No. LCP 10220) is indicated schematically in Figure VII-11. It consisted of a copper heater block with three small heaters for uniform

FIG. VII-11. THERMOELECTRIC MODULE COLD RING TO HOUSING
 ΔT TEST FIXTURE

temperature distribution, a stainless steel thermal shunt with six thermocouples to determine heat flux to the sample, a copper block with close fitting slots to deliver heat to the ribs in simulation of the thermoelement followers in the actual generator design, and Min-K insulation to minimize edge losses. The cold "ring" sample was made to the same dimensions as the 3M cold rings with the exception of being made flat to facilitate manufacturing and testing, and also it had no cam locks and holes, since no individual thermoelements were being installed. The springs were made to 3M specifications and the loads were the same as for the generator.

Instrumentation consisted of 26 Chromel/Alumel .003" thermocouples. Six were used to measure the total heat flux and ten each were used in the "cold ring" and "housing" samples. They were located as shown in Fig. VII-12, four pairs beneath the springs, four in the ribs, and two in the open web areas.

All of the TC's were read automatically on a Doric Digitrend 240 process monitor. All 26 TC's could be read in several seconds minimizing drift effects. For each test, the samples were raised to operating conditions and allowed to stabilize for several hours. Then 10 minute data were taken until there was less than 1°C drift for 1 hour (7 consecutive readings). These readings were averaged for ΔT calculations.

4. Results

The test results are summarized in Figs. VII-13 thru VII-18. Several of the tests were repeated up to four times to check reproducibility. In vacuum, the average readings of the TC's under the ribs, springs, or webs were reproducible within 1.5 degrees in all cases except one when a 2.5 degree difference in rib average was found for bare 16/63 samples. In Argon all the average ΔT 's were within one degree in all cases.

As indicated in Fig. VII-13 there was a fairly wide spread in the data for various

CONTACT ΔT VS. SURFACE FINISH IN VACUUM

FIG.VII-13a.

CONTACT ΔT VS. SURFACE FINISH IN ARGON

FIG.VII-13b.

GAP ΔT WITH .001" FOILS IN VACUUM

FIG.VII-14a.

GAP ΔT .001" FOILS IN ARGON

FIG.VII-14b.

FIG. VII-12. THERMOCOUPLE LOCATIONS AT INTERFACE

GAP ΔT WITH .001" FOIL IN VACUUM

FIG.VII-15a.

GAP ΔT .001" FOIL IN ARGON

FIG.VII-15b.

GAP ΔT WITH .002" FOIL IN VACUUM

FIG.VII-15c.

GAP ΔT WITH .001" FOIL VACCUM

FIG.VII-16

GAP ΔT VS SURFACE FINISH WITH
.001" Pb FOILS IN VACUUM

FIG.VII-17.

GAP ΔT VS SURFACE FINISH WITH
TIN & GOLD INTER LAYERS IN VACUUM

FIG.VII-18.

bare surfaces in vacuum. However the 16/63 combination did give better conductance overall. In Argon the differences were much less but the 16/63 was still better than all except the very difficult to machine 16/16 combination (which was rather poor in vacuum).

An obvious pattern observed in all of these tests was that of a rather high ΔT under the stiffening ribs as compared to the ΔT 's at the more flexible and conformable spring and web areas of the surface. Thus, there may be somewhat more heat transferred through the springs than one would at first believed. Even though the ΔT is larger under the ribs, there is still a large heat flux through them. It must be distributed over the conformable surfaces, though, before crossing the interface.

From Figs. VII-14 to VII-18 it is obvious that the use of foils significantly lowers the ΔT 's. One surprising observation was that the hardness of the foil material was much more important than the thermal conductivity in lowering the interface ΔT (Table VII-3). This would indicate that it is more important to ensure contact over large areas rather than having a good conductor touching in fewer places.

TABLE VII-3
PHYSICAL PROPERTIES OF INTERFACE MATERIALS

<u>Material</u>	<u>Thermal Conductivity ($\text{Wm}^{-1} \text{K}^{-1}$)</u>	<u>Brinell Hardness</u>	<u>Melting Point ($^{\circ}\text{C}$)</u>	<u>Density (g/cm^3)</u>
Silver	416	27	961	10.49
Copper	390	45	1082	8.94
Gold	310	25	1063	19.32
Aluminum	230	23	660	2.71
6061-T6		95		
2024-T3		120		
Tin	60	4	232	7.3
Lead	33	5	327	11.35

In almost all cases, tin and lead were better than the high conductivity Cu, Ag, Au, and Al. Also, as can be seen in Figs. VII-15a and VII-15c, the copper is easily work hardened and must be carefully annealed to get reasonable ΔT 's. (In Fig. VII-15a the work hardened Cu data points are off the graph as indicated by the ΔT values adjacent to the symbols.)

Figure VII-15c indicates that .002" foils are less effective than .001" foils in reducing the interface ΔT .

Very little difference was observed between tin foil and .001" tin plate (solid symbols in Figs. VII-15a, VII-15b, VII-18). As a matter of fact, the plated samples showed somewhat more variation between individual thermocouple readings.

The effect of surface finish is greatly diminished by the use of soft foils as can be seen in Figs. VII-17 and VII-18. However, the 16/63 rms combination still appears to be the best choice.

5. Conclusions

Lead or tin foils (or plating) are the preferred materials to reduce the interface ΔT . The choice would depend upon temperatures experienced during processing. The combination of 16 rms on the harder (2024) surface and 63 rms on the softer (6061) surface gave the best results, but the use of foils minimized the difference between surface finishes. Either of the materials recommended here would yield an acceptable interface ΔT for the SIG design.

G. EMISSIVE COATING DEVELOPMENT

1. Background

The basic requirements of the thermal control coating for the SIG/Galileo Mission are ease of application and repair, good adherence to aluminum and copper, high total hemispherical emittance, low solar absorptance, and good stability against damage by solar ultraviolet radiation. In March 1976, under the LCHPG Program, a subcontract was placed with Battelle Columbus Laboratories, Columbus, Ohio to identify candidate coatings, to test and evaluate appropriate surface preparation and paint application procedures, and to apply the coatings selected for final evaluation to hardware supplied by TES. The BCL technical report which describes these efforts is listed in Ref. 7. With regard to the above-listed criteria and as a result of this program, two thermal control paints were selected for final detailed evaluation: MS-74, which consists of ZnO , Al_2O_3 , and TiO_2 in a potassium silicate (K_2SiO_3) binder, developed by NASA-Goddard Space Flight Center, Greenbelt, Md; and SNAP 19, which consists of ZrO_2 in a sodium silicate (Na_2SiO_3) binder, developed by TES. MS-74 was flight-qualified on Apollo 14, 15, and 16, and ATS-6 spacecraft; SNAP 19 was proven effective on Pioneer 10 and 11 and on Viking RTG's.

Both coatings, as prepared by BCL, were tested for solar absorptance (α_s) and total hemispherical emittance (ϵ_{TH}) by TRW, Redondo Beach, Cal., under the above-cited BCL subcontract. Solar absorptance values were 0.19₂ for MS-74 and 0.28₅ for SNAP 19. Total hemispherical emittance values at 120 °C were 0.94 for MS-74 and 0.93 for SNAP 19.

An interim topical report, "Heat Pipes, Attachment, and Thermal Coatings," Ref. 8, was prepared.

Because BCL encountered severe problems in attempting to prepare MS-74 and apply it to aluminum hardware, another similar paint, Z-93 (ZnO in a potassium silicate (K_2SiO_3) binder developed by IITRI, Chicago, Illinois), was chosen to replace MS-74 as a candidate coating for SIG application.

2. Technique Development

TES initiated a test program designed to: (a) evaluate the effect of various surface preparation methods on the adherence of SNAP 19 and Z-93 to aluminum, and (b) compare the adherence of these two paints to aluminum. Results of this study, as reported in Ref. 9, not only showed that the adherence of Z-93 to aluminum substrates were significantly superior to that of SNAP 19 but also identified an optimal surface preparation method for Z-93. On this basis, and also because of its appreciably lower solar absorptance, Z-93 was chosen for application to the SIG GDS and related heat-pipe/fin test panels.

From the results of this test program and from information provided by ITRL, the developer of Z-93, a process specification for the formulation, blending, and application of Z-93 to aluminum and copper alloy, PS 030021 (Z-93 Thermal Control Paint Formulation and Application), was written and released in February 1978. Minor modifications of this process, based on continued technique development, were made and released in March 1978 and in March 1979.

In April 1978, Z-93 was successfully applied to two heat-pipe/fin test panels, LCP 10009-029 and -049. Local repair of a small painting defect that developed after coating was also successfully accomplished. This indicated that Z-93 had acceptable repairability potential.

During the period July thru December, 1978 emphasis was directed toward the continued development of operator application skills and associated facility improvements.

The scale-up spectrum encompassed four stages: (1) specimens, (2) flat panels, (3) simple cylindrical/fin structure, and (4) dummy housing with fins. Excellent coating skills were developed thru stage 2; initiation of stages 3 and 4 was terminated by the stop work order.

3. Long Term Tests

As a result of thermal aging tests (0, 24, 500, and 2500 hours) followed by bend tests over a 0.720-inch diameter mandrel, the adherence of Z-93 to small (4.5 x 2.0 x 0.025 inch) coupons was found to be excellent. In addition, Z-93 applied to the same-size coupons survived aging in air for 5000 hours in good condition. The same type of thermal aging and bend tests showed that SNAP 19 had poor adherence to aluminum.

4. Properties Characterization

In addition to its adherence to aluminum and copper substrates, the following four properties of Z-93 were regarded as important for the Galileo mission: (1) solar absorptance, (2) total hemispherical emittance, (3) solar absorptance degradation, and (4) stability toward electrostatic charging. A purchase order was placed with IITRI for a compilation and assessment of these properties as they were related to Galileo mission environmental specifications. A final report for this subcontract, entitled, "Z-93--An Estimate of its Properties and Performance in the Galileo Mission," (IITRI C6429-1), was issued on January 29, 1979. A summary tabulation of the (in IITRI's opinion) most reliable values of the above-listed properties, taken from this report, is given in Table VII-4.

Emittance tests performed by TRW (P.O. 81539) gave the values listed in Table VII-5. These values were somewhat lower than expected, based on the BCL-TRW data, contemporary KIPS-TRW data, and typical literature data. Hence additional tests were initiated with the Boeing Company, Seattle, Washington (P.O. 81232) and with Aerojet ElectroSystems

TABLE VII-4

COMPREHENSIVE TABULATION OF THE
PROPERTIES OF Z-93 THERMAL CONTROL PAINT⁽¹⁾

Optical Properties*

Solar absorptance, * α_s	0.165 ± 0.01
Total normal emittance, ϵ_N	0.93 ± 0.03
Total hemispherical, ϵ_H	$\begin{cases} 0.92 \pm 0.02 (50^\circ\text{F}) \\ 0.90 \pm 0.02 (200^\circ\text{F}) \end{cases}$
$d \epsilon_N / dt$	$\begin{cases} \sim 0 & T < 100^\circ\text{F} \\ -4 \times 10^{-4}, & T > 100^\circ\text{F} \end{cases}$

Electrical Properties**

Surface conductivity, (σ_s)	$\sim 5 \times 10^{-8} (\text{ohm-cm}^2)^{-1}$
Volume conductivity, (σ_v)	$\sim 10^{-9} (\text{ohm-cm})^{-1}$
Temperature dependence, ($\sigma = \sigma_0 \exp [-\Delta H_a / RT]$)	$\Delta H_a \approx 0.05 \text{ eV}$

*Values given pertain to opaque coatings (minimum 3 mil thickness).

**These properties depend upon moisture content of the coating, which in turn depends upon its humidity-temperature history.

(1) From IITRI Report No. IITRI C6429-1, January 29, 1979.

TABLE VII-5

Z-93 - TRW EMITTANCE DATA
Received by Telecon on September 20, 1978

Measurement Temperature °C	Total** Hemispherical Emittance $\pm .005$
25	0.896*
90	0.898
105	0.902
115	0.904
125	0.903
140	0.910
150	0.907

*Average of 16 inspection measurements, one per quadrant, on four 4-inch X 4-inch samples.

**Calculated from spectral reflectance measurement.

(P.O. 81225). At the nominal SIG operating temperature at 125 °C, Aerojet found the total hemispherical emittance of Z-93 to be $0.95^{+0.02}_{-0.03}$.

A subcontract (P.O. 81232) was placed with Boeing to measure the solar absorptance of Z-93, and a preliminary value of 0.16 was transmitted to TES by telecon in January 1979. In order to characterize the rate of absorptance degradation and electrical property degradation at 125 °C, an approval package was prepared in October 1978 and sent to DOE. The proposed work was an optical-electrical degradation measurement study to be performed by Boeing in their combined UV-proton-electron facility.

The surface resistivity of Z-93, after outgassing in vacuo ~ 150 °C for three days, was measured by NASA-Goddard Space Flight Center at temperatures from 25-125 °C. Results are illustrated in Figure VII-19. These data indicate that Z-93 satisfied the Galileo requirement that the paint develop no more than 10 volts potential at a 5×10^{-10} amp/cm² charging current. To characterize further the electrostatic charging behavior of Z-93, plans were formulated and test conditions selected for UV and electron irradiation of Z-93 in a simulated space environmental chamber at NASA-Lewis Research Center. In this test, a traveling probe scans the surface of the sample and measures its potential while the electron beam is on. In September 1978, NASA-Lewis submitted to DOE an estimated cost for conducting this program. Approval was received. Test specimens (4) were prepared and shipped to NASA-Lewis in January, 1979. Testing which was scheduled to start in February, 1979 was cancelled due to the "stop work order."

