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SNAP 21A-TEN-WATT
DEEP SEA ISOTOPIC
POWER SUPPLY

Contract AT(30-1)-3322

MND-3322-13

CLASSIFICATION CANCELLED
DATE 2/27/68
For The Atomic Energy Commission
H. F. Canale
Chief, Declassification Branch

Second Quarterly Progress Report
for Period
June Through August 1964

W. A. McDonald
Engineering Manager

Approved Paul J. Nick
Program Manager

Published October 1964

Downgraded at 12 year
intervals; not auto-
matically declassified.

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FOREWORD

This is the second quarterly progress report describing the technical status of the SNAP 21A, Phase I development program. It covers the work accomplished by Martin-Nuclear during the period from June through August 1964 under Contract AT(30-1)-3322 for the U.S. Atomic Energy Commission.

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SUMMARY

During the June to August 1964 quarter, the SNAP 21A system configuration was set and all required components were ordered. The titanium pressure vessel raw forgings were received and are now undergoing final machining. The uranium-8% molybdenum biological shield was also received for use in the full-scale operating model. Tantalum-10% tungsten and Hastalloy-C rod was finish machined into the proposed fuel capsule configuration in preparation for seal welding at Oak Ridge National Laboratory and hydrostatic pressure testing at the Naval Research Laboratory.

The segmented thermoelectric couple was reoptimized based upon refined material property data. The selected thermoelectric couple for the SNAP 21A system, with a design thermoelectric efficiency of 10.4% at 950° F hot junction temperature is described in this report.

Initial fabrication of the selected thermoelectric couple showed that the temperature cycle required to bond the high performance cast lead telluride P element to the hot shoe caused a resistance increase of two to three times the initial element resistance. Subsequent lower temperature bonding cycles for assembly of the bismuth-telluride cold side segment and cold junction electrical conductor straps produced additional resistance increases in the P lead telluride element. Concurrent test data on hot junction pressure contact thermoelectric couples revealed lower initial power output than couples with metallurgical bonds at the hot junction. However, the pressure contacted couple performance remained essentially constant with time while P element performance reduction occurred in bonded couples. Based upon this performance data and the desire for high acceptance yield in manufactured couples, hot junction pressure contact was selected for the P side and hot junction metallurgical bond for the N side of the thermoelectric couple.

During the quarter, a thermoelectric couple acceptance criteria was specified stating the minimum allowable Seebeck voltage and maximum allowable resistances to be used in full-scale 16-couple modules.

SNAP 21A material galvanic corrosion studies were initiated. This study will result in a predictable corrosion rate for the various material combinations used in the generator in the event of a pressure vessel leakage occurring in the seawater environment. A study of the proper welding procedures to be used in welding the selected tantalum-10% tungsten fuel capsule material was also made. The resulting information was given to ORNL, the facility where the fuel capsule will be seal welded in preparation for hydrostatic pressure test.

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A preliminary reliability and failure mode analysis of the generator system was made during the quarter. These analyses will be updated as more test data become available.

Power conditioner design and analysis were completed and a breadboard test was made. The results of the test showed that when operating at a generator input to the converter of 1.8 volts, rated output of 10 watts was maintained at 24 volts d.c. with an overall system efficiency of 94%.

The safety analysis of the SNAP 21A generator showed that by proper design of a generator shipping pallet, air transportability was entirely feasible. The safety analysis also showed that for the other modes of transportation, the fueled generator complies to Bureau of Explosives and ICC regulations.

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I. INTRODUCTION

During the first quarter of the SNAP 21A development program (March through May 1964), the Martin Company devoted major efforts to detail design and analysis of the power system with emphasis on procurement of materials and assemblies for component development tests. Major components of the system, including fuel capsule, pressure vessel, thermal insulation and biological shield, were engineered and released for procurement and manufacture. The thermoelectric system was also analyzed to facilitate design and manufacture of component test assemblies.

During the second quarter, June through August 1964, major efforts were devoted to selection of the most suitable segmented thermoelectric couple material combinations by accelerated furnace aging tests. All primary procurement and material items required for component tests and to assemble a full-scale electrically heated operating model were placed on order. Prior to this time, the system configuration had been firmed up by detail engineering design.

Preliminary thermoelectric couple and module tests were initiated to evaluate selected material combinations under operating conditions prior to firming up the final couple configuration.

Dc-to-dc converter regulator design studies and breadboard tests were completed to demonstrate all planned performance objectives.

System reliability studies, including a probability estimate of system ability to operate unattended for five years without failure, were undertaken.

Safety studies included analysis of techniques to permit air transportation of the fueled generator under considerations of accident and subsequent terminal velocity earth impact.

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II. GENERATOR DEVELOPMENT*

A. DESIGN CONFIGURATION

The SNAP 21A design configuration (Fig. 1) consists of four major subassemblies. These are:

- (1) Titanium lower pressure vessel with depleted uranium shielding.
- (2) Vacuum insulation system.
- (3) Fuel capsule with top uranium shield plug.
- (4) Titanium upper pressure vessel including power conditioner, segmented thermoelectric module assembly and electrical feedthrough connector.

Two pressure vessel raw forgings which are made from the Ti-6% Al-4% V alloy were received during the quarter and can be seen in Fig. 2. These units are designed to withstand 10,000 psi external pressure without collapse or leakage. The parting flange seal between the upper and lower halves of the vessel is made using two Viton-A O-rings. Plans were made with the U.S. Naval Research Laboratory, during the quarter, to hydrostatically pressure test the assembled unit to the 10,000 psi design pressure level.

The lower portion of the pressure vessel contains the biological shielding which is cast and machined from depleted uranium, alloyed with 8% molybdenum. The shielding was also received during the quarter and can be seen in Fig. 3. The shield plug, fitted to a fuel capsule, can be seen in Fig. 4. The shielding is held in place by eight holddown tabs which mesh with a retaining groove in the lower section of the pressure vessel. These tabs are designed to hold the shielding in place with loadings up to 60 g.

The vacuum insulation assembly is then fitted into the central portion of the shield body. The insulation system which is presently under manufacture is scheduled to be shipped during the month of October.

The fuel capsule is tantalum-10% tungsten alloy, a high density, high strength refractory alloy. A Tantalum and a Hastalloy-C fuel capsule was manufactured during the quarter and can be seen in Fig. 5. The body-to-plug weld area which is awaiting some welding develop-

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A	Electrical feedthrough connector	H	Hold-down ring
B	Cooling fin	I	T/E module assembly
C	Instrumentation space	J	Vacuum insulation assembly
D	Dc/Dc converter	K	Biological shielding
E	Pressure vessel	L	Fuel capsule
F	Indexing pin	M	Fuel cannister
G	Viton "O" ring seals	N	Cold junction heat sink

Fig. 1. SNAP 21A Ten-Watt Undersea Generator

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Fig. 2. SNAP 21A Forged Pressure Vessel Prior to Final Machining

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Fig. 3. SNAP 21A Uranium-8% Molybdenum Biological Shield

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Fig. 4. SNAP 21A Biological Shield and Fuel Capsule

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Fig. 5. SNAP 21A Machined Fuel Capsule Components

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ment by Oak Ridge National Laboratory (ORNL) remains the only area of possible redesign. The material to be used during the welding development has been supplied to ORNL.

The fuel capsule assembly, including the attached uranium-8% molybdenum shield plug which completes the biological shielding of the heat source, is held in place by a retaining ring of Min-K insulation. The ability of the Min-K to withstand dynamic loading conditions and the response of the holddown rings and retaining plate system to random vibrational inputs will be checked experimentally during the Phase I program.

The upper section of the pressure vessel houses and supports the thermoelectric module assembly, instrumentation, the dc-to-dc converter and the electrical feedthrough connectors. The thermoelectric package is bolted in place, drawing the conically shaped portion of the module heat sink into contact with a mating surface on the inside of the pressure vessel dome. A good heat transfer contact is maintained along this interface by coating the two mating surfaces with aluminum grease. The primary heat rejection path is then from the module cold junction through the copper cold sink, across the mating conical sections, through the pressure vessel dome and fins to the ambient conditions surrounding the generator.

The SNAP 21A thermoelectric module design, shown in detail in Fig. 6, employs a conventional spring and piston-type cold junction arrangement. This will assure that all thermoelectric elements are maintained under the proper external compressive loading. Instrumentation for the unit will consist of thermocouples and voltage taps along with a pressure transducer to monitor the module pressure. Fabricated components of the thermoelectric module assembly can be seen in Fig. 7. The development of the printed circuit thermoelectric module has been deleted from SNAP 21A because of the problem of maintaining a high enough compressive loading on the thermoelectric elements.

The dc-to-dc converter, which will be packaged into the envelope shown in Fig. 1, was shown to have an efficiency of 94% during the quarter. This component and its packaging are shown in Fig. 8.

Final vessel sealing is made at the electrical feedthrough connector after the electrical leads are pulled through and soldered to the connector. This unit is bolted to the top of the pressure vessel and also relies upon the use of Viton-A O-rings to effect the flange seal.

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Fig. 6. SNAP 21A Thermoelectric Module Design

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Fig. 7. SNAP 21A Thermoelectric Module Fabricated Parts

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Fig. 8. SNAP 21A DC-DC Converter/Regulator Breadboard and Packaging Components

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B. PERFORMANCE AND OPERATING CHARACTERISTICS

1. Design Operation

The thermoelectric analysis which was made during the first quarter was updated during this period using new thermoelectric property data (see Appendix A). The Vince-Tom code for the digital computer was used. The Melpar measurements of the Melcor material were under question; therefore, measurements of Seebeck voltage and resistivity were made in the Martin Metallurgical Laboratory. The results of these measurements were considerably different than the data used in the previous analyses. The figure-of-merit (or power conversion worth) of the P material, BiSbTe, was substantially better than previously believed. However, the figure of merit for the N material, BiSeTe, was considerably lower than expected. The present couple configuration is shown in Fig. A-1, Appendix A. The P leg has been adjusted to account for this new data while the N leg cold segment of BiSeTe was replaced by a segment of PbSnTe doped with 0.03% PbI₂.

The use of the variably doped N leg increases the thermoelectric efficiency because a low resistance diffusion bond between the two PbSnTe segments is possible. However, the revised thermoelectric module design utilizing springs and pistons necessarily causes an increase in the cold junction temperature to approximately 120° F. This increase in the cold junction temperature lowers the thermoelectric efficiency and does not allow all of the increase made possible by the change made in the N leg material. The revised performance figures for the SNAP 21A generator are shown in Table 1.

2. Off-Design Generator Operation

The calculated hot junction temperature of the thermoelectric elements was 950° F at end of life and increased to 1050° F at beginning-of-life conditions because of the necessary overload of fuel required to compensate for the isotope decay. Because of the uncertainty of long time operation at this temperature, two approaches have been studied to lower the beginning-of-life hot junction temperature. Both approaches utilize the power conditioner. The first approach utilizes a power conditioner which operates at 1.8 to 2.0 volts, increasing the operating current from 6 to 7.35 amperes and thus cooling the hot junction temperature to approximately 1020° F. The other alternative studied is a power conditioner which operates at constant power throughout life. This unit will operate at 8.25 amperes initially and will cool the hot junction temperature at beginning-of-life to 975° F. Either of these converters will limit the beginning-of-life temperature to a reasonable value.

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SNAP 21A Performance Summary
(all items refer to end-of-design life except where noted)

	<u>Before**</u>	<u>Present</u>
Thermoelectric efficiency (%)	10.2	10.38
Heat to elements (watts)	117.6	115.7
Total heat inventory (watts)	160.9	159
System Efficiency* (%)	6.75	6.83
Thermal inventory--beginning-of-life (watts)	182	179.7
Fuel loading--beginning-of-life (kc)	28.9	28.5
Generator		
Segmented module		
Output power (watts)	12	12
Load voltage (volts)	2.0	2.0
Open circuit voltage (volts)	3.5	3.47
Optimum load resistance (ohm)	0.333	0.333
Internal resistance (ohm)	0.251	0.245
Couples/module (No.)	16	16
Thermoelectric characteristics material		
N leg Cold segment	BiSeTe	PbSnTe (0.03% PbI ₂)
Hot segment	PbSnTe	PbSnTe (0.062% PbI ₂)
P leg Cold segment	BiSbTe	BiSbTe
Hot segment	PbTe	PbTe (0.04% Na)
Element length (in.)	0.5	0.525
Element area (in. ²)		
N leg	0.069	0.08
P leg	0.0846	0.098
Element diameter (in.)		
N leg	0.297	0.319
P leg	0.328	0.354

*Based on converter efficiency of 90%.