FIG.VII-19. EFFECT OF TEST TEMPERATURE ON AREA OF Z-93 EMISSIVE COATING
(MEASUREMENTS BY (NASA GODDARD))

H. ELECTRICAL RECEPTACLE/BIMETAL RING TESTS

The SIG/Galileo generators are provided with hermetically sealed receptacles serving as electrical feedthroughs for power and instrumentation. It was the goal of this subtask to establish a .95 reliability factor at 50% confidence level for the hermetic seal integrity of flight hardware units. As a design criterion, the maximum leak rate of this receptacle type was defined to be within $\leq 1 \times 10^{-8}$ st cc He/sec-atm over a temperature range from +70°F to +400°F.

The receptacle model selected was a type P/N 78033-16-26PN manufactured by the Deutsch Company, Banning, California. Since the shell of the receptacle is stainless steel (type 20CB3) and the housing of the generator is aluminum, it was necessary to develop a special stainless steel/silver/aluminum bimetal transition ring in order to weld the receptacle unit first to the transition ring and then the receptacle/bimetal ring assembly directly to the generator housing.

A test installation with 36 receptacle test stations was built at the facilities of TES to perform extensive receptacle as well as receptacle/bimetal transition assembly leak testing. A detailed receptacle test plan was developed which included thermal cycle tests, short and long-term life performance tests (thermal aging at 400°F, 1000 and 8000 hours) and thermal safety margin tests to determine the thermal limit of the test sample (up to failure). The receptacles were tested in different groups which had different sequences of these functional tests.

Three (3) receptacle units and seven (7) receptacle/bimetal transition ring assemblies were tested and the results were satisfactory. After 8664 hours at 400°F the leak rates of these test samples varied from 5.8×10^{-10} to 1.6×10^{-9} st cc He/sec-atm

at 400°F. A thermal margin test of one receptacle showed the thermal limit of this unit to be at 698°F temperature after 30 thermal cycle tests from 150°F to 400° to 150°F and 1635 hours of life performance testing at 400°F. Detailed test results were reported in the Monthly SIG/Galileo Technical Progress Reports and Ref. 7.

I. HEAT SOURCE SUPPORT VIBRATION TESTS

In early 1979, the final series of vibration tests of a dynamic model of a generator were performed at the Naval Surface Weapons Center (NSWC) test facility. The model consisted of a simulated heat source weighing 42 lbs. supported within a housing at both ends by insulation (CBCF-3 and Min-K 1400). The detailed parts of the mock up system are shown in Figs. VII-20 and VII-21. Min-K insulation in particular has very little structural or hysteresis damping, in the vicinity of 1%, and therefore the load amplification of the heat source in either the axial or lateral directions could be rather critical from the standpoint of insuring positive structural margins.

It can be shown that a test in which the simulated heat source is a single mass is conservative with respect to a system with an isotopic heat source. The MHW isotopic heat source can be realistically divided into two principal masses for a mathematical model, the aeroshell and the heat source intervals. Each mass has its own particular modal frequency. Therefore, an important facet in the SIG program was to verify dynamic load capability of an ETG at qualification and acceptance levels.

The environment of interest included the qualification level of random vibration in both axial and lateral directions where the spectrum could be defined with a spectral density of $0.1 \text{ g}^2/\text{Hz}$ from 250-1000 Hz with rolloffs of 9 db/octave and 12 db/octave at each end, respectively. Other important questions had to be answered such as:

1. In the lateral vibration mode is there sufficient frictional forces to support the heat source?
2. What are the loads generated into the housing from response to an input vibration spectrum?

FIG.VII-20. HEAT SOURCE SUPPORT DYNAMIC MODEL

FIG.VII-21. DYNAMIC MODELS DETAILS

3. Both insulations have a relatively low stress capability. Will the insulations survive?

A range of preloads were included in the test program since the initial preload will relax with time.

The initial testing in any series consisted of low level sinusoidal inputs. This data would help to define damping characteristics, determine the natural frequencies and check out the system.

For the referenced test series, the thicknesses of insulations were 0.73 - 0.75 inch of Min-K and 1.34 - 1.35 inch of CBCF-3 at each end of the simulated heat source. The internals were shimmed to obtain the desired preload for each phase of the test program. Preloads of about 1900 and 1600 lbs. were included. Additional testing at somewhat lower preload levels was also planned. However, a lead on the secondary of the output transformer in the master gain amplifier chassis failed. This produced a transient in the system consisting of a high acceleration level that has been speculated as in excess of 100 g's. This anomaly cancelled further testing.

Table VII-6 presents the sequence and type of test conducted in this series. Up until the time of the lead failure, all test results were as expected. Loss in preload was either low or immeasurable. Preload was measured with a Dillon gauge. Insulation details were not fractured and the accelerometer readings were acceptable. There was no indication of separation or shock between components.

TABLE VII- 6

HEAT SOURCE SUPPORT VIBRATION TEST PROGRAM RESULTS

<u>Test</u>	<u>Load/Level</u>	<u>Direction</u>	<u>Initial Preload (lbs.)</u>	<u>Comments</u>
Sinusoidal	1/2 g	Lateral	1850-1950	
Sinusoidal	1 g	Lateral	1850-1950	Accelerometer failed. Test had to be rerun.
Sinusoidal	1 g	Lateral	1850-1950	
Sinusoidal	1 1/2 g	Lateral	1850-1950	Final preload measured at ~1900 lbs.
Random	Acceptance	Lateral	1900	
Random	Qualification	Lateral	1900	Preload ~1900 - 2000 lbs.
Sinusoidal	1/2 g	Lateral	1600	
Sinusoidal	1 g	Lateral	1600	Final preload ~1200 lbs. Disassembled unit; total insulation compression about .011 inch.
Sinusoidal	1/2 g	Axial	1600	Final preload ~1700 lbs.
Random	Acceptance	Axial	1600	
Random	Qualification	Axial	1600	Final preload 1650-1680 lbs.
Sinusoidal	1/2 g	Lateral	1650-1680	Final preload ~1650 lbs.
Random	Acceptance	Lateral	1650	Test facility equipment failure (see test).

Natural Frequencies: 150-160 Hz lateral.
~ 220 Hz axial.

J. CBCF INSULATION DEVELOPMENT

1. Background

Carbon Bonded Carbon Fiber (CBCF) is a generic designation for a class of high temperature insulations which was developed at the Oak Ridge Y-12 Plant for reentry vehicle applications since 1967 (Refs. 11, 12 and 13). The typical ranges of CBCF product properties are as follows:

a. Density	0.06 to 0.90 gm/cm ³
b. Thermal conductivity	0.20 to 1.4 W/m-°K at 900°C in nitrogen
c. Compressive strength	7.3 psi to 13,000 psi at 20% strain

The material can be tailored to meet specific properties within the above ranges.

Variables which influence product properties include:

- a. Material variables - fiber type, fiber diameter, fiber length and binder type
- b. Process variables - fiber carbonization time and temperature, binder/fiber content, temperature/time profile for curing and carbonizing cycled.

A billet or preform shape is fabricated by: (1) vacuum molding the carbon fiber/binder slurry, (2) air curing the molded shape while still supported on the mold or mandrel, and (3) removing the molded shape from the mold or mandrel and carbonizing.

Earlier Y-12 development efforts included the following type raw materials:

a. Fiber materials	Rayon or PAN (polyacrylonitrile)
b. Binder materials	Starch or Phenolic resin

Machining of CBCF to close tolerances (typically ± 0.0005 " to ± 0.010 ") can be attained using single point tooling in conjunction with appropriate fixturing for support of the semi-rigid blank.

CBCF materials exhibit anisotropic microstructure and properties, both thermal and mechanical. During vacuum molding of the carbon fiber/binder slurry the carbon fibers become oriented preferably in planes which are "perpendicular" to the thickness direction of the moulded form. Typically the "thru-thickness" direction is associated with a minimum in both thermal conductivity and compressive strength.

2. CBCF Development for SIG Applications

The development of CBCF insulation materials for potential SIG/GDS applications was initiated at ORNL in early 1977. * The prime driving force for this effort was the need for a load bearing high temperature insulation material for use in the heat source support system and the prospective inadequacies of contemporary "oxide type" low thermal conductivity insulation materials. Concurrently, the development of a low conductivity non-structural CBCF insulation was pursued as a potential backup or replacement for the fragile Zircar ZYF-B3 (fibrous zirconia) insulation, the primary "hot side" insulation candidate for the GDS/SIG generator.

The ORNL CBCF program yielded six types of CBCF insulations. The typical properties and characteristic raw materials and manufacturing processes are summarized in Table VII-7. CBCF-3 and CBCF-4 insulations were successfully produced and utilized in GDS-1. With the selection of molybdenum multi-foil for the side insulation in the flight system design in the spring of 1978, development of production capability was focused on the "load bearing" CBCF materials, namely CBCF-3 and, for an interim period in the first half of 1978, CBCF-5.

Since initiation of the Galileo flight program in early 1978 the ORNL/TES CBCF program was directed toward the following objectives:

*Participants in program included the following: ORNL - A.C. Schaffhauser, Program Manager; G.C. Wei, Task Leader; TES Liaison - W.J. Barnett; Y-12 - Z. Ardary, G.W. Brassell and D. Reynolds.

TABLE VII-7
MATERIAL PARAMETERS OF CBCF INSULATIONS
PREPARED BY ORNL

Parameter/Type	CBCF 1	CBCF 2	CBCF 3	CBCF 4	CBCF 5	CBCF 6
Fiber Precursor	Rayon	Rayon	Rayon	Rayon	PAN	PAN
Precursor Fiber Average Diameter	11 μ m	11 μ m	11 μ m	8 μ m	12 μ m	12 μ m
Fiber Carbonization Temperature	1000°C	1000°C	1000°C	1000°C	1350°C	1350°C
Carbon Fiber Average Diameter	7 μ m	7 μ m	7 μ m	5 μ m	9 μ m	9 μ m
Precursor Fiber Average Length After Chopping	0.010 in.	0.010 in.	0.010 in.	0.030 in.	0.010 in.	0.030 in.
Type of Binder	Phenolic Resin	Phenolic Resin	Phenolic Resin	Phenolic Resin	Phenolic Resin	Phenolic Resin
Carbon Fiber Content	44.4%	66.7%	54.1%	50%	25%	25%
Binder Content	55.6%	33.3%	45.9%	50%	75%	75%
Fiber Wiley Milling Sizing Plate Hole Size	2 mm and next 1/2 mm	2 mm and next 1/2 mm	2 mm and next 1/2 mm	1/4 in. and next 2 mm	2 mm and next 1/2 mm	1/4 in. and next 2 mm
Vacuum Molding Pump Vacuum Line Diameter	2 in.	2 in.	2 in.	1 in.	1 in.	1 in.
Drying and Curing	100°C for 48 hr	100°C for 48 hr	100°C for 48 hr	130°C for 24 hr	100°C for 48 hr	130°C for 24 hr
Furnacing	1350°C/3 hr under N ₂ or Ar	1350°C/3 hr under N ₂ or Ar	1350°C/3 hr under N ₂ or Ar	1350°C/3 hr under N ₂ or Ar	1600°C/3 hr under Ar	1600°C/3 hr under Ar
Nominal Density	0.26 g/cc	0.24 g/cc	0.25 g/cc*	0.15 g/cc*	0.29 g/cc	0.13 g/cc
Nominal Density Standard Variation	±0.01 g/cc	±0.01 g/cc	±0.01 g/cc*	±0.01 g/cc*	±0.01 g/cc	±0.01 g/cc
Nominal Compressive Strength	270 psi	130 psi	175 psi*	24 psi*	160 psi	14 psi
Nominal Standard Deviation of Compressive Strength @ 5% strain	10 psi	10 psi	10 psi*	5 psi*	10 psi	5 psi
Chemistry	All impurity elements shall be less than approximately 100 ppm by weight. The total impurity content shall be less than 0.02% by weight.					
Thermal Conductivity In Vacuum (W/K/M)	650°C 0.165	-	0.130	0.044	0.300	0.071
	1000°C 0.170 (?)	-	0.180	0.083	0.485	0.115
Relaxation Time Obtained from Relative K Screening System	-	-	87.5 min	138.5 min	74.5 min	110.5 min

*Properties noted are typical for GDS vintage product.

- a. Evaluate PAN (polyacrylonitrile) type fiber as a replacement for the rayon type fiber previously employed in CBCF -3 and -4 development.
- b. Update production procedures and quality plans for CBCF manufacture.
- c. Scale up from GDS 24" diameter by 2.5" thick billet size to a 26.5" diameter by 2.5" thick billet.
- d. Develop a rapid reproducible technique for the measurement of thermal conductivity on a qualitative or comparative scale.
- e. Demonstrate pre-production reproducibility of load bearing (i. e. CBCF-3) insulation billet stock.
- f. Manufacture CBCF billet stock for flight program.

Interest in the PAN type fiber was prompted by contemporary technical changes in the tire cord industry which eliminated commercial production of small diameter rayon fiber. While the "on hand" stock of rayon fiber appeared adequate for the Galileo program, qualification of an alternate commercial fiber appeared mandatory. Subsequently a new commercial source of rayon fiber (Baunit) was identified and characterization of this fiber stock was initiated (a low priority effort due to overlap with CBCF-3 production qualification and manufacture).

3. Comparative K-Test Development

The relative thermal conductivity screening system is based on the premise that the cooling curve of an insulation-surrounded thermal mass induced by a sudden drop in the system temperature is a good indication of the insulation's relative thermal conductivity. The cooldown, or relaxation time from 950 to 550 °C is the measured parameter for "thermal conductivity." This system, as developed by ORNL in the October 1978 thru January 1979 time frame exhibited great promise for obtaining rapid data on the relative thermal con-

ductivity of insulations. Measurements are reproducible. Correlation of relaxation times for various CBCF insulations of known conductivity (guarded hot plate) indicate that a 20% difference in conductivity in the range covered by CBCF insulations is distinguishable by this technique. With additional correlation, this Comparative K-Test Method offers promise as a quality control tool for CBCF type insulations.

4. PAN Fiber Utility

Results of the PAN fiber CBCF production study yielded the following observations and conclusions:

- a. Continued development of the PAN fiber CBCF-5 load bearing type insulation is not warranted due to its unexpectedly high thermal conductivity and its unusually large temperature dependence (c.f. Table VII-7).
- b. Continued development of the PAN fiber "low-K/non-structural" CBCF-6 insulation, while technically warranted, is not required for the SIG program due to the adoption of a multifoil side insulation design concept. As noted in Table VII-7, the thermal conductivity of CBCF-6 is approximately 20% higher than that of CBCF-4, its rayon fiber counterpart.

5. CBCF Manufacturing and Quality Control Procedures

The manufacturing and quality flow diagram for carbon-bonded-carbon-fiber insulation materials, as developed by ORNL, is illustrated in Figure VII-22. Applicable manufacturing and quality procedures are designated for each inspection point and/or manufacturing operation. These procedures which were evolved by ORNL were reviewed and approved (with minor changes) by TES Materials Engineering and Quality Engineering prior to the initiation of production of material for flight quality generators - S/N 1, etc.

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FLOW DIAGRAM FOR MANUFACTURE AND CHARACTERIZATION OF CARBON-BONDED-CARBON-FIBER INSULATION AND INSTRUCTIONS IN PARENTHESIS FOR CONTROLLING EACH STEP

FIG. VII-22. (FROM REF. 14)

A set of three (3) CBCF-3 pre-production qualification billets was received from ORNL on October 28, 1978. Unfortunately, due to scheduling delays at the testing vendor, Dynatech, and the extended cycle time for receiving inspection, specimen machining and inspection at TES, outgassing at MRC Mound facility, re-inspection at TES and shipping to the testing source, reliable (guarded hot plate) thermal conductivity characterization data was not available at the time of the "stop work order." Also, due to program schedule requirements the manufacture of the initial six* CBCF-3 billets was initiated in Nov. 1978.

The pre-production billets, as shown in Table VII-8, did not meet the ORNL density specification for CBCF-3, namely $0.25 \pm .01 \text{ gm/cm}^3$. The impact of this higher density, typically 10-12% above specification and 13-17% above nominal, on thermal conductivity remains to be assessed. Also, the room temperature compressive strength at 5% strain, 307 ± 31 to 337 ± 25 , considerably exceeded the specification requirement of 175 psi average and 150 psi minimum.

A comparison of several material and process parameters for this pre-production and two earlier CBCF-3 lots are presented in Table VII-9. Inspection of these data suggest that the marked changes in product density and compressive strength may be associated with the noted variances in fiber length distribution, particularly the magnitude of short fibers. Hence, pending further confirmation of this hypothesis, improved control of the chopping operation and a tightening of the fiber length distribution specification (MET-CER-MS-11) should be considered.

The SIG flight design is predicated on the properties exhibited by CBCF-3A.

*Procurement of three billets was completed at the time of the "stop work order."

TABLE VII-8
CBCF-3 DENSITY MEASUREMENTS - GMS /CM³

	<u>Billet No. - Preproduction Reproducibility Test Series</u>		
	<u>TR-1B</u>	<u>TR-2B</u>	<u>TR-3B</u>
ORNL-QC	0.284 ± .006	0.293 ± .009	0.292 ± .014
TES-As received billet	0.281	0.287	0.288
TES			
Thermal Conductivity	0.275	0.275	0.274
Test specimens*	0.280	0.275	0.271
8" dia. x 1" from billet midplane			
TES			
Nominal 1.25" diam. x 0.4" specimens taken at top, center and mid-plane loca- tions near edge, center and mid- radius of billet	0.283 ^{+.016} - .013	0.288 ^{+.029} - .029	0.287 ^{+.022} - .029

*Measured after outgassing.

NOTE - QC specification density for CBCF-3 $0.25 \pm .01 \text{ gm/cm}^3$.

TABLE VII-9

MATERIAL PARAMETERS OF CBCF-3 INSULATIONS MADE FROM
THREE BATCHES OF AMERICAN ENKA FIBER

<u>Purpose</u>	<u>CBCF 3-A</u>	<u>CBCF 3-B</u>	<u>CBCF 3-C</u>
	<u>Initial Study</u>	<u>GDS Production</u>	<u>Flight System Preproduction</u>
Average carbon fiber diameter	7 μ m	7 μ m	7 μ m
Fiber chopper	Temple Co. *	Precision Co. *	Microfiber
Average precursor fiber length	0.010"	0.010"	0.010"
Distribution of fiber length** - frequency %			
< 50	N/A	3	14
51-100		9	23
101-150		25	30
151-200		49	24
201-250		8	7
251-300		3	1
301-350		3	0.5
> 350		2	0.5
Binder, Wiley mill, and etc. are constant.			
Billet size	10" diam by 1.5" thick	24" diam. by 2.5" thick	26.5 diam. by 2.5" thick
Average density (g/cc)	0.25	0.27	0.29
Density variation (g/cc)	± 0.01	± 0.01	± 0.01
Compressive strength*** (psi)	175	236	317
Strength variation*** (psi)	10	15	17
Thermal conductivity in vacuum (W/K/M)	650°C 0.130 1000°C 0.180	N.A. N.A.	In Process In Process
Relaxation time obtained from relative K screen- ing system	?	87.5 min.	In Process

*Out of business.

**Process specification calls for greater than 80% of fibers shall be within $\pm 15\%$ of the aim of 175 microns.

***At 5% strain in direction normal to fiber alignment plane.

K. HEAT PIPE ATTACHMENT TECHNOLOGY

On April 1, 1976, Battelle Columbus Laboratories were subcontracted by Teledyne Energy Systems to develop an optimum joining method for attaching copper/water heat pipes to an aluminum alloy (6061-T6) radiator and housing for the SIG/Galileo Program. Specifications required that the joint between the copper heat pipe and the aluminum must have a sufficient thermal conductivity such that there is no more than a 2°C temperature drop at a nominal operating temperature of 125°C (257°F). Further, any type of joining technique which is used must take into account the thermal stability of both the heat pipe and the heat-treated 6061-T6 temper aluminum alloy hardware. Accordingly, a maximum temperature of 177°C (350°F) during joining was desirable, however this condition was increased to 221°C (430°F) per solder alloy selection. In addition, the resultant joint had to be capable of a minimum of 1,500 psi shear strength which was determined in a preliminary flight case analysis by TES.

It first appeared that either organic adhesive technology or metallurgical joining technology could both be useful in developing a copper-to-aluminum bond to meet the specifications outlined above. Both approaches were evaluated during the research program. The organic adhesive technology was dropped because it failed to meet some of the requirements.

A topical report summarizing the heat pipe technology program is presented in Ref. 3.

L. REFERENCES

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13. T. G. Godfrey, D. L. McElroy and Z. L. Ardary, "Thermal Conductivity of Oriented Fibrous Carbon Insulation from 300 to 1300°K in Nitrogen and Argon at One Atmosphere," Nuclear Technology, Vol. 22, April, 1974, pp. 94-106.
14. G. C. Wei, "Quality Assurance Program for Fabrication of Carbon Bonded Carbon Fiber Insulations," ORNL, November 1, 1978.

VIII. IMPROVED MHW HEAT SOURCE DEVELOPMENT

As a result of the predicted severity of potential launch abort accident environments associated with the Shuttle, a development program was initiated in early calendar year 1978 to assess the thermal/structural characteristics of a new aeroshell material. The original aeroshell was fabricated from POCO AXF-5Q, a polycrystalline graphite. The new aeroshell consists of two forms of 3D carbon-carbon utilizing the high modulus PAN fiber and carbonized per the HiPIC process at 1500 psi. To evaluate these materials the test program included numerous environments including oxidation, plasma arc, blast overpressure, etc. The results of this development test program are reported in TES-33009-43, a topical report titled, "Improved MHW Heat Source Aeroshell Project," dated June 1979.

IX. MATERIALS COMPATIBILITY AND OUTGASSING

The SIG flight generator design concept, configuration and materials, as illustrated in Fig. IV-3, presents several significant changes relative to the GDS generator which impacted the existing GDS compatibility/outgassing data base. These changes and a qualitative assessment of the associated system impact are as follows:

- Elimination of the Isolation Hot Frame, IHF - Exposes the converter section to the MHW isotopic and/or graphite type electrical heat source and their effluents. Conversely, the heat source is exposed to the converter effluents (e.g., selenium).
- Vacuum operation of the RTG after deployment - Aggravates selenium sublimation from the "P-leg;" exposes the generator internal volume to the dynamic space vacuum via the punctured diaphragm of a PRD (pressure relief device) provides an innocuous "sink" for noxious non-condensable outgassing or reaction products.
- Molybdenum multifoil insulation (three cylindrical sections) is employed as the primary side insulation. Because of installation restrictions, the multifoil pack between the two thermoelectric module rings is augmented by a cylindrical ring of "packed" Fiberfrax Hi-Fi 660H paper. Raises question of molybdenum/selenium compatibility.

During the Phase III SIG Galileo flight program the compatibility impact of the above design/material changes were addressed. Also, inputs from on-going GDS experimental compatibility/outgassing studies and the GDS diagnostic disassembly, were employed in the continued assessment of system compatibility.

Experimental and analytical support of the system compatibility assessment effort

was provided by Oak Ridge National Laboratory (ORNL), Battelle Columbus Laboratory (BCL) and General Atomics Company (GA). Supporting activities included the following:

- Oak Ridge National Laboratory
 - Molybdenum/Selenium Compantibility
 - CBCF Insulation Outgassing
 - CBCF Trace Impurities
- Battelle*
 - CBCF and Graphites Outgassing
 - Carbon/Min-K and Fiberfrax Compatibility
 - Trace Impurities in As-received and Outgassed Insulations and Graphites
- General Atomics*
 - Vapor species above Min-K TE 1400 and Min-K TE 1800
 - Primary Outgassing Behaviour Min-K TE 1400 and Min-K TE 1800
 - Trace Impurities in As-received Min-K Inuslation
 - Thermodynamic Analyses of Potential Cyclic Reactions

A "peer group review" of the SIG materials compatibility was provided by the meetings of the SIG Compatibility Working Group. This informal, group which was formed at the initiation of Phase I (LCHPG/GDS) and continued to function to date, is comprised of representatives from 3M, ORNL, BCL, JPL and TES (W. J. Barnett of TES served as Chairman). A meeting of this working group was held at the Jet Propulsion Laboratory on September 20, 1978, with attendance by representatives each of the cited organizations.

* These efforts while funded under Phase II overlapped the Phase III SIG/Galileo flight program.

To date, no major materials compatibility problems have been identified in the non-module segments of the SIG/Galileo RTG design. Areas of potential concern or minimal data base are reviewed. Module compatibility problems (i.e., N-leg cracking and crystal structure stability, P-leg sublimation and creep, P-leg copper extrusion, P-leg wrap stability, selenium sublimation effects - spring relaxation, reaction with other cold side hardware, cold side electrical shorts, and others) have been reviewed extensively (Ref. 1) and hence are not discussed herein.

A. COMPATIBILITY STUDIES AND ASSESSMENTS

1. Selenium Sublimation Non-Module System Effects

Sublimation products, primarily selenium, emitted from the T/E module area, may interact with non-module components. Due to the magnitude of selenium sublimation associated with the current 3M T/E design objective (i.e., 50 mg./couple over 50,000 hrs. service or a total source term of 16.8 gms. of selenium for the SIG system) concern has been evidenced regarding potential secondary effects of the sublimation products on degradation of system performance. Areas of concern include the following:

- Selenium interaction effects on electrical and instrumentation circuits.
- Selenium interaction effects on fibrous insulations.
- Selenium interaction effects on molybdenum multifoil insulation.

As described below, the projected effects of selenium sublimation on "non-module" system performance appear tolerable. However there is a definite incentive to reduce the selenium source term.

a. Selenium/Electrical and Instrumentation Circuit Interactions

All electrical or instrumentation circuits within the non-module section are protected by one or more layers of Varglass sleeving. Based on GDS experience, the Varglass sleeving appears to be a satisfactory protective barrier. No evidence of "electrical shorts" arising from selenium deposits was noted in GDS.

b. Selenium/Fibrous Insulation Interactions

Selenium/fibrous insulation (i.e., Min-K TE 1400 and 1800, Fiberfrax and CBCF-3) compatibility studies are lacking. Limited "high spot" studies by 3M and ORNL suggested no significant incompatibilities. Additional studies should be implemented.

However, in lieu of the tortuous path between the selenium source and the end fibrous insulations, selenium interactions, if any, should be mitigated. Moreover, it should be noted that GDS exhibited a stable thermal performance which is evidence for stability of the fibrous insulations.

c. Molybdenum Multifoil/Selenium Interaction

(1) Potential Concerns and Degradation Mechanisms

Experimental and analytic studies of molybdenum/selenium and molybdenum multifoil/selenium interactions were prompted by the observation of molybdenum corrosion by low pressure selenium vapor (several torr) vapor corrosion (MoSe_2 corrosion product) reported by J. E. Dutrizac (Ref. 2). Potential degradation modes which may be associated with selenium/molybdenum multifoil interactions include the following:

- (a) Thermal degradation of the multifoil due to surface reaction product, MoSe_2 , or condensates (i.e., liquid selenium, copper selenide) which lead to:
 - Increase in emissivity
 - Mechanical thermal shorts
- (b) Mechanical degradation of the multifoil due to:
 - Corrosion - material loss associated with formation of MoSe_2
 - Embrittlement of the molybdenum

(2) Molybdenum/Selenium Compatibility Screening Tests (ORNL)

The essential experimental features of the ORNL compatibility screening tests are as follows:

- Evacuated sealed tube.
- Molybdenum foil strip exposed to thermal gradient approximately 200 to 1050°C.
- Controlled selenium vapor pressure.
- Foil/selenium source weight ratio approximately 1.
- Approximately 350 hour exposure time.