**Based on printed circuit cold junction hardware and BiSeTe on cold segment of N leg

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3. Insulation

An additional study was made during the quarter to assess the penalty of using an insulation system other than a vacuum super type system. The insulation chosen was Min-K1301. The generator was enlarged to its maximum size limit allowed by specification and the additional volume was filled with Min-K1301. The results of the analysis showed a drop in thermal efficiency from 73.1% to approximately 58%. In addition, the weight of the pressure vessel increased by about 80 pounds; however, a shielding weight decrease of about 40 pounds was allowable because of the larger envelope and hence lower surface dose rate. Therefore, the weight limit for the unit could be maintained but the system efficiency would decrease. The vacuum super insulation package is still planned for use in the final generator assembly.

4. Structural Characteristics

The analysis of the holddown tab for the unit shielding was completed during the quarter and the results showed the tabs would easily withstand 60 g loadings.

The analysis of the module bellows was also completed and showed, under full design deflection, that the stresses were acceptable limits.

In addition, an analysis was made to determine the necessary thickness of iron to be bonded to the stainless steel to truly make it behave as stainless steel from a thermal expansion standpoint and thus minimize the differential thermal expansion stresses between the shoe and thermoelement. The resulting maximum thickness was 0.007 inch when bonded to a stainless steel piece 0.020 inch thick. The structural analyses can be found in Appendix C.

5. Materials Compatibility

Continuing the materials compatibility work for the SNAP 21A generator, the condition of the tantalum-10% tungsten alloy to be used in the fuel capsule was studied. The material condition selected was chosen to give the structural characteristics used in the stress analysis of the capsule and was specified as arc-cast, forged (50% cold worked) and stress relieved below 1200° C.

The stress relief treatment was necessary in order to weld the material.

During the quarter, a trip was made to ORNL to review the capsule design and discuss the welding of the material with ORNL personnel. The design of the capsule was considered acceptable and it was felt no gross design changes would be necessary. It was stated that the

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0.156-inch weld groove depth should be sufficient to get a 0.150 inch minimum weld penetration. However, pending the welding of samples of the configuration, this could not be confirmed. In the event a greater groove depth is required, ORNL stated they would machine the capsule to the new dimension and specify that depth for further capsules.

It was determined that the atmosphere to be used for welding the capsule would be 99.999% pure argon. This will eliminate weld contamination by residual impurities of O_2 , N_2 and H_2 in the argon. It was also decided that the welded samples would be sectioned, and depth of penetration, extent of heat affected zone and hardness traverses would be measured. In addition, several samples will be pulled in a tensile test to assure adequate weld strength.

During the quarter, a program was outlined to determine the galvanic corrosion action of Tantung in conjunction with the other materials in the generator. A literature search will be made to determine applicable corrosion rates. Where no information exists, a couple will be made and tested in an environment of synthetic seawater.* By this procedure it will be possible to calculate the corrosion rates ($mg/dm^2/day$) of the various metallic materials in the generator in the event of an external pressure vessel leak.

In order to determine if a copper heater block could be used in the full-scale electrically heated operating model of the generator, a literature survey was made to check the compatibility of the copper with uranium-8% molybdenum. The result showed the two materials were incompatible. Therefore, in order to make use of the excellent heat transfer characteristics of the copper and to closely simulate fuel capsule weight, a tantalum stud will be threaded into both the uranium-8% molybdenum plug and the copper heater block along with a tantalum washer to separate the two materials.

6. Power System Reliability Analysis

a. System description

The SNAP 21A power supply is basically comprised of four components, namely, a thermoelectric generator, a converter, a feed-through connector and a pressure vessel. The thermoelectric generator is composed of a heat source and a 16-couple module. The dc-to-dc converter is an electrical box whose function is to provide a power con-

*Navy Dept. Specification; 44 + 27 Pb 7/1/40. A stock solution comprising 10 grams of KCl, 45 grams of KBr, 550 grams of $MgCl_2$ and 110 grams of $CaCl_2$ dissolved in sterile distilled water and diluted to one liter. Twenty grams of this stock solution plus 23 grams of NaCl and 8 grams of $Na_2SO_4 \cdot 10 H_2O$ dissolved in sterile distilled water and diluted to one liter.

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ditioning system which can convert the electrical output of the thermoelectric generator to a useful and regulated voltage level. The feedthrough connector has been used in submarine applications and has a history of reliable performances in the required use environment. The pressure vessel is a titanium alloy structure designed to be leakproof and corrosion resistant for five years or longer.

b. Reliability model

Since the thermoelectric generator, dc-to-dc converter, and the connector all must function for the power supply to achieve its mission, the reliability model will be a simple series type. The functional block diagram for the power supply is shown.

The mathematical equation that defines the reliability of the power supply is a simple series type:

$$R = R_G \cdot R_C \cdot R_F \cdot R_P$$

where

R = reliability of the power supply

R_G = reliability of the generator

R_C = reliability of the converter

R_F = reliability of the feedthrough connector

R_P = reliability of the pressure vessel.

Generator reliability. The reliability of the generator is based fundamentally on thermoelectric couple performance. Although the heat source is a major subassembly of the generator, its reliability approaches one since its performance is basically a function of a physical law, namely, the exponential decay of the radioactive isotope. Moreover, the mechanical reliability of the generator approaches one as there are no moving functional parts; the structural reliability of the generator approaches one since adequate design measures are to be taken to assure failure-free operation in the launching phase. Therefore, the reliability of the generator will be approximately equal to the reliability of the thermoelectric couple module.

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Failure rate data on nonsegmented thermoelectric couples was available as of August 1964; over 17,200,000 couple hours have been accumulated and only one catastrophic failure has resulted--an open circuited SNAP 9A. However, in this application, segmented couples will be used to achieve a higher efficiency; consequently, since this failure rate data was accumulated on nonsegmented couples, a conservative estimate of the failure rate of the nonsegmented thermoelements has been statistically determined so that one could say with 50% confidence that the failure rate is not greater than 0.0098%/1000 hours. It will be assumed, then, that the failure rate of the segmented couple will be 0.0098%/1000 hours. Therefore, the probability that the generator will have failure-free operation for a period of five years is 0.934. Appendix D contains the calculations. This calculation was based on the fact that the thermoelectric couple arrangement is a simple series type. Also given is a mean estimate of the segmented thermocouple failure rate along with its associated reliability.

DC-to-dc converter reliability. The reliability of the dc-to-dc converter is based fundamentally on the performance of its component parts. The functional block diagram for the converter is as shown.