Tests conducted at selenium vapor pressure levels of approximately 5×10^{-6} , 1×10^{-3} , 5×10^{-2} , 0.8 and 1 torr yielded the following observations (Ref. 3). At selenium vapor pressure levels of 1×10^{-3} torr and above the reactivity of molybdenum with selenium vapor, particularly within the nominal 400-900 °C temperature range was confirmed. The reaction product is MoSe_2 . Based on visual inspection and initial metallographic observations (Ref. 4) negligible reactivity was noted at the lowest selenium vapor pressure, approximately 5×10^{-6} torr.

The effective selenium vapor pressure in a generator spans the range approximately 5×10^{-6} torr to approximately 1×10^{-4} torr. Further long term tests within this vapor pressure range are necessary to better define the reactivity.

(3) Moly Multifoil Washer/Couple Compatibility Tests

Four molybdenum multifoil washer stack assemblies, as shown in Fig. IX-1, were fabricated by TECO and provided to 3M for integration into single couple tests. The test objectives include: (1) evaluation of lateral selenium migration thru the P-element wrap and couple Fiberfrax insulation as evidenced by interaction with the molybdenum foil and (2) evaluation of the nature of selenium reaction with the molybdenum foils. A single couple compatibility tests was initiated in late October, 1978. Test results are not yet available.

(4) A "Worse Case" Assessment of Multifoil Corrosion Effects

It is assumed that all selenium vapor contacting molybdenum foil in the temperature range of ~ 400 - 900 °C will react and form MoSe_2 . Thus, the extent of Se/multifoil reaction is controlled by the "selenium source term" and the selenium transport paths and integrated selenium vapor flux within the multifoil assemblies. Selenium transport paths are illustrated in Fig. IX-2 by a simplistic schematic model. The selenium

MOLY MULTIFOIL "WASHER STACKS" FOR 3M COUPLE COMPATIBILITY TESTS

FIG.IX-1. MULTIFOIL WASHES STOCK ASSEMBLIES

Se_T = TOTAL SELENIUM SOURCE TERM $0.05 \text{ gm} \times 336 = 16.8 \text{ gms.}$

Se_A = AMOUNT Se TRANSPORTED IN AXIAL DIRECTION FROM MODULE FACE
(i. e. TRANSPORTED TO MULTIFOIL)

Se_C = AMOUNT Se TRANSPORTED DIRECTLY TO MODULE COLD SIDE

$$Se_T = 4 Se_A + 2 Se_C$$

FIG.IX-2. SELENIUM TRANSPORT PATHS- SCHEMATIC

source term is predicated on the 3M T/E design objective, 50 mg. Se/couple over 50,000 hour life.

(a) Emissivity Changes and Multifoil Thermal Degradation

The formation of a MoSe_2 surface layer on the molybdenum foils will undoubtedly result in a marked increase in emissivity. The actual ϵ_T for a MoSe_2 surface layer remains to be quantified. However, thermal degradation was calculated for the following "worst case" assumption - emissivity of 50% of area of all foils in temperature regime 400-900°C (i.e. 40 layers) is 0.8. The associated system performance degradation is as follows:

$$\Delta Q = -38.4 \text{ W(t)}$$

$$\text{or } \Delta P = -4.3 \text{ W(e)}$$

(b) Selenium Corrosion - Mechanical and Thermal Degradation

Corrosion by selenium of the center multifoil section (c.f. Figs. IV-3 and IX-2) was calculated for the following conditions:

<u>2 Se_c* - gms</u>	<u>Total Se-2 Se₄* Attacking Center Multifoil - gms.</u>	<u>Axial Depth of Attack Zone - 2t</u>
0	8.4	Varied - 2.5, 5, 10, 20, 50 and 100% of axial length; attack restricted to 40 layers.
7.6	5.04	
11.68	3.36	

*c.f. Fig. IX-2.

Calculated EOM center section multifoil corrosion data (i.e. % thickness decrease of molybdenum foil) are summarized in Table IX-1. Inspection of these corrosion data indicate that in general negligible mechanical degradation (i.e. loss in base foil thickness) will be incurred. For the unique condition of very concentrated multifoil edge attack a

TABLE IX-1
CALCULATED SELENIUM VAPOR CORROSION OF
CENTER MULTIFOIL SECTION
% Thickness Decrease of Molybdenum Foil
in Specified Reaction Area

		% of Sublimed Se Available to Multifoils		
		<u>100%</u>	<u>60%</u>	<u>40%</u>
Reaction Area - % of Multifoil Area or (axial depth - in.)	100% (2.6")	3.6	2.16	1.44
	50% (1.3")	7.2	4.3	2.88
	20% (0.52")	18.0	10.8	7.2
	10% (0.26")	36.0	21.6	14.4
	5% (0.13")	72.0	43.2	28.8
	3.6% (.093")	100	60.0	40.0
	2.5% (0.065")	96.0 (144)	86.4	57.6

Assumptions:

Total available Se, $2 \text{ Se}_a = 8.4 \text{ gms.}$

Total number of wraps reacted 40 (i.e. typically span 400-900°C temp. range)

Foil thickness 0.0003"

Multifoil stack - center section 2.60" axial length x 60 wrap x 8.592 arc dia

Variables:

Percent of axial length of multifoil assembly over which uniform corrosion occurs.

Percent of sublimed.

possible loss of .046" (axial depth) is projected as a "worst case" condition. The calculated performance degradation associated with this total edge corrosion (i.e. .046" loss over 40 foils at four locations adjacent to modules) is as follows:

$$\Delta Q = -39.4 \text{ W(t)}$$

$$\Delta P = \sim -4.4 \text{ W(e)}$$

(c) Multifoil Insulation/Selenium Compatibility Conclusions

The currently projected magnitudes of system performance degradation associated with potential Mo/Se interactions appear tolerable. However a definite incentive exists to lower the selenium source term.

2. Heat Source Effluents

An assessment of the expected level of gas effluents emitted from the MHW Isotopic Heat Source and the impact of these gas loads on generator design features (i.e. getter sizing, conductance slots), generator fueling and outgassing procedures, and operational performance degradation was initiated in the fall of 1978. In addition, it was intended to address plausible methods for "treating" the heat source so as to reduce the gas source term at the time of generator fueling. These efforts were in process at the time of the "stop work order." Progress to that date is as follows.

The primary noxious gas effluent from a MJS heat source is carbon monoxide, CO. The mechanisms for CO evolution or formation include (Ref. 5):

- a. Outgassing of the heat source carbon/graphite materials.
- b. Fuel, PuO_2 , reduction due to a CO/ CO_2 cyclic.

Inspection of typical MJS/IHS storage and LAS gas tap data and GE monopole mass spectrometer data during LAS (RTG Loading and Assembly Station) RTG outgassing (Ref. 6), yielded the following preliminary estimate of a "worst case" CO source term during "integration and generator outgassing."

This source term is the sum of the following:

- a. CO generation for twelve hour period based on generation rate for storage conditioning - Typical Rate 3×10^{-3} gm/day or 1.5 mg. CO.
- b. CO mass flow during LAS outgassing based on change in CO partial pressure over 48 hour outgassing period and assuming an effective CO pumping speed of 2500 l/sec, where - Typical PP_{CO} change is 8×10^{-7} torr
 and $Q_{(Vol. CO)} = 8 \times 10^{-7} \text{ torr} \times 2.5 \times 10^3 \text{ l/sec} \times 48 \text{ hr.} \times 3600 \text{ sec/hr}$
 $= 345.6 \text{ torr-liters}$
 $= 0.454 \text{ liter-atm or } 0.568 \text{ gms. CO}$

Total CO Source Term = 0.5695 gms. CO or 0.3254 gm oxygen equivalent.

This preliminary estimate of CO gas load, made on somewhat naive assumptions, suggests the following:

- a. For this gas load, in-situ outgassing of a fueled generator would require an inordinately long processing time (see Section IX-C).
- b. Pre-fueling outgassing of the heat source (i. e. independent of generator outgassing) may be required if post fueling generator outgassing is to be accomplished with the generator cover in place.
- c. A study of alternate fueled generator outgassing configurations (i. e. temporary cover with minimal end insulation) is desired.

Based on MJS data and LASL CO/FSA analytic (Ref. 5) and experimental studies (Ref. 7), it appears that CO generation from the heat source will be tolerable, or manageable, during ground storage (gas cover) or during space (i. e. vacuum) operation. During ground storage carbon monoxide formation by the C/CO₂ cyclic process (i. e. CO/Fuel Sphere Assembly interaction) should be mitigated by the nominal one atmosphere xenon

gas cover which should impede CO/CO_2 gas transport through the PICS vent. Also, CO level if not adequately controlled by the hot zirconium getter, can be controlled by periodic gas exchange. Due to the dynamic vacuum associated with space operation, the "cyclic" mechanism for CO generation should not be operable.

Undoubtedly, further review of the so-called "outgassing source term" is necessary. Unfortunately, the organization of a "discussion-review" meeting on the above topics with representatives from LASL, MRC and BCL which was initiated in December 1978 was deferred until resolution of the SIG program and hence did not come to fruition.

3. Thermodynamic Studies of Potential Cyclic Reactions

Thermodynamic analysis of the GDS-1 materials system conducted by General Atomic Co. (Ref. 8) revealed two potential cyclic reactions involving the possible reduction of the titania, TiO_2 , opacifier in the Min-K TE 1400 and 1800 insulations. The reactions, as depicted in Fig. IX-3, involve the following constituents:

- a. $\text{C}/\text{CO}_2/\text{CO}/\text{TiO}_{2-x}$
- b. $\text{Zr (hot getter)}/\text{H}_2\text{O}/\text{H}_2/\text{TiO}_{2-x}$

It is believed that these reactions will not be effective in the SIG/Galileo flight system for the following reasons. For the case of steady state space operation, both reactions will be inhibited by the dynamic vacuum operational environment. For the case of ground or launch operation under a nominal one atmosphere cover gas (probably xenon), reaction (a) is inhibited by the hot zirconium getter, and reaction (b) is inhibited by the cold zirconium getter (i. e. hydrogen trapping).

The potential impact of the CO/PuO_2 (GIS) cycle as demonstrated by analytical and experimental studies at LASL (Ref. 5) are discussed in Section IX.A.2, Heat Source Effluents.

FIG.IX-3. POTENTIAL CYCLE REACTIONS WITH MIN-K TITANIA OPACIFIER (FROM REF.8)

4. Carbon/Min-K and Fiberfrax Insulations Compatibility

Studies of the carbon/Min-K TE 1400, carbon/Min-K TE 1800 and carbon/Fiberfrax Hi Fi 660H paper reactions were conducted at Bettelle Columbus Laboratories using Knudsen Cell mass spectrometric techniques for the detection of possible volatile reaction product species. Results of these studies, (Ref. 9) yielded the following observations:

- a. No indication of a volatile silicon monoxide, SiO, reaction product.
- b. A moderate level of CO₂ and/or CO formation.

The CO₂ and/or CO evolution rates appear to be inordinately high, based on gas analysis data from GDS-1. It is postulated that the observed CO/CO₂ formation is primarily associated with the reaction of oxygen or oxygen containing outgassing products from the insulation with the carbon reactant or Knudsen cell. Further data (i. e. long term isothermal thermogravimetric measurements of C/insulation couples employing pre-outgassed materials) is required to assess a realistic CO/CO₂ source term for these interface reactions.

5. Trace Impurities and Volatile Species

Impurity levels in outgassed non-module insulations and graphites are acceptable, as adequately documented by work at ORNL, GA and BCL (Refs. 8 and 9).

Knudsen cell mass spectrometer studies of systems containing Min-K TE 1400 and Min-K TE 1800 at General Atomic (Ref. 8) and Battelle Columbus Laboratories (Ref. 9) indicated a moderate vapor pressure, approximately 10^{-6} torr, of potassium and in some cases sodium at temperatures circa 850-900°C. This observation raises a "flag"; however, as yet no degradation mechanism associated with these species has been identified.

B. COMPONENT OUTGASSING STUDIES

The outgassing procedures selected for the SIG Galileo flight system piece parts were patterned after the following procedures developed for the GDS-1 system: SIG 30060 for carbon, graphitic and boron nitride type materials and SIG 30064 for oxide type insulation (thermal and electrical) materials. These updated procedures, SIG 110077 and SIG 110076, had been prepared as draft copies and were in the review and approval cycle at the time of the stop work order. The essential features of these primary outgassing procedures are described below. The efficacy of these procedures was demonstrated by the outgassing behavior observed for the GDS-1 generator and the high level of cover gas purity observed during the operation. The adequacy of the noted outgassing parameters was confirmed by selected laboratory primary outgassing studies conducted on the following:

Min-K TE 1400	General Atomic (Ref. 8)
Min-K TE 1800	General Atomic (Ref. 8)
CBCF-3	ORNL and Battelle CL (Ref. 9)
EBP Graphite	Battelle (Ref. 9)

With the exception of limited CBCF-3 studies, no additional work was conducted on secondary outgassing rates or source terms. The data base on the secondary outgassing behavior of SIG materials remains minimal.

1. Oxide Type Insulations

The outgassing parameters selected for oxide type insulations (thermal and electrical) are summarized in Table IX-2.

Two changes were made relative to GDS procedures: an air bakeout, as discussed in Section IX.B.3, was added to the Varglass sleeving process, and the Fiberfrax insulation material were added.

TABLE IX-2

TYPE I AND TYPE II VACUUM OUTGASSING PROCESS PARAMETERS

Material	Heating Rate Maximum °C/hr ⁽¹⁾	HOLD 1			HOLD 2		
		Temp. °C	Time hrs.	Press. ⁽¹⁾ torr	Temp. °C	Time hrs.	Press. ⁽²⁾ torr
Min-K TE 1400 ⁽⁴⁾	100	400 ± 10	8.0 ± 0.5	1 x 10 ⁻⁵	870 ± 10	168 ± 8	1 x 10 ⁻⁶
Min-K TE 1800 ⁽⁴⁾	100	400 ± 10	8.0 ± 0.5	1 x 10 ⁻⁵	910 ± 10	168 ± 8	1 x 10 ⁻⁶
Zircar ZYF-B3 ⁽⁴⁾	100	400 ± 10	8.0 ± 0.5	1 x 10 ⁻⁵	1150 ± 10	168 ± 8	1 x 10 ⁻⁶
Zircar Felt ⁽⁴⁾	100	400 ± 10	8.0 ± 0.5	1 x 10 ⁻⁵	1150 ± 10	168 ± 8	1 x 10 ⁻⁶
Fiberglass Sleeving ⁽⁵⁾ Varglass Non- Fray SI600 Typ. H	200	-----N/A-----			740 ± 10	6 ± 0.5	1 x 10 ⁻⁶
Alumina Insulators ⁽⁵⁾ 99.0% Al ₂ O ₃	200	-----N/A-----			1300 ± 10	6 ± 0.5	1 x 10 ⁻⁶
Fiberfrax HiFi 660 AH ⁽⁴⁾ Paper and Fiberfrax H Blanket	200	-----N/A-----			1000 ± 10	64 ± 4	1 x 10 ⁻⁶

(1) Maximum desired pressure limit during heating is 1 x 10⁻³ torr. If exceeded, hold isothermally until pressure falls below limit before resuming heating cycle.

(2) Indicated maximum pressure must be attained at end of indicated hold time. If condition is not met, hold at indicated temperature until condition is met.

(3) Indicated maximum pressure must be sustained over the final 3 hrs or 25% of hold time, whichever is greater. If above condition is not met within the indicated time frame extend the holding time until this condition is met. This maximum pressure also must be maintained throughout the cooling cycle.

(4) Outgassing is preceded by air bakeout as follows: heat at 400°C at maximum rate of 200°C/hr., hold at 400°C for 8 ± 1/2 hr., heat to 800°C at maximum rate of 200°C/hr., hold at 800°C for 16 ± 1/2 hrs.; cool in furnace at a maximum rate of 200°C/hr to 150°C or below. Remove from furnace, air cool to ambient, package in argon filled polyethylene sealed bag or transfer to outgassing glove box within 2 hrs.

(5) Outgassing is preceded by air bakeout as follows: heat to 675 ± 10°C at a maximum rate of 300°C/hr., hold at temperature for 1 ± 0.1 hr., furnace cool to ambient.

NOTE: Type I outgassing requires INERT GAS HANDLING of the outgassed product.

2. Carbon and Graphite Type Materials

The outgassing parameters selected for carbon and graphite type materials are summarized in Table IX-3. No significant changes from the GDS practices were made.

3. Outgassing Optimization for Varglass Fiberglass Sleeving

Three problems have been observed in the outgassing, use and behavior of "out-gassed" Varglass S1600H fiberglass sleeving. The problems and respective solutions or assessments are:

- a. Low electrical resistance of some "vacuum outgassed sleeving lots" - corrected by introduction of an air bakeout prior to outgassing.
- b. Embrittlement of an occasional coil (lot) of sleeving - a vendor material quality control problem not impacting generator performance.
- c. Surface particulate contaminants observed to varying degrees in all sleeving lots which were examined - not an immediate problem. Requires further data for impact assessment.

Results of pertinent investigations, or assessments and selected solutions of these problems are described below.

a. Low Electrical Resistance of Vacuum Outgassed Sleeving

This problem was first noted during the assembly of GDS-1. A low resistance, typically $220\text{ K}\Omega$, was measured between the heater power circuit and the housing. A check of the electrical resistance of the Varglass S1600H sleeving employed on the heater leads (measurements made in the assembly glove box using a Simpson multimeter and clip on leads space approximately one inch apart) yielded resistance values in the kilo-ohm range versus an expected level of several hundred gig-ohms. Further resistance measurements on other outgassed coils showed that each lot (coil of a given sleeving size) exhibited

TABLE IX-3

TYPE I⁽³⁾ AND TYPE II⁽⁴⁾ VACUUM OUTGASSING PROCESS PARAMETERS⁽⁵⁾
FOR CARBON, GRAPHITE AND BORON NITRIDE MATERIALS

Material	Heating Rate Max., °C/hr ⁽¹⁾	HOLD		
		Temperature °C	Time hrs.	Pressure ⁽²⁾ torr
CBCF-3 or CBCF-4 (ORNL insulation)	350	1350 ± 15	64 ± 4.0	5 x 10 ⁻⁶
POCO AXF-Q1 or AXF-5Q	350	1500 ± 25	64 ± 4.0	5 x 10 ⁻⁶
EBP Graphite	350	1500 ± 25	64 ± 4.0	5 x 10 ⁻⁶
ATJ Graphite	350	1500 ± 25	64 ± 4.0	5 x 10 ⁻⁶
Pyrolytic Graphite	350	1500 ± 25	64 ± 4.0	5 x 10 ⁻⁶
UF-4S Graphite	350	1500 ± 25	64 ± 4.0	5 x 10 ⁻⁶
Grafoil	350	1500 ± 25	64 ± 4.0	5 x 10 ⁻⁶
Graphite Felt (Type WDF)	350	1500 ± 25	64 ± 4.0	5 x 10 ⁻⁶
Boron Nitride Hot Pressured Grade	350	1250 ± 15	16 ± 1.0	5 x 10 ⁻⁶
Boron Nitride Felt	350	1250 ± 15	16 ± 1.0	5 x 10 ⁻⁶

(1) Maximum desired pressure limit during heat is 1×10^{-3} torr. If this pressure is exceeded hold isothermally until pressure falls below this limit before resuming heating cycle.

(2) Indicated maximum pressure must be sustained over the final 25% of indicated hold time. If above condition is not met within the indicated time frame extend the holding time until this condition is met. This maximum pressure must also be maintained throughout the cooling cycle.

(3) Type I outgassing requires INERT GAS HANDLING.

(4) Type II outgassing does not require inert gas handling; exposure of outgassed details to ambient air is permissible.

(5) A maximum of two (2) thermal interruptions in the noted thermal cycle (i. e. cooldown due to furnace/utilities malfunction) are permissible provided the cited pressure restrictions are maintained and there is no malfunction of the vacuum system. Cycle time measurement is resumed after recovery of temperature.

a characteristic average resistance, be it high or low. A significant number of the coils, approximately 60%, showed abnormally low average electrical resistance, kilo-ohm range. All GDS sleeving was vacuum outgassed per SIG 30064 (i. e. $740^{\circ}\text{C} \pm 10^{\circ}\text{C}$ for 6 ± 0.5 hours or as required to achieve an ultimate pressure of $\leq 1 \times 10^{-6}$ torr; all post-outgassing handling, storage and assembly was conducted under a high purity argon gas atmosphere).

Based on the results of air bakeout studies, on both virgin as-received and GDS vacuum outgassed sleeving, as shown in Table IX-4, it is concluded that the introduction of a $1250^{\circ} \pm 20^{\circ}\text{F}$ ($675 \pm 10^{\circ}\text{C}$) one hour air bakeout prior to vacuum outgassing will consistently yield a high (i. e. gig-ohm range) resistance sleeving. This change was adopted in the appropriate outgassing procedure. The above noted responses to air-bakeout temperatures suggest that the virgin Varglass S1600H fiberglass sleeving is contaminated, each coil to varying degree, with a carbonaceous probably organic material.

b. Embrittlement of Varglass S1600 Type H Natural Fiberglass Sleeving

During the vacuum outgassing of the S1600H fiberglass sleeving, either with (c.f. Table IX-4) or without (i. e. GDS material processing) a prior air bakeout, random coils were observed to completely embrittle. This behavior is believed to be a vendor quality problem with the basic glass fiber composition or incorrect labeling or loss of fiber traceability. This problem remains to be pursued with the vendor. The embrittlement is believed to be associated with "vitrification" of the glass fibers. It should be noted that the S1600H grade is rated by the vendor for 1600°F (870°C) service and the outgassing temperature at which the occasional embrittlement occurred is 1364°F (740°C).

c. Surface Particulate Contaminants

In the course of the search for the cause of the above cited "low electrical

TABLE IX-4

EFFECT OF AIR BAKEOUT ON THE ELECTRICAL RESISTANCE OF
S 1600H VARGLOSS SLEEVING INSULATION

	Sleeving Size	Prior to Air Bakeout	Sleeving Resistance ^{(1) (2)} Air Bakeout Temperature Indicated		
			750 °F	1000 °F	1250 °F
As-Received Virgin Material	1" I. D.	250 ⁺⁵⁰ ₋₄₀ GΩ	4.7 ^{+1.3} _{-1.0} GΩ	4.2 ^{+2.5} _{-1.2} GΩ	154 ⁺⁵⁶ ₋₃₄ GΩ
	1/2" I. D.	266 ⁺⁸⁴ ₋₆₆ GΩ	200 ^{+5.4} _{-2.6} GΩ	34.6 ^{+5.4} _{-2.6} GΩ	286 ^{+14*} ₋₃₆ GΩ
	#20	266 ⁺¹⁰⁸ ₋₆₂ GΩ	50 ⁺¹⁵ ₋₂₀ GΩ	48.5 ^{+6.5} _{-3.5} GΩ	240 ⁺⁶⁰ ₋₄₀ GΩ
Tool Resistance ⁽³⁾ for above readings		400 GΩ	200 GΩ	170 GΩ	300 GΩ
GDS Vacuum Outgassed Material	#3	47.5 ^{+142.5} _{-27.5} MΩ	110 ± 10 GΩ	53.6 ^{+16.4} _{-10.6} GΩ	300 GΩ *
	#7	227.8 ^{+502.2} _{-196.8} KΩ	82 ⁺³ ₋₂ GΩ	83.7 ^{+46.3} _{-18.7} GΩ	300 GΩ *
	#14	391 ⁺²⁸⁹ ₋₁₈₁ KΩ	107 ⁺²³ ₋₂₂ GΩ	93.0 ⁺⁷ ₋₈ GΩ	300 GΩ *
	#20	40.7 ^{+159.3} _{-40.25} GΩ	196 ⁺⁴ ₋₁₆ GΩ	152 ⁺⁴⁸ ₋₄₂ GΩ	300 GΩ *
Tool Resistance for above readings		400 GΩ	200 GΩ	170 GΩ	300 GΩ

(1) Resistance measurements made using a megohm bridge and 50 VDC test voltage. Five measurements were made over 1" length of sleeving at five different random locations for each processing condition.

(2) Tool resistance measurements were made at 50 volts 26 to 38% relative humidity.

(3) All sleeving samples from each lot were completely embrittled after vacuum outgassing.

*Measurements conducted after outgassing at MRC were limited by "stop work order" but confirmed the high resistance of the outgassed product.

resistance" GDS Varglass S1600H sleeving, several samples of the outgassed GDS sleeving were submitted for examination by scanning electron microscope/energy dispersive x-ray probe (i. e. SEM/EDAX) analysis. Results of these and subsequent examinations of newly procured "virgin" lots of sleeving (Refs. 10 and 11) showed varying degrees of foreign particle contaminants on the surface of the fiber. Chemical elemental constituents of various particles (including indications from the fiber substrate) include the following:

(1) From GDS sleeving -

Ti, Mg, Al, Si

Fe, Ca, Cl, K, Ti, Cl and Cu

K, Fe, Cl, Ti, Ca, S, Cy

P, Ti, Fe, Ca, K, Cu

(2) From "virgin" sleeving -

Si, Ca, Al, Ti

Si, Ca, Al, Ti, S, Cl, K, Cr

S, Ti

Cl, Ti, Cr

The apparent "dirtiness" of the GDS sleeving is not surprising since the "in-process material" was in the close proximity of Min-K insulations and other generator materials throughout the assembly process. Further data on the contaminant particulates observed on "virgin" sleeving is necessary to fully assess their significance, if any, on system compatibility. It appears unlikely that the foreign particulates significantly influence the electrical resistance of the sleeving.

C. POWER SECTION OUTGASSING STUDY

One step in the manufacture of the SIG is its outgassing. This step is performed primarily to minimize the degradation of the thermoelectrics (T/E's) and other components due to their reacting with impurities (water, oxygen, hydrogen, etc.) that may be trapped in the generator. Outgassing is accomplished by pumping out the impurities while slowly heating the generator with an electrical heat source to slightly above its operational temperature. Two exhaust ports are provided in the generator for outgassing, one in each end cover. During outgassing the total pressure is measured continuously and partial pressures, as a function of mass number, are measured periodically by a partial pressure analyzer (PPA) located approximately 33 inches downstream from one of the exit ports. Additional protection of the components from the impurities is provided by five zirconium foils (getters) located within the generator whose sole purpose is to react with H_2 and O_2 before these impurities can react with the other components.