Each block is a component part of the dc-to-dc converter. From this block diagram, it is seen that the reliability equation for the converter is

$$R_c = e^{-t \sum_i \lambda_i}$$

where

R_c = probability that the converter will complete its mission without failure

t = mission time (five years)

λ_i = failure rate of the i th converter part

Based on MIL-HDBK-217 failure rate data, the failure rate of the dc-to-dc converter was tabulated to be 0.800%/1000 hours; the corresponding reliability of the converter for the five-year mission is 0.705. Based on MIL-HDBK-217 failure rate data of Minuteman parts, another estimate was made using the highest rates for Minuteman parts. The failure rate was tabulated to be 0.2155%/1000

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hours and the corresponding reliability of the converter for the five-year mission is 0.910. From this study, it is feasible to assume that the converter can achieve a reliability in the neighborhood of 0.90 for the five-year mission. Because of the high efficiency desired for the converter and the sensitivity required by its parts to achieve this efficiency, a wise selection of parts is mandatory. Parts selection will be based on the suitability of the part for this application along with the effect of this part on reliability (see Appendix D).

Feedthrough connector reliability. The reliability of the feed-through connector will be basically a function of the active pins. Since the number of pins that will be active is unknown at this time, a worst case estimate of the connector reliability will be made whereby it will be assumed that all 37 pins will be active. The failure rate as per MIL-HDBK-217 is 0.031%/1000 hours; the corresponding reliability for the five-year mission is 0.987. The design of the connector will be such so as to make the probability of a hydrostatic leak approach zero to assure that the only significant mode of failure will be a poor contact between connector pins and sockets. As additional information becomes available, it will be included to up-date the estimate.

Pressure Vessel Reliability. The probability that the pressure vessel will not experience a hydrostatic leak during its five-year mission will approach one since corrosion and hydrostatic tests are planned to check corrosion rate and for a hydrostatic leak. Again, as additional information becomes available it will be included to up-date this estimate.

The reliability of the SNAP 21A power supply could vary between two limits because of the converter failure rate variability; the chart shows the comparison:

R_G	$R_{c(217)}$	$R_{c(MM)}$	R_F	R_P	R_H	R_L
0.934	0.705	0.987	0.987	1.0	0.850	0.658

where

R_H = reliability of SNAP 21A power supply assuming Minuteman-type parts for the converter

R_L = reliability of SNAP 21A power supply assuming MIL-HDBK-217 caliber parts for the converter

From this reliability study, it is seen that if the design were complete the reliability for the five-year mission might vary between 0.740 and

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0.573 for the power supply. In conclusion, it should be noted that this is a pessimistic estimate of the reliability of the power supply and that part selection has not been finalized. In addition, the highest failure rates for Minuteman parts as per MIL-HDBK-217 were assumed in predicting converter reliability for the optimistic estimate of the converter reliability. Further, the thermoelement failure rate used in this estimate is taken to be conservative; as more couple data are accumulated daily, the failure rate estimate will become more accurate. There is a good possibility that the failure rate will be lower than that used in this first estimate. This factor, along with the converter considerations, can prove to be significant in raising the reliability estimate for the power supply.

Fig. 9. SNAP 21A System Reliability

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Figure 9 outlines the minimum and maximum reliabilities that can be achieved for the SNAP 21A system for the discrete periods of one, two, three, four and five years. SNAP 21A reliability will approach the maximum reliability estimate, if high reliability Minuteman parts can be used for the converter application; whereas, the SNAP 21A reliability will approach the minimum reliability estimate, if military standard parts as per MIL-HDBK-217 are used. The minimum and maximum reliability estimates for the discrete time periods are given in Table 2.

TABLE 2
SNAP 21A System Reliability

<u>Time Period</u> <u>(yr)</u>	<u>Reliability</u>	
	<u>Minimum</u>	<u>Maximum</u>
1	0.919	0.967
2	0.846	0.937
3	0.777	0.906
4	0.715	0.877
5	0.658	0.850

During this quarterly reporting period, test specifications and procedures on various components of the power supply were reviewed. Among those reviewed were the test procedure for the Linde Vacuum insulation, the test specification for the 16-couple module, and the couple acceptance criteria for the SNAP 21A 16-couple module. The Linde insulation test procedure is not finalized at this time. However, the procedure is essentially complete since it has been decided to maintain a constant power input to the thermocouples; only the number of data points that will be monitored needs to be decided. The couple acceptance criteria for the 16-couple modules has been specified and an effort will be made to incorporate these data into a usable test procedure.

A failure mode analysis has been started to investigate the mechanisms of short, open and performance drift in the converter. In addition, the general effects on the external input and output of the converter will be outlined for each part in the converter; the internal effects, as a result of the particular mode of failure for each part, will be dismissed.

A failure mode analysis has begun involving the generator and other components which comprise the power supply. A failure mode analysis of each major component in the power supply, listing the component, its

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primary failure mode, the effect on mission success, and planned reliability action to avoid or overcome the mode of failure is given in Table 3. A much more detailed failure mode analysis is planned at the part level and is to be included in subsequent reports.

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TABLE 3

Failure Mode Analysis of SNAP 21A

<u>Component</u>	<u>Failure Mode</u>	<u>Effect on Mission Success</u>	<u>Planned Reliability Action</u>
Fuel capsule	Hydrostatic leak	Affects safety only as system would have already failed	Environmental test planned on fuel capsule to determine if hydrostatic leak develops (around weld area). Non-destructive tests required to ensure adequacy of weld
Pressure vessel	Hydrostatic leak	Will cause mission failure	Environmental tests planned on pressure vessel to determine if hydrostatic leaks develop around O-ring seals or feedthrough connector. Adequate inspection techniques mandatory to assure leak-proof mechanism
Linde insulation	Thermal insulation	Will cause mission failure by causing gradual drift out of tolerance	Environmental tests planned on insulation to obtain heat loss data and determine effect on insulation after prolonged operation and at ambient temperature
Thermocouples	Thermocouple inefficiency	Mission failure occurs if power output below 10 watts at 24 vdc	Multiple exploratory and environmental tests planned on thermocouple to determine adequacy of thermocouple efficiency

TABLE 3 (continued)

<u>Component</u>	<u>Failure Mode</u>	<u>Effect on Mission Success</u>	<u>Planned Reliability Action</u>
Electrical feed-through connector	Hydrostatic leak	Would cause mission failure	Environmental tests planned to determine if hydrostatic leak develops in or around connector
SNAP 21A	Poor electrical connection	Would cause mission failure	Continuity checks made to assure proper contact was made
	Inadequate radiation shielding	Affects personnel safety in handling and possibly marine life	Complete operational model assembly test planned at elevated temperatures to determine adequacy of shielding and to notice any change in radiation properties of shielding
DC-to-dc converter	Converter	Inefficient converter could cause mission failure	Environmental and exploratory tests planned to determine adequate efficiency that would be desirable in application

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III. COMPONENT DEVELOPMENT*

A. DEVELOPMENT TESTS

1. Six-Couple Module Box Development Tests

Module Test Boxes 1 and 2 of the six-couple module test series were continued on test through the end of the quarterly reporting period. During the 54 days of accumulated test time on 24 segmented couples involving about 20,000 couple hours, resistance of the N legs has remained essentially constant. The configurations of couples in Module Test Boxes 1 and 2 are given in Table 4.