Although several SIG components are vulnerable to reactions with impurities, the TPM-217 P-leg and $GdSe_x$ N-leg of the T/E's are of the most concern since, in addition to known reactions with several impurities (see Ref. 12), their degradation directly affects the SIG's power output. Accordingly, the criterion for outgassing of the generator is dictated by the 3M Co. who manufacture the T/E's. The criterion, established originally for the GDS, and considered applicable for the flight generator, is that the sum of the partial pressures of all gaseous species that may be harmful to the T/E's ($\sum P_h$) should not exceed 5×10^{-5} torr at the T/E's. (By previous standards this is an unusually low outgassing pressure.) The apparent reason for selecting this criterion is that past studies conducted at 3M have shown that T/E degradation due to oxidation to be negligible if this criterion is satisfied. It does not mean that higher outgassing pressures would necessarily

result in significant T/E degradation. A knowledge of the reaction kinetics associated with their oxidation would be necessary to ascertain if the T/E degradation is, in fact, minimized with a minimized outgassing pressure. Unfortunately, the advantage of outgassing at low pressure remains a moot point since reaction kinetic data are not available for the oxidation of the thermoelectrics (Ref. 13).

Consider now the effects of outgassing pressure on the degradation of the carbon and graphite components. For these components a high outgassing pressure is definitely preferred. A case in point is the oxidation of POCO AXF-Q1 graphite which was determined experimentally to be of order $(n) = 0.7$, i. e. the oxidation rate varied as $P_{O_2}^{0.7}$ (Ref. 14). In this case, as in all cases where $n < 1.0$, the total oxidation would be minimized by maximizing the pressure level during outgassing. ⁽¹⁾

While impurities can be expected to exist in all regions of the generator, it is known that most impurities are trapped within the low density fibrous insulations (Min-K 1400, Min-K 1800, CBCF-3 and Fiberfrax). For the sake of simplicity, it is assumed that all impurities within the generator are distributed uniformly within these insulations. It is further assumed that the impurities contained within the insulations will flow to the nearest slot at a rate sufficient to satisfy the demand of the pump. Intrapore conductances of the insulation materials are not available, but the results of the GDS outgassing indicate this assumption to be valid.

Several changes in the basic design of the SIG have occurred since the GDS was built that make outgassing problems slightly different between it and the GDS. Probably

⁽¹⁾ The reason for this statement is apparent from the following relationships. Total oxidation = oxidation rate x outgassing time

$$\begin{aligned} &= C_1 P^n \times C_2 (1/P) \\ &= C_3 P^n - 1 \end{aligned} \tag{1}$$

where the C's are constants and the temperature is fixed.

the most significant change was the introduction of the multifoil side insulation which resulted in replacing fibrous insulation with the multifoil and relatively large annular chambers on either side of the multifoil. Other changes include the elimination of the IHF, Zirconia and CBCF-4 insulations and the use of fewer outgassing ports. (There are 3 outgassing ports in the GDS vs. 2 in the flight generator.)

One concern with the outgassing of the flight generator is how impurities that are inside the inner annular chamber will be removed since there are no intentional passageways between it and the outgassing port. It is anticipated that outgassing of this volume and the heat source, which is adjacent to the inner chamber, will occur through minute gaps in the insulation that probably cannot be avoided in the design. In addition, the insulations themselves are porous and will pass gases although their effectiveness as passageways can only be crudely estimated. Just in case the gaps and pores are not as effective as anticipated, 2 - 0.10" dia. holes are drilled through each end insulation to provide additional passageways between the inner chamber and the exit port. As will be shown later, these holes will not be capable of solely providing sufficient conductance to outgas the generator in 21 days, but rather the holes will act as a "safety valve" in the event the other passageways are less effective than anticipated.

A comparison of outgassing characteristics that were used in predicting the SIG outgassing time requirement are presented in Table IX-5.

The approach taken in this study is to compute an effective conductance of the generator for a particular slot/hole configuration. This was accomplished with the aid of the Thermal Analyzer Program, TAP-3 (Ref. 15), by treating the gas flow problem similar to a steady state thermal problem since an analogy relates the two phenomena. In this analogy pressure corresponds to temperature, gas through put corresponds to

TABLE IX-5
COMPARISON OF FLIGHT GENERATOR (ETG) AND
GDS OUTGASSING CHARACTERISTICS

<u>Item</u>	<u>Flight Generator</u>	<u>GDS Converter Compartment</u>
Volume of fibrous insulation (in. ³)	364	287
Outgassing time (days)	21 (maximum allowed)	7.7*
ΣP_h (torr)		
- at T/E's	5×10^{-5}	5×10^{-5}
- at PPA	(see results)	2.78×10^{-5}
Source term, Q (torr-liters)	203.6***	160.5**

*This study assumes the GDS outgassing time to be 11 days rather than the 7.7 days of actual outgassing. This increase is to compensate for: (1) the maximum allowable level of ΣP_h being exceeded in the early stages of outgassing, (2) the operational temperature of the GDS never being reached, let alone exceeded, during outgassing, and (3) P_{CO} not being included in the computation of ΣP_h .

**Computed from GDS outgassing data based on an 11 day GDS outgassing period. Prior to the GDS outgassing, a value of 140.98 torr-liters was predicted based on an estimated impurity mass of 1.7 mg/couple having a mass number of 32.

***Computed based on source terms being proportional to corresponding volumes of fibrous insulation.

heat generation and conductance corresponds to admittance. The conductance of the entire outgassing system (generator, piping and pump) is determined by treating the generator's conductance as being in series with the conductance of the other components. After the computer results are available, the effective conductance of the generator (F_{gen}) is calculated by the following equation.

$$F_{\text{gen}} \text{ (liter/sec)} = \frac{\Sigma Q}{(P_{\text{max}} - P_{\text{port}})} \quad (2)$$

where: P_{max} = highest pressure computed within generator (torr)
 P_{port} = pressure computed at outgassing port (torr)
 ΣQ = sum of all gas through puts (100 torr-liter/sec).

The pumping speed (S) is then calculated by the following equation which represents the 3 units in series.

$$\frac{1}{S \text{ (liter/sec)}} = \frac{1}{F_{\text{gen}}} + \frac{1}{1/2 F_{\text{pipe}}} + \frac{1}{1/2 F_{\text{pump}}} \quad (3)$$

where: F_{gen} , F_{pipe} and F_{pump} are the conductances of the generator, piping, and pump, respectively. The 1/2 coefficients are consistent with the 1/2 generator model.

The pressure ratio ($P_{\text{T/E}}/P_{\text{PPA}}$) is computed by the following equation.

$$\frac{P_{\text{T/E}}}{P_{\text{PPA}}} = \left[1 + \frac{\Sigma Q}{(P_{\text{T/E}} - P_{\text{port}})} + \frac{\Sigma Q}{(P_{\text{port}} - P_{\text{PPA}})} \right] \quad (4)$$

The time required to outgas (t) is computed by the following equation.

$$t \text{ (sec)} = \frac{\tilde{Q}}{(S) P_{\text{PPA}}} \quad (5)$$

where: $\tilde{Q}$ = source term (torr-liter).

The model used for this study is shown in Fig. IX-4. For the sake of simplicity it represents only half of the generator since the internal construction of the generator is approximately symmetric about its midplane. Figure IX-4 shows the location of slots, holes, assumed gaps and the pipe line between the generator and PPA. The conductances of individual segments of the outgassing network were obtained from Ref. 16 which presents conductance equations for air at 300 °K when passing through several types of configurations.

Gas is assumed to be generated at those elements that represent fibrous insulation. Each piece of insulation is assumed to generate gas in proportion to its volume, with a total model (1/2 generator) gas generation rate arbitrarily set at 100 torr-liter/sec. Each element is assumed to generate gas in proportion to the surface area of the insulation it represents.

After several tries, the slot/hole configuration shown in Fig. IX-5 was selected as being capable of providing a sufficiently small outgassing time. It calls for one large slot between the exit port and the outer chamber in addition to numerous 1/4" x 1/4" slots on the outer face and OD of the end insulation composite. The model assumes a 0.002" wide gap at the ends of the multifoil insulations. (These gaps connect element 27 to elements 22 and 26.) Also, the 0.010" wide gap between corner and end insulation pieces is assumed to be continuous even though it is actually blocked by a Fiberfrax ring. (This connects element 27 with elements 1, 18, 19 and 20.) A safety valve is provided in the form of two 0.100" dia. holes that connect element 27 to element 1.

Results are presented for three cases in Table IX-6. The first case represents the recommended configuration, including the assumed passageways and drilled holes to the inner chamber. The second case eliminates the drilled holes while keeping the assumed

FIG. IX-4. FLOW MODEL

FIG. IX-5. RECOMMENDED SLOTS AND HOLES FOR OUTGASSING

TABLE IX-6
SUMMARY OF RESULTS

Case	Holes Per End	Gap Thickness (in.)		Effective Conductance (liter/sec)				$\frac{P_{T/E}}{P_{PPA}}$	Maximum P_{PPA}^{***} (torr)	Outgassing Time (days)	
		-At end of Multifoil	-Between End and Corner Ins	Generator	System*	Line to PPA	Port to T/E's			Heat-up	Total**
1	2	0.002	0.010	4.958	3.020	9.39	12.53	1.56	3.2×10^{-5}	12.2	17.2
2	0	0.002	0.010	4.500	2.844	9.38	12.33	1.53	3.2×10^{-5}	12.9	17.9
3	2	0	0	0.6954	0.6379	9.39	13.80	1.11	4.5×10^{-5}	41.0	46.0

*Assumes the conductance of the pump = 450 liters/sec and the conductance of the piping = 16 liters/sec, half of each of these values are used to be consistent with the 1/2 generator model.

**Includes 4 days for cold outgassing and leak checks and one day for setup and tear-down.

***Based on maximum $P_{T/E} = 5 \times 10^{-5}$ torr.

passageways. The third case eliminates the assumed passageways while keeping the drilled holes. It is apparent from Case 3 that the drilled holes will not provide sufficient conduction to realize an outgassing time less than 21 days. On the other hand, as noted previously, the assumptions regarding passageways to the inner chamber may be unservative.

The heat lost due to the slots in the end insulation was computed by hand. This was accomplished by assuming several Min-K 1400/vacuum interface temperatures for a 0.25" gap between the Min-K 1400 insulation and end cover. Heat conduction through the insulation and radiation across the gap were then calculated for each assumed temperature, considering the temperatures of the end cover and Min-K 1400/CBCF-3 to be unchanged by the gaps. A crossplot of the results yielded an interface temperature for which the radiative and conductive heat fluxes were equal. A heat flux increase due to slots of 20 Btu/ft^2 was computed as being equal to the difference in fluxes between a gap and gapless configuration. (The heat flux increase due to slots in the corner insulation was assumed to be the same as in the end insulation.) The total heat lost due to the slots is the product of slot area ($.296 \text{ ft}^2$) and the $20 \text{ Btu/ft}^2 \text{ hr}$ heat flux increase. For the proposed slot configuration, this loss is computed to be 1.734 watts(t)/generator.

The heat loss through a 0.100" dia. hole was computed for two different cases. The first case was to assume no heat transfer to the walls of the hole. For this case solutions were obtained with the aid of the IRLIMP Program (Ref. 17). The second case allowed heat to flow into the surrounding insulation and hence to the end cover. Here solutions were obtained with the aid of the TAP-3 Program. Hot and cold end temperatures in both these calculations were fixed at 964°C (1768°F) and 119°C (247°F), respectively. The second case yielded the greater heat loss (0.1247 vs. 0.1032 watts/hole or 0.4988 watts/generator).

Combining the slot heat loss with the larger of the hole losses yields a total heat loss of 2.23 watts(t)/generator.

As noted in Table IX-6, maximum pressures at the PPA are only slightly lower than at the T/E's, indicating a relatively high conductance between the two. The controlling factor of outgassing is the conductance between the inner chamber (element 27) and the rest of the system. This is apparent from Fig. IX-6 where it is seen that P_{27} is the highest pressure in the system.

D. GETTER DESIGN - MATERIAL SELECTION AND SIZING

The primary function of the getter system is to provide an added "sink" for noxious system effluent gases, H_2O , H_2 , CO and CO_2 , during the following generator assembly and operational stages:

- a. Generator outgassing
- b. Terrestrial and launch operation under a static vacuum and/or gas cover
- c. Space operation under dynamic vacuum (i.e. vented to space).

The getter function is illustrated schematically in Fig. IX-7.

The getter system, as shown in Fig. IV-3, is comprised of two subsystems, a "hot" and a "cold" getter. The hot getter provides a sink for O_2 , CO and CO_2 and also the oxygen contained in water. The cold getters provide a sink for H_2 , the product of water vapor reacting with reducing agents (i.e. Zirconium hot getter, carbon) within the system.

1. Getter Material Selection

Two candidate getter materials were considered:

- a. Pure Zirconium (reactor grade) -
- b. Zr-12.5 at % Ti alloy

FIG. IX-6. PRESSURE DISTRIBUTION (TORR) FOR RECOMMENDED CONFIGURATION
 $\sum Q = 100$ TORR - LITER/SEC.

FIG.IX-7. GETTER FUNCTIONS- SCHEMATIC ILLUSTRATION

Both materials can be produced in a foil form. The Zr-Ti alloy offers a superior "gettering rate," particularly for the cold side getter (i.e. gettering hydrogen). However, meaningful quantitative data on respective gettering rates for these materials in a SIG generator environment are not available. Reactor grade zirconium was selected for the SIG/Galileo generator application, primarily on the basis of its commercial availability and programmatic schedule restraints. Consideration of the more active Zr-Al alloy intermetallic powder type getter materials was rejected because of the system integration problems arising from the fine powder form of these materials.