One N leg which had a relatively high initial internal resistance has begun to show a decrease in resistance indicating the leg apparently is healing itself with time and temperature. The resistance of the P legs in Module 2 appear to be increasing slightly. Thermoelectric modules 3 and 4 went on test during the middle of August and are showing some evidence of resistance increase, primarily in the P legs.

The fact that the N leg resistances are holding relatively constant has verified the selection of Sn hot junction bonds. Initially, there was a question on the use of Sn compared to nickel which had been used successfully in the past. A thermoelectric analysis of the two N leg bonding agents showed that the use of nickel bonds decreased the thermoelectric efficiency by 0.6% because of a higher initial resistance (approximately four times that of Sn). It was because of the lower initial resistance that the Sn bond was chosen.

Fabrication of couples for Module Test Box 3 is proceeding. This couple configuration, illustrated in detail in Fig. 10, has now been established as the couple configuration for full-scale 16-couple module tests. The first six completed couples have been delivered for assembly into Module 5. Single couple tests are also being run on this configuration to determine the temperature distribution and power output characteristics. The results of the initial test at two temperature levels are shown in Table 5. The temperature at the intermediate junctions show good agreement with those values determined from the figure of merit data curves. A second couple of the same configuration is presently undergoing life test.

*Contributors: D. Evans, A. Gregory.

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TABLE 4

Six-Couple Module Couple Configuration Details

Module No.	Module Test Box 1				Module Test Box 2			
	1		2		3		4	
Leg	N	P	N	P	N	P	N	P
Conductor								
Bond	40-60 PbSn	40-60 PbSn	Same	Same	Same	Same	Same	Same
Cold shoe	SS Ni plate	Fe Ni plate	Same	Same	SS	Same	Same	Same
Bond	Bi	Bi	Same	Same	Sn	Same	Same	Same
Thermoelement	BiTe	BiTe	Same	Same	PbSnTe hot pressed	Same	Same	Same
Bond	Bi	Bi	Same	Same	--	Same	Sn	Same
Barrier	Fe Ni plate	--	Same	--	--	--	SS	--
Bond	Sn	--	Same	--	--	--	Sn	--
Thermoelement	PbSnTe cast	PbTe cast	Same	Same	PbSnTe hot pressed	PbTe hot pressed	Same	Same
Bond	Sn	Sn	Same	GeTe	Same	*Sn	Same	Same
Hot shoe	SS	Fe	Same	Same	Same	Same	Same	Same
Conductor	Copper		Same		Same		Same	

*Hot pressed element, hot pressed to shoe.

Fig. 10. T/E Couple Assembly Details for Six-Couple Module
Test Box No. 3

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TABLE 5
SNAP 21A Thermoelectric Couple Temperature Profile

T_1 (° F)	T_2 (° F)	T_3 (° F)	T_4 (° F)	T_5 (° F)	T_6 (° F)	E_{oc} (mv)	E_{oc} (mv)	I (amp)	R_i (mΩ)	P_{out} (mw)
926	932	205	199	506	567	196	108	4.05	21.8	437
968	976	236	227	554	619	202	104	4.23	23.2	440

2. Single-Couple Development Tests

Figures 11 and 12 show the results of the most recent operating single thermoelectric couple tests. Couple UGB 7-16-2 has all metallurgical bonds while couple UGB 7-23-3 has pressure contacts on the N and P leg hot junctions. The description of both couples is given in Table 6.

TABLE 6
Single Thermoelectric Couple Test Configuration

<u>Couple No.</u>	<u>Description</u>	
UGB 7-16-2	N leg	Two doping levels of hot pressed PbSnTe with no barrier between segments.
	P leg	Cast BiSbTe and hot pressed PbTe with no barrier between segments bonded together with Bi solder. Both legs bonded in vertical bonding furnace. N leg is bonded with Ni; P leg bonded with SnTe.

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TABLE 6 (continued)

<u>Couple No.</u>		<u>Description</u>
PGB 7-23-3	N leg	Same as UGB 7-16-2 except pressure contact at hot junction.
	P leg	Same as UGB 7-16-2 except pressure contact at hot junction.

Figure 11 shows that the pressure contacted couple started at a lower power output but remained practically constant in output through 320 hours while the metallurgically bonded couple started at a higher power output but degraded rapidly. Figure 12 shows the resistance characteristics of the couples measured during this test. After 320 hours of operation, the hot junction temperature of the pressure contacted couple was raised from 950° to 1100° F. These results are also shown in Figs. 11 and 12.

Evaluation of this performance data and six-couple module test data in conjunction with room temperature measurements of the thermoelectric couples during the various stages of fabrication has developed the following conclusions:

- (1) PbSnTe N-type elements, metallurgically bonded to the hot shoe using Sn, offer the highest performance with no evidence of performance degradation with time.
- (2) Segmented cast BiTe-PbTe elements with a hot shoe pressure contact, while producing lower initial performance, do not show evidence of appreciable performance reduction with time as do the metallurgically bonded elements tested to date.
- (3) Long-term performance stability can be achieved most reliably based on tests performed to date using P side pressure contact and N side metallurgical bond at the hot shoe.

3. Sixteen-Couple Thermoelectric Module

During this reporting period, the test setup for the 16-couple module test series was firmed up and is illustrated in Fig. 13. The setup is relatively simple consisting of a commercial heater plate held to the module assembly by C-clamps and insulated so as to allow the required heat input to the thermoelectric module. Tests of the full-scale 16-couple thermoelectric module will be initiated during the next quarter.

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Fig. 11. Power Versus Temperature for Segmented Couples

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Fig. 12. Internal Resistance Versus Time at Temperature for Segmented Couples

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Fig. 13. Sixteen-Couple Module Test Setup

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4. Thermoelectric Couple Manufacturing Specifications

To assure that selected thermoelectric couples are acceptable as manufactured, a couple acceptance criteria was specified stating the maximum allowable values of room temperature resistance for the various junctions in the couple as well as the individual segments and legs. Also specified for the individual legs and overall couple was the minimum acceptable Seebeck voltage when operated between 975° and 115° F hot and cold junction temperatures, respectively. The couple acceptance criteria values are as shown.