2. Getter System Configuration

Configuration and design features of the getter system are summarized in Table IX-7 and illustrated in Fig. IV-3. The getter elements are mechanically associated with the multifoil insulation packages. The "hot" getters are "dimpled" cylindrical sections of 0.007" reactor grade zirconium foil which are mechanically attached to the inner diameter (i.e. hot side) of each of the three multifoil segments. The two "cold" getters are comprised of approximately one wrap of 0.003" thick reactor grade zirconium foil which are sandwiched within the two end multifoil stacks with nominal five mil spacers. The location within the "stack" is selected to yield the desired steady state operating temperature, 375-450°C. Each layer of the sixty layer molybdenum multifoil stack contains six randomly space 1/4" diameter holes in order to slightly enhance gas transport thru the multifoil assembly.

3. Getter Sizing

Getter sizing was initially based on a scale-up of the getter weights employed in GDS-1 (Ref. 18). It was assumed that the gas load or "source term" for the SIG/Galileo RTG would be approximately 2 times that of GDS-1. This rationale was based on the

TABLE IX-7
GETTER SYSTEM DESIGN FEATURES

	<u>Hot Getter</u>	<u>Cold Getter</u>
Size typical	.007" x 3.57" wide x 8.28" diam. (2 places) Plus .007" x 2.60" x 8.28" diam.	.003" x 3.57" wide x 8.68" diam. (2 places)
Weight typical	185 gms.	60 gms.
Total surface area-typical	500 in. ²	385 in. ²
Location	Hot side or I. D. of each multifoil assembly	Sandwiched within two end multifoil stacks with 5 mil spacers
Operational temperature - typical	950-1025 °C	375-450 °C

following approximations: (1) converter section gas loads will be approximately 0.8 X GDS TE section source term (i. e. oxygen equivalent) due to the introduction of multifoil insulation, (2) the source term associated with the MHW/IHS will be approximately 1.2 X GDS TE section source term. This approach was necessitated by the early design stage at which the getter design definition was required. Also it should be noted that the GDS-1 getter design included a safety factor of $\geq 7X$ with respect to getter mass.

A preliminary evaluation of the Galileo getter system based on updated estimates of secondary outgassing source terms indicate that the following criterion employed for GDS-1 are generally met or exceeded.

- a. Hot getter effective surface area active surface area of N-elements.
- b. Total hot getter capacity should exceed the anticipated outgassing term by a factor of 7X or more.
- c. At EOM the average oxygen and carbon content of the hot getter should not exceed $ZrO_{0.2}$ and $ZrC_{0.15}$ in order to minimize embrittlement.
- d. Total cold getter capacity should exceed the H_2 source term by a factor of 7X or more.
- e. At EOM the average hydrogen content of the cold getter shall not exceed $ZrH_{0.3}$ in order to minimize embrittlement.

E. REFERENCES

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X. RELIABILITY

The reliability program for the Selenide Isotope Generator for the Galileo Mission is formulated in general accordance with the procedures specified in DOE Reliability Engineering Program Requirements Publication SNS-2 dated June 17, 1974. The specific tasks to be accomplished by TES in the area of reliability during design, development, fabrication and testing of ETG, RTG's and GSE is defined in the SIG Reliability Program Plan, TES-2865-06, dated October 1978. The reliability program plan is approved by DOE in accordance with the contract requirements.

The Reliability Program Plan is organized into four major sections identified as management, design analysis, testing evaluation and reliability assessment. Results of studies and analyses prepared in compliance with the plan and deemed of interest to the government and technical community are included in the SIG Monthly Technical Progress Reports.

XI. SAFETY

The safety analyses and assessments (Task 11.0) relate to all aspects of fuel handling at the Contractor's facility, shipment of fueled RTG's, and potential accident situations for all phases of the Galileo Mission from launch through nominal earth escape conditions. In the time span covered by this report, effort was directed only to the launch and reentry safety aspects of the RTG system, specifically the containment capability of the heat source. Since significant analyses and testing had been performed on the generic Multi Hundred Watt (MHW) heat source in support of previous flight programs, The SIG/GM safety work was limited to the new environment and response thereto resulting from the use of the Space Transportation System (STS), the mission profile, and the Improved Multi Hundred Watt (IMHW) Isotopic Heat Source (IHS). A test matrix was deemed desirable to enhance the knowledge of the capability of the IHS response to various launch and reentry accident environment was prepared and is given in the "Safety Test Plan, SIG/GM," TES-33009-33, January 31, 1979. The safety analyses are summarized in the "Preliminary Safety Analysis Report," TES-33009-42, June 1979. The report comprises three volumes (in a single binding). Vol. I - Reference Design Document (RDD), Vol. II - Accident Model Document (AMD), and Vol. III - Nuclear Risk Assessment Document (NRAD). Volume I characterizes the mission, nuclear system, launch vehicle and launch site. Volume II describes the potential accidents and abort conditions, resulting environments and the response of the nuclear system to these environments. Volume III examines the transport mechanics of the fuel and the fuel response consequences.

APPENDIX A

LIST OF DRAWINGS AND PROCEDURES

<u>Drawing No.</u>	<u>Revision Level</u>	<u>Title</u>
SIG108000	A	Glove Box Line Assembly
SIG108001	F	Glove Box Modification
SIG108006	-	Track Installation - Sub Hoist and Cover Removal Carriage
SIG110015	-	Auxiliary Cooling Tube Installation Procedure
SIG110017	A	Welded Assembly - Transition Fitting to Receptacle
SIG110018	E	Cooling Tubes - Auxiliary, Generator
SIG110021	A	SIG Leak Check Procedure Generator Housing, Power Section
SIG110024	-	Clip and Spacer - Cooling Tube Support
SIG110026	-	Receptacle - Electrical
SIG110027	-	Electrical Receptacle Acceptance Test Procedure
SIG110029	-	Leak Check Procedure - Generator End Covers
SIG110030	-	Radiator Assembly
SIG110031	-	Body - Radiator
SIG110033	-	Details - Radiator
SIG110040	A	Heat Pipe Assembly
SIG110041	-	Heat Pipe Assembly, Axially-Grooved, Performance Requirements
SIG110043	A	Heat Pipe Tubing Selection and Charge Requirements
SIG110044	-	Tubing - Axially-Grooved, Internally
SIG110050	C	Housing Assembly - Power Section

<u>Drawing No.</u>	<u>Revision Level</u>	<u>Title</u>
SIG110051	B	Housing - Preliminary Machining
SIG110053	A	Fin Details - Radiator and Power Section
SIG110054	-	Fin Welding Procedure, Housing and Radiator
SIG110056	-	Ring - Insulation Retainer, SIG Generator
SIG110061	-	End Cover Assemblies - Fueling End and Radiator End
SIG110062	A	Transition Fitting - Bimetal, Electrical Receptacle
SIG110063	-	End Covers - Fueling End and Radiator End
SIG110064	A	Electrical Receptacle Assemblies
SIG110065	A	Transition Fitting - Bimetal, Outgassing Tube and Gas Management Valve
SIG110066	-	Mounting Clip - Gas Management Valve
SIG110067	-	Handle - Gas Management Valve
SIG110071	-	End Rings, Thermal Insulation, Generator
SIG110072	-	Insulation Disc
SIG110073	C	Multifoil Sleeve Assembly - SIG Generator
SIG110082	-	Ultrasonic Inspection Procedure, 6061 Al/Ag/304L Clad Plate Blanks
SIG110083	-	Fitting - Gas Management Valve
SIG110086	-	Terminal Assembly - Electrical Heat Source
SIG110087	-	Insulation Support - Power Section Assembly
SIG110100	-	Connector Saver Cable - ETG

<u>Drawing No.</u>	<u>Revision Level</u>	<u>Title</u>
SIG111001	-	Mandrel - Grooved Tube
SIG111003	B	Bonding Tool - Auxiliary Cooling System
SIG111005	-	Leak Check Fixtures - End Covers
SIG111006	C	Leak Check Fixture - Generator Housing
SIG111010	-	Electrical Heater - Bonding Tool, Auxiliary Cooling System
SIG111011	-	Internal Support Structures - Housing Leak Test Fixture
SIG111013	-	Test Cable - 3M Shipping Container
SIG111015	-	Outgassing Station - SIG/Galileo
SIG111016	F	Installation Fixture - Housing/Fin
SIG111018	A	Tool Assembly - Heat Pipe, He Leak Test
SIG111019	A	Manifold and Piping - Outgassing Station
SIG111020	A	Gas Sample and Calibration System - Outgassing Station
SIG111021	-	Covers - Temporary, Elastomer Sealed
SIG111022	-	Heat Sink Assembly - Electrical Connector
SIG111025	-	Stand - Generator, Outgassing Station
SIG111028	-	Transfer Dolly - Power Section
SIG111030	-	Side Beam Installation and Handling Fixture - Power Section Assembly
SIG111031	Holding F	Holding Fixture Assembly and Installation - Power Section, Glove Box
SIG111032	-	Positioning Tool - Power Section Insulation
SIG111037	-	Handling Sling - Power Section

<u>Drawing No.</u>	<u>Revision Level</u>	<u>Title</u>
SIG111038	-	Transition Fitting - Bimetal, Outgassing Tube, Temporary Cover, RTG Configuration
SIG111041	-	Handling Dolly and Adapter - Heat Source Container
SIG111042	-	Outgassing Valve Assembly
SIG111043	-	Outgassing Valve
SIG113000	-	Shipping Crates - Heat Pipes
SIG113001	B	Transfer Container - GE Electrical Heat Source
SIG113002	C	Shipping Container Assembly, Generator Housing and Radiator
SIG113010	-	Stand Assembly - Generator
SIG113016	-	Processing Procedure - Electrical Heat Source Transport Container
SIG114109	-	Mass Model - Engineering RTG
SIG114150	-	Cable Assembly - Power Out and TC
SIG114151	-	Cable Assembly - Generator Heater Power and TC
SIG114152	-	TC Cable Assembly - Generator Instrumentation
SIG115001	-	Electrical Receptacle Life Test Procedure
SIG115003	B	Test Fixture - Vibration, Multifoil Insulation Sleeve
SIG115022	-	Multifoil Sleeve Assembly Vibration Test Procedure
SIG115023	A	Details - Cooling Tube Dummy Loop Test
SIG116001	-	3M/TES Interface - Mechanical and Electrical

<u>Drawing No.</u>	<u>Revision Level</u>	<u>Title</u>
SIG116002	-	GE/TES Interface - Electrical Heat Source
SIG120002	A	Diaphragm - Generator Venting System
SIG120003	-	Lance - Three Edged, Generator Venting System
SIG120004	B	Bimetal Seal Ring - Generator Vent System
SIG120005	A	Piston, Body and Collar Assembly - Generator Vent System
SIG120008	-	Bimetal Ring/Diaphragm Assembly - GVS
SIG120012	-	End Cover Assemblies - Fueling End and Radiator End
SIG120022	-	End Covers - Fueling End and Radiator End
SIG120023	-	Spring - Inner, Lance Assembly, GVS
SIG120024	-	Spring - Outer, Lance Assembly, GVS
SIG122000	-	Oxidation of 3D Carbon-Carbon (HM3000 Pan Fiber) - Test Procedure
SIG125001	B	Plasma Arc Model Assembly Disc Specimen AFFDL Facility
SIG125002	A	Plasma Arc Model Disc Specimen AFFDL Facility
SIG125005	-	Model Holder - Disc Specimen NASA/Ames Facility
SIG125006	-	Test Coupons - Flyer Plate and Gas Gun
SIG125007	-	Model Holder - Hollow Cylinder Specimen, NASA/Ames Facility
SIG125008	B	Aeroshell Assembly - Heat Source, Over-pressure Test
SIG125009	A	Plasma Arc Model Hollow Cylinder Specimen AFFDL Facility

<u>Drawing No.</u>	<u>Revision Level</u>	<u>Title</u>
SIG125010	A	Plasma Arc Model Assembly Hollow Cylinder Specimen AFFDL Facility
SIG125011	A	Model Holder Hollow Cylinder Specimen AFFDL Facility
SIG125012	-	Charpy Impact Test Coupons
SIG125013	-	Manufacturing Definition: Allocation of 3D Carbon-Carbon for Test Coupons
SIG125014	A	Model Assembly - Disc Specimen, Plasma Arc, NASA/Ames Facility
SIG125015	A	Model Assembly-Hollow Cylinder Specimen, Plasma Arc, NASA/Ames Facility
SIG125019	-	Generator Vent System - Test Procedure
SIG125021	-	Diaphragm Piercing - Test Fixture, Generator Vent System
SIG125022	-	SIG Auxiliary Cooling Loop - Test Procedure
SIG125027	-	Test Fixtures - Generator Vent System
SIG127001	-	Isotopic Heat Source Receiving, Storage and Monitoring Procedure
SIG127002	-	Isotopic Heat Source Transfer from Shipping Container to Fueling Chamber Procedure
SIG127003	-	Isotopic Heat Source Transfer from Fueling Chamber to Shipping Container and Packaging Procedure
SIG127004	-	Isotopic Heat Source Shipping Procedure

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APPENDIX B
SIG GALILEO REPORTS LIST

SIG GALILEO REPORTS LIST

TES-2865-01	Documentation Plan
TES-2865-06	Reliability Program Plan
TES-2865-09	Program Plan
TES-2865-10	DOE/TES/3M Interface Agreement
TES-2865-12	Configuration Control Plan
TES-2865-13	Quality Assurance Program Plan
TES-2865-15	DOE/TES/MRC Interface Agreement
TES-2865-22	SIG/GM Power Prediction Procedure
TES-2865-25	Study of Micrometeoroid Penetration of Heat Pipes
TES-2865-26	GDS Disassembly Report
TES-2865-30	ETG Acceptance Test Plan
TES-33009-33	SIG Safety Test Plan
TES-33009-42	Safety Analysis Report (Draft)
TES-33009-43	Improved MHW Heat Source Aeroshell Project
TES-33009-44	Heat Pipe Development Report
TES-33009-45	Heat Pipe Attachment Technology
TES-33009-46	SIG/Galileo Program Final Report
TES-33009-47	Hermetic Receptacle Test Program Report
TES-33009-50	Hermetic Receptacle Test Program Report
TES-33009-51	SIG Bimetal Weld Transition Joint Report