(1) Junction resistance shall not exceed (at RT):

P leg hot junction (200 microohms)
P leg segment junction (160 microohms)
P leg cold junction (130 microohms)
N leg hot junction (80 microohms)
N leg segment junction* (180 microohms)
N leg cold junction (80 microohms)

(2) Couple resistances shall not exceed (at RT):

P leg BiSbTe segment (625 microohms)
P leg PbTe segment (440 microohms)
N leg PbSnTe 0.03% PbI₂ segment (975 microohms)
N leg PbSnTe 0.062% PbI₂ segment (530 microohms)
P leg (1.555 microohms)
N leg (1.845 microohms)
Total couple (3.4 microohms)

(3) Open circuit voltage between 975° and 115° F shall not be less than:

P leg (115 millivolts)
N leg (107 millivolts)
Total couple (220 millivolts)

5. Pressure Vessel and Fuel Capsule Hydrostatic Pressure Test

The SNAP 21A titanium pressure vessel and two fuel capsules, one of tantalum-10% tungsten and one of Hastalloy C will be tested for structural and leaktight integrity. Tests will be conducted during October 1964 in the U.S. Naval Research Laboratories at Washington, D.C. to pressures of 10 to 15,000 psi. The detailed test program to be conducted is given in Appendix E.

*For diffusion bonded segments, the measured value is zero.

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IV. POWER CONDITIONER DEVELOPMENT*

The objectives of the power conditioner development effort were to design and analyze a highly efficient dc-to-dc converter and regulator system to take a 1.8 to 2-volt dc input and step it up to an output of 24 volts $\pm$ 1% dc at a power level of 10 watts with a resistive load.

In designing the converter, consideration was given to the various methods of converting electrical energy from one voltage level to another. The methods are distinct with respect to efficiency, size, complexity, application and performance in different environments. In order to increase a voltage level, it becomes necessary to use a transformer. However, a transformer is an alternating current device and therefore it is necessary to convert the direct current input voltage to alternating current by use of a switching device. Several methods are available for switching purposes; the most common being shown in Fig. 14 are:

- (1) Tunnel diode static converter--These are systems using tunnel diodes as switching elements where the maximum theoretical efficiencies are 65% for germanium and 45% for silicon diodes.
- (2) Transistor single-ended static converter--These systems use single ended and flyback tape systems for switching with germanium or silicon transistors. The efficiency in this case is approximately 70%.
- (3) Transistor push-pull static converter--These are push-pull type systems using silicon or germanium transistors as switching elements. Efficiencies obtainable are in the order of 80% and 90%, respectively.

The converter circuit concept selected was a push-pull system using germanium transistors. A block diagram of the converter circuit is shown in Fig. 15.

*Contributor: M. Monaco, W. Findley

Tunnel diode static converter

Transistor single-ended static converter

Transistor push-pull static converter

Fig. 14. Converter Switching Methods

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Fig. 15. SNAP 21A Converter Block

The selection of an oscillator driver power stage concept allowed operation of the power output transformer out of saturation for minimum losses. The oscillator is of the overdriven, push-pull transformer coupled type and supplies base drive switching current to the power stage. A schematic of the oscillator driver is shown in Fig. 16.

Fig. 16. Converter Oscillator-Driver

In the circuit shown transistors (Q_1 and Q_2) are used to alternately switch the input dc voltage (E_1) across the primary windings (P_1 and P_2) of the transformer (T_1). This voltage is changed and the transformer action to S_1 or S_2 is used to supply the base drive power for the power transistors in the power stage. Resistances R_1 and R_2 forward bias transistors Q_1 and Q_2 into conduction and effectively make the oscillator self-starting. The transistors Q_1 and Q_2 are operated in a switching (saturation) mode for minimum losses. The transformer (T_1) is a saturating transformer using a square hysteresis loop-type

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of magnetic material. The parameters of T_1 establish the frequency of oscillation since it can be shown by Faraday's Law that:

$$f = \frac{E_1}{4N\phi_S}$$

where:

- E_1 = input voltage (constant)
- f = frequency of oscillation
- N = number of turns on T_1
- ϕ_S = saturation flux level of the magnetic material used.

The oscillator-driver, as shown in Fig. 16, consists of a transformer (T_1), two transistors (Q_1 , Q_2), two resistors (R_1 , R_2), and one capacitor (C_1). Since the principal functions of T_1 are to control the frequency of oscillation and the shape of the voltage wave form, it is desirable to select a magnetic material which has a high squareness ratio and low loss. The iron core of T_1 is utilized fully in that flux is driven to positive and negative saturation on alternate half cycles and induces in all windings an alternating square wave voltage. The voltage spike on the leading edge of each pulse is produced by the inductive kickback of a switch (Q_1 or Q_2) opening an inductive circuit. If the spike is too high there is danger of damaging the transistors; therefore, it is desirable to use magnetic material which has a rectangular hysteresis loop such that at saturation the windings exhibit low inductance and small kickback. The squareness ratio, B_r/B_m , is a measure of the rectangularity of the hysteresis loop of a magnetic material. This shown in the typical magnetic hysteresis loop.

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where

B_m = maximum flux density

B_r = residual flux density

H = coercive force

Table 7 shows the range of squareness ratios available.

TABLE 7
Squareness Ratios

<u>Material*</u>	<u>B_r/B_m</u>
Square orthonol	0.94
Round orthonol	0.85 → 0.95
48 alloy	0.80 → 0.92
Magnesil	0.85
Square Permalloy-80	0.80
Round Permalloy-80	0.45 → 0.75
Supermalloy	0.40 → 0.70
Supernendur	0.90

*These material names are Magnetics Incorporated tradenames. Other vendors have comparable materials with similar squareness ratios.

Table 8 shows the loss characteristics of various magnetic materials at 1 kc/sec and at saturation flux density.

TABLE 8
Loss Characteristics

<u>Material</u>	<u>Loss (watts/lb)</u>
Orthonol (square and round)	6.8
48 alloy	6.0
Square Permalloy-80	0.85
Supermalloy	0.95

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The material selected for the transformer (T) was square Permalloy-80 which has a guaranteed minimum squareness ratio of 0.80 and a loss of 0.85 watt/lb. The magnetic core is packaged in an aluminum box coated with epoxy. The resultant package is leakproof and is not sensitive to winding tension, thus preserving the magnetic material properties.

The principal function of the transistors Q_1 and Q_2 is to alternately close and open the input circuit to the transformer (T_1). For this application, it is desirable to select transistors which have a very low ON time saturation resistance, high gain (collector current/base current), fast rise and fall time, and low forward base emitter bias voltage. Silicon and germanium transistors were both considered for this function. A static test was conducted on various transistors to determine the saturation collector-emitter and base emitter loss. Table 9 shows the voltages and current at a collector current of 75 milliamperes.

TABLE 9

Collector-Emitter and Base-Emitter Losses in Transistors

<u>Part No.</u>	<u>V_{Ce}</u>	<u>V_{Be}</u>	<u>I_c / I_B</u>	<u>P_t</u>	<u>Material</u>
2N2060	0.3	0.84	7.5	0.03	Silicon
2N526	0.1	0.4	11	0.01	Germanium
2N2382	0.14	0.43	12	0.012	Germanium

where

V_{Ce} = collector emitter voltage

V_{Be} = base emitter voltage

I_c / I_B = d-c current gain = β

I_c = collector current

I_B = base current

P_t = $V_{Ce} I_c + V_{Be} I_B$ (watts)

Since V_{Ce} and V_{Be} are smaller for germanium, the converter start voltage would be less for germanium. This is an important consideration for low voltage converters. The 2N256 transistors were selected for the converter breadboard test and the results were satisfactory. The

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rise time of the 2N2382 transistor is approximately 25 nanoseconds compared to 10 microseconds for the 2N526 transistor. The faster switch transistor will be tested in the converter breadboard to determine if there is any significant advantage in its use.

The principal function of the resistors R_1 and R_2 is to place a forward bias on Q_1 and Q_2 and effectively place these transistors in a conduction mode for rapid start of the oscillator. For this application, it is desirable to select resistors which will undergo relatively small changes during the soldering process and will remain within tolerance during operational life. The general specification for high stability fixed film resistors is MIL-R-10509E. RN60C-F resistors were chosen for this job. This part number defines a 1% resistor which has a temperature characteristic of 0.005%/°C and a rated wattage of 1/8 at 125°C. This selection assures starting voltage bias stability and also a safe rating of operating stress to rated stress.

The principal function of the capacitor (C_1) is to reduce the spike on the leading edge of the voltage waveform. It is desirable to use a small capacitor for space saving and packaging problems and to assure that the unit is safely rated. The capacitor chosen for this application conforms to MIL-C-25C and is designated CP08A1KB10K3. This designation refers to a 100-working volt d-c capacitor which is reliable in an ambient temperature of -55° to 125°C. The maximum operating stress is four volts dc.

The oscillator driver feeds the power stage and output transformer which is shown in the following schematic.

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Transistors Q_3 and Q_4 are alternately driven into conduction by the oscillator-driver stage and supply square wave voltage to P_1 and P_2 , the primary windings of transformer T_2 . This voltage is changed by transformer action to S_1 and S_2 . The transformer is operated at a flux level below saturation for minimum losses.

The stage as shown consists of an output power transformer (T_2), two power transistors (Q_3 and Q_4), a resistor (R_3) and two capacitors (C_2 and C_3). The function of the transformer (T_2) is to increase the generator (RTG) output voltage to the level required by the load and at the same time transfer the generator power to the load. For this application, it is desirable to use a magnetic material which has a very low loss characteristic. Table 10 shows the loss of various magnetic materials operating at 400 cps and a flux level below saturation.

TABLE 10
Magnetic Material Losses

<u>Material</u>	<u>Loss</u> <u>(watts/lb)</u>
Orthonol	1.8
48 alloy	1.3
Square Permalloy-80	0.16
Supermalloy	0.11

The lowest loss material, Supermalloy, was selected for this application. The transformer is designed to operate below saturation flux levels for minimum distortion of voltage waveforms and low loss. The packaging is similar to that for transformer T_1 .

The function of the power transistors (Q_3 and Q_4) is to alternately switch the RTG terminal voltage across the primary winding of transformer T_2 . For these units, it is desirable to have very low loss during the conduction phase and fast switching characteristics to minimize switching losses. In order to select the lowest loss transistor, consideration was given to the following design technique.

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A graph of the total conduction power loss (P_c) versus base drive current (I_B) with collector current (I_c) as a parameter for a transistor as a closed switch is shown in the following sketch.

The power loss is given by:

$$P_c = I_B V_{Be} + I_c V_{Ce}$$

where

V_{Be} = base-emitter voltage

V_{Ce} = collector-emitter voltage

Line OA is the locus of minimum losses for varying I_c . Data of the nature exist in Martin Company files for various power transistors so that power converters can be easily optimized.

MHT2305, a 50-ampere germanium transistor, has exhibited the lowest loss for low voltage applications. Tests on numerous units at a collector current of 25 amperes has indicated a loss (P_c) spread of from 1.55 to 2.1 watts and a gain (I_c/I_B) spread of 25 to 17. Using these data, a specification was developed for a transistor similar to MHT2305 which would have a maximum loss of 1.6 watts when tested at a collector current of 25 amperes and a gain of 25. This transistor was given the part number MHT2302.

To determine the optimum base drive current and empirically prove the selection technique, static tests were conducted on MHT2305 and MHT2302 at a collector current of 3.1 amperes with the following results.

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Type	P_c (watts)	$B = I_c / I_B$
MHT2305	0.12	21.5
MHT2302	0.08	33

This selection technique represented a power loss decrease of 33%. In order to achieve this low loss, it became necessary to parallel two MHT2302 transistors so that each would conduct 3.1 amperes or half the RTG full-load current.

Selection of the resistor (R_3) and the capacitors (C_2 , C_3) was based on the same logic as used for the oscillator-driver.

Additional considerations in the converter design included determination of the optimum frequency and minimization of losses in rectification. In order to determine optimum frequency and obtain minimum loss, consideration is given to the relationships depicted in the following sketch.

where

- A = total loss
- B = transistor loss
- C = transformer loss
- f_o = optimum frequency

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As shown the optimum frequency occurs when the power transistor losses equal the power transformer losses. Consideration is also given to the losses in the power rectifiers. These can be minimized by using germanium transistors as rectifiers.

The regulation of the system was considered next. Regulation techniques are available to maintain the voltage, current or power constant. The type used depends upon application requirements. The purpose of the regulator in this application is to maintain the output voltage at $24 \pm 1\%$ volts dc. The regulator is also used to maintain the operating point of the generator constant with respect to its EI load line characteristic for any load variation. Figure 17 illustrates a shunt regulator circuit which meets the specified requirements.