TES-33009-46

APPENDIX C

LIST OF MONTHLY PROGRESS REPORTS

APPENDIX C

LIST OF MONTHLY PROGRESS REPORTS

Monthly Technical Progress Reports

April 1978	TES-2865-03
May 1978	TES-2865-05
June 1978	TES-2865-07
July 1978	TES-2865-11
August 1978	TES-2865-17
September 1978	TES-2865-20
October 1978	TES-2865-23
November 1978	TES-2865-28
December 1978	TES-2865-32
January 1979	TES-33009-35
February 1979	No report
March 1979	TES-33009-36
April 1979	TES-33009-38
May 1979	TES-33009-40

Monthly Top Summary Reports

April 1978	TES-2865-02
May 1978	TES-2865-04
June 1978	TES-2865-08
July 1978	TES-2865-14
August 1978	TES-2865-16
September 1978	TES-2865-19

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LIST OF MONTHLY PROGRESS REPORTS

Monthly Top Summary Reports

October 1978	TES-2865-24
November 1978	TES-2865-27
December 1978	TES-2865-31
January 1979	TES-33009-34
February 1979	No report
March 1979	TES-33009-37
April 1979	TES-33009-39
May 1979	TES-33009-41

APPENDIX D

GENERATOR HOUSING AND RADIATOR

STRESS ANALYSIS APPROACH

1. Description

The generator power section housing is a welded assembly of a cylindrical barrel with end covers and cooling fins (see Fig. IV-1 and IV-2). The barrel is machined from a 6061-T652 aluminum alloy ring forging to be a circular cylindrical shell with integral fin stubs, heat pipe assembly saddles, and installation flanges and lugs. The stubs and saddles are parallel to the cylinder axis, are equally spaced about the periphery, and, with the fins, stiffen the shell longitudinally. A sleeve is provided at the power section and radiator interface for the attachment to the radiator flange.

The end covers are machined from 6061-T6 aluminum alloy plate. Designed for stiffness, they are circular flat plates integrally stiffened with radially oriented ribs. They are attached by welding to the cylinder barrel.

The radiator extension is a thin (.020 in. minimum) 6061-T652 aluminum alloy shell (see Fig. IV-1 and IV-2), longitudinally stiffened by 10 heat pipe saddles and fins and by two internal rings, an aft integral ring, and a forward flange. The flange serves to stiffen the cylinder and is also the attachment joint connecting the generator to the support boom. At the aft end it is riveted to the connecting sleeve on the power section.

The fins, fabricated from 6061-T6 aluminum alloy plate, are rectangular plates tapering in cross-section from root to tip. They are welded to the stubs integral with the power section housing and radiator shell and project radially from them.

The heat pipes are copper alloy tubes internally grooved with longitudinal grooves spaced around the inner periphery and sealed at the extremities. They are attached by .60/40 tin lead solder to the saddles on the generator housing and radiator.

2. Design Loads

The generator housing and end covers are subjected to three internal loads simultaneously:

- (1) a pressure differential of 1.5 atmospheres (22.05 psi internal, 0 psi external) or one atmosphere external and vacuum internal,
- (2) a maximum axial heat source preload of 4000 pounds,
- (3) the thermoelectric radial spring loads at two symmetric annular locations. The radial loading is imposed by 168 thermoelectric elements (84 positive and 84 negative) in each of the two rings. Each positive element imposes a load of 7.37 pounds and each negative element imposes a load of 21.21 pounds.

The housing and radiator are also subjected to loads while in the stowed position as shown in Fig. D-1. Loads at points P_1 , P_2 , and P_3 design the lugs and lug support structure at both the fueling end and the monoball attachment. A pair of lugs is assumed for each vector set. The alternate load condition is obtained by reversing all vectors. Post-deployment loads for the power section - radiator interface are shown in Fig. D-2. However, they are less critical than the stowed position loads.

The dynamic environments of random vibration, shock, and acoustic noise applicable to the structural analysis and design of the generator are as follows:

- (1) random vibration, per Fig. D-3, at the three RTG truss mounting joints,
- (2) pyrotechnic shock, per Fig. D-4, at the three RTG truss mounting joints,
- (3) acoustic noise, as specified in Table D-1.

FIG. D-1. RTG APPLIED ULTIMATE LOADS

STOWED CONDITION
 $T \approx 300^\circ\text{F}$

ALTERNATE LOADING: REVERSE ALL VECTORS

$$F = mR\omega^2 = \frac{90}{386} (126) \left[2\pi \frac{30}{60} \right]^2 = 290 \#$$

363 # (ULTIMATE)

FIG. D-2. POST DEPLOYMENT LOADS

FIG. D-3

FIG. D-4

TABLE D-1
ACOUSTIC SOUND PRESSURE LEVELS

1/3 Octave Band
Center Frequency, Hz

Sound Pressure Level, db

32	126
40	128
50	130
63	131.5
80	133.5
100	134.5
125	136
160	137
200	137.5
250	138
315	138.5
400	138.5
500	138
630	137.5
800	137
1000	136
1250	135.5
1600	134
2000	133
2500	132
3150	130.5
4000	129
5000	128
6300	126.5
8000	125
10000	124

Overall Level = 149 db.

The environments thus defined represent the levels that exceed the expected flight environments. They are the levels to be used in the qualification tests to establish confidence in the design by verification of the hardware design margins.

The cooling fins, loaded by the dynamic environment of either acoustic noise, random vibration, or shock, are also subject to the thermal gradients between the fin roots and tips due to the thermal conduction from the heat pipes. They are critical for the combined effect of the sound pressure loads and the thermal environment.

The heat pipes, primarily subjected to the internal pressure that is a function of the working fluid and temperature are also subject to the thermal and dynamic environments described. The heat pipe attachments to the generator are primarily designed by the thermal environment.

3. Design Approach

The structural criteria applied to the design of the housing and radiator are the stress requirements stated in Ref. 1. These stress requirements are defined for primary and secondary structure and fasteners and joints.

The material minimum strengths at temperature are obtained from the material data presented in Ref. 2 and Ref. 3.

The structural analysis of the housing and covers initially assume the housing to be an internally pressurized cylinder end-capped with flat plates. The discontinuity shears and moments for these shell junctions are combined with the forces due to the internal pressure and the preload. The resultant forces are utilized to determine minimum structural dimensions compatible with the maximum allowable deflection and the material strength properties at temperature.

The pressurized cylinder is edge-loaded by the shears and moments at the junctures of the housing cylinder and the end covers. The effect of the non-concurrence of the middle surfaces at cylinder radial offsets and changes of thickness are also taken into consideration as additional discontinuities. The net discontinuity stresses are combined with the stresses due to internal pressure and preload.

The housing areas supporting the thermoelectric elements are assumed to be closed circular rings loaded internally by equal radial forces, equally spaced, in addition to the internal pressure. They are considered to be statically determinate beams and are analyzed as such by the use of Castigliano's first theorem for bending and tension stresses due to the imposed loads. The rings are assumed to be of uniform cross-section, of large radius in comparison to the radial thicknesses so that the deflection theory for straight beams is applicable, and deformation does not cause loss of circular shape.

Handling and mounting lugs with adjacent support structure are analyzed and designed for bending, shear tearout, and bearing due to the loads imposed during prelaunch handling and while in the stowed position during launch and flight.

Discrete element models are used to determine the stresses diffused through the radiator from the boom. Fig. D-5 shows the model used for the boom loads and Fig. D-6 shows the thermal stress model.

The radiator consists of a thin shell stiffened longitudinally by ribs and transversely by the internal rings. The buckling strength of the skin between the ribs is greater than the buckling strength of an unstiffened cylinder. The longitudinal ribs, stabilized by the adjacent skin, are analyzed for column buckling. The radiator shell

FIG. D-5

Discrete Element Model for Boom Loads

FIG. D-6

Discrete Element Thermal Load Model

skin is thus investigated for buckling by the following methods of analysis:

- (1) skin stress vs. unstiffened shell buckling stress,
- (2) load on a longitudinal rib vs. the Euler load for the length between a flange and a ring or between two rings.

The intermediate internal rings and the aft internal ring support the fins and are loaded radially by couple forces from fin longitudinal bending due to radial temperature gradients. The forward flange, in addition to stiffening the radiator shell, also acts as tension fittings at the boom attachment points.

The fins on the power section housing are continuously supported cantilever plates in bending due to the lateral pressure from the acoustic noise environments. The fins on the radiator section, supported mainly by the discrete rings, are analyzed for bending due to the transfer of sound pressure loads to the supporting rings and differential thermal expansion.

The heat pipes are analyzed for the circumferential stresses due to the internal pressure. The ends are analyzed for the combined membrane stresses due to internal pressure and to the structural discontinuities at the junctures of the heat pipe cylinders and end caps.

The housing-radiator splice is designed for bending, axial load, and shear due to the loads in the stowed condition (see Fig. D-1).

REFERENCES

1. Structural Design Criteria, LCHPG-FAS-692, January 4, 1977.
2. Metallic Materials and Elements for Aerospace Vehicle Structures, MIL-HDBK-5B, September 1, 1971.
3. Aerospace Structural Materials Handbook, Vol. 3, AFML-TR-68-115, 1977.

APPENDIX E

LAUNCH AND FLIGHT INDUCED ENVIRONMENTS

LAUNCH AND FLIGHT INDUCED ENVIRONMENTS

The requirements listed in this section consist of the environments that the RTG may encounter during powered flight through spacecraft separation from the IUS.

1. Low Frequency Random Vibration

Launch vehicle and IUS-induced transient sinusoidal environments are presented as a low frequency random vibration as defined in Table E-1. These levels should be assumed at the RTG mounting points in any direction.

TABLE E-1
Random Vibration (Flight Levels)

<u>Damping*</u> <u>Ratio</u>	<u>Frequency</u>			<u>Overall Amplitude Grms</u>
	<u>35-45 Hz</u>	<u>45-60 Hz</u>	<u>60-100 Hz</u>	
$C_1 = 3\%$	+24 dB/Oct	G^2/Hz .06	-12 dB/Oct	1.45
$C_1 = 2\%$	+24 dB/Oct	G^2/Hz .053	-12 dB/Oct	1.35
$C_1 = 1\%$	+24 dB/Oct	G^2/Hz .034	-12 dB/Oct	1.10

*Must be determined in development tests.

2. Random Vibration

The Gaussian random vibration level is shown in Figure E-1. Levels are inputs in any direction at the mounting points of the RTG. Duration is three (3) minutes per axis.

3. Static Acceleration.

The RTG will be subjected to 5G in any axis.

Figure E-1 Flight Level Random Vibration Spectrum

4. Spin Velocity and Spin-up Rate

The RTG will be subjected to various centripetal accelerations for various spin durations. The centripetal acceleration varies with change in spin rate, spin arm and RTG/major axis/SC spin axis angles. A summary of spin conditions are provided in Table E-2.

TABLE E-2
RTG Spin Details

<u>Spin Event</u>	<u>Spin Rate (RPM)</u>	<u>Spin* Arm (M)</u>	<u>Axis/SC Spin Axis (+Z) (° F)</u>	<u>Spin Duration (S)</u>	<u>Centripetal Acceleration (G)</u>
IUS Burn	70	1.35	3.5 ± 3	900	7.40
Cruis	5	3.71	90 ± 5	**	0.10
JOI	30	3.71	90 ± 5	5×10^3	3.73
Orbit	5	3.71	90 ± 5	3×10^6	0.10

*From S/C spin axis to cg of RTG

**Combined with orbit time

5. Shock

The shock level at the RTG mounting structure is defined by the shock response spectrum given in Figure E-2. The waveform of the transient defined by the shock spectrum may be assumed to be an exponentially decaying complex sinusoid with an approximate decay time of 7 ms.

6. Acoustic

The acoustic noise environment consists of a reverberant acoustic field of 143 dB with 1/3 octave band sound pressure levels as specified in Table E-3.

TABLE E-3Flight Level Acoustic Sound Pressure Levels

<u>1/3 Octave Band Center Frequency Hz</u>	<u>Sound Pressure Level (dB ref 20×10^{-6} Newton/m² (2×10^{-4} dyne/cm²))</u>
32	120
40	122
50	124
63	125.5
80	127.5
100	128.5
125	130
160	131
200	131.5
250	132
315	132.5
400	132.5
500	132
630	131.5
800	131
1000	130
1250	129.5
1600	128
2000	127
2500	126
3150	124.5
4000	123
5000	122
6300	120.5
8000	119
10000	118
Overall	143
Duration	1 minute

7. Temperature

During ascent, until opening of the ATS cargo bay ducts, the cargo bay temperature will not exceed TBD°C. After the STS cargo bay doors are open, the cargo bay temperature shall exceed TBD°C.

8. Pressure

The pressure change during powered flight will be from sea level to a pressure of less than 1.33×10^{-3} Newton/m² (10^{-5} torr). The atmospheric pressure reduction rate maximum will be $7.34 \times 10^3 \pm .69 \times 10^3$ Newton/m²/sec (55 ± 5 torr/sec) beginning from a rate of less than $1.33 \times 10^3 \times 10^3$ Newton/m²/sec (10 torr/sec) and returning to a rate of less than 1.33×10^3 Newton/m²/sec (10 torr/sec) in a period of less than five (5) seconds.