Fig. 17. SNAP 21A Shunt Regulator Circuit

The circuit operation is as follows: differential amplifiers (Q_5 , Q_6) detect any voltage variation across d which is proportional to load variations. The differential amplifier output voltage (V_d) is converted to a current proportional to V_d by Q_7 and proportionately biases Q_8 and Q_9 . Q_9 is designed to handle full-load current. This type of regulator maintains the operating point of the generator constant and will also maintain the converter output voltage within specifications. The generator, regulator, and load reflected to the input is shown in the sketches.

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where

E_{oc} = generator open circuit voltage

R_o = generator internal resistance

R'_L = reflected load

R_{reg} = equivalent regulator resistance

$E_1 = \frac{E_{oc} R_k}{R_o + R_k}$ = constant since R_k is constant and

$$R_k = \frac{R_{reg} R'_L}{R_{reg} + R'_L}$$

Using this information, magnetic components were designed fabricated, and tested. A breadboard was fabricated and optimization modifications performed. The function test setup is shown in Fig. 18. Functional electrical tests were conducted based on the following test circuit shown in block form:

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Fig. 18. Functional Test of SNAP 21A Converter-Regulator Breadboard

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The results were:

<u>E₁</u>	<u>I₁</u>	<u>P₁</u>	<u>E_o</u>	<u>I_o</u>	<u>P_o</u>	<u>E_{ff}</u>	<u>Remarks</u>
1.8	6.2	11.16	24.05	0.436	10.49	94%	Full load
1.802	6.12	11.03	24.08	0	0		Open circuit

The circuit starts operating when an input voltage (E_1) of 0.1 to 0.2 volt dc is applied to the input terminals. It can be seen that the input and output voltages are within tolerance and the efficiency is greater than required. The design formulas used can be found in Appendix F.

A complete schematic of the circuit, as tested, is shown in Fig. 19.

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Fig. 19. SNAP 21A DC-to-DC Converter/Regulator Schematic

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V. SAFETY ANALYSIS AND TEST

A preliminary evaluation of the SNAP 21A radioisotope thermo-electric generator with respect to safety design criteria has been made. The design objective is to build a lightweight 10-watt undersea generator that will not expose the general public to any undue radiological hazard as a result of its use. Potential hazards associated with its use can be classified as follows:

- (1) Internal radiation exposure from the ingestion and inhalation of the radioactive fuel as a result of a breach in the fuel containment structure.
- (2) External radiation exposure resulting from a loss of the biological shield.

The primary nuclear safety requirement for the SNAP 21A RTG is fuel containment throughout all credible accidents and its operating environment. To conform to this basic concept and the operational philosophy, the following criteria for the system were developed:

- (1) The pressure vessel must completely contain the system for its design life, a minimum of five years.
- (2) The pressure vessel will not leak during system installation nor for the duration of design life.
- (3) The pressure vessel must withstand the corrosive conditions associated with the operational environment for the design life.
- (4) The radiation level must not exceed 200 mr/hr at the surface of the generator and 10 mr/hr at one meter from the surface.
- (5) The generator must withstand a standard one-hour fire as stated in the code of the National Fire Protection Association.
- (6) The fuel capsule must be capable of complete fuel containment under all conditions of intended use, credible accidents for a minimum of 300 years as follows:
 - (a) Environmental corrosion for 300 years before fuel release.
 - (b) Hydrostatic pressure from sea level to 10,000 psi.

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Compliance to these criteria must be shown through detailed analysis or experimental test programs where feasible, in order to permit the use of the RTG in an undersea environment. In addition to the undersea requirements, the SNAP 21A RTG must be air transportable. Design considerations imposed by this requirement and the influence of the previously stated criteria are now discussed.

A. AIR TRANSPORTATION

To simplify transportation of the system, a feasibility study for air transport was conducted. For air transportability, the system must maintain its integrity under the maximum credible accident which may occur. Such accidents include crashes during takeoff, mid-air collisions, mid-air explosions and terminal velocity impacts.

Major accidents occur at a rate of approximately 1.3 accidents / 100,000 hr of airtime for cargo aircraft (Refs. 1 and 2.) This figure includes all the possible accidents previously mentioned. An examination of each type of accident determined the maximum credible incident would be terminal velocity impact for the generator structure alone.

A crash occurring during takeoff will involve high velocity impact and fire possibilities. However, the impact velocity for a takeoff crash will be less than that for a free-fall terminal velocity impact condition, and, in addition, the plane would provide additional cushioning material. The temperature and duration of a takeoff fire are less than those of the standard one-hour fire of the Bureau of Explosives specifications. A mid-air collision will result, once again, in less impact energy than terminal velocity impact due to the added cushioning of the aircraft. An explosion will result in the generator structure falling free at terminal velocity. The critical problem is thus the protection for the system so as to withstand terminal velocity impact.

Terminal velocity calculations for the fuel capsule and generator alone, utilizing the terminal velocity equation, yielded velocities of approximately 485 and 622 fps, respectively.

Since these impact velocities are very high, a method was chosen of attaching a shipping pallet of corrugated aluminum sheets to the generator pressure vessel. The pallet will be attached to the vessel in a positive manner by bolts and clamps that will prevent separation during an air accident. The purpose of the pallet is to provide sufficient aerodynamic drag to lower the generator impact velocity to an acceptable value. Several configurations were investigated. However, for maximum drag a cubic structure design was selected. This is shown in Fig. 20. The location of the generator is somewhat arbitrary, since the object is to limit the impact velocity of the generator. However, maximum use of the crushable structure could be realized through a central location.

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Fig. 20. Proposed Shipping Pallet

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To determine the terminal velocity of the system, including the pallet, the equation used was:

$$D = W = \frac{1}{2} \rho C_D A V^2$$

where

D = aerodynamic drag force (lb)

W = system weight (lb)

ρ = density of air at the point where the velocity is to be calculated (slugs/ft³)

C_D = drag coefficient

A = cross sectional area (ft²)

V = velocity (fps)

The weight of the system including generator, electrical cable, aluminum collar, and shipping pallet was calculated to be 980 pounds. Using an area of nine square feet as the cross sectional area, a drag coefficient of two, and an air density of 0.0023769 slugs/ft³, the terminal velocity for an end first re-entry, was calculated to be 214 fps. The terminal velocity for a side first re-entry was calculated to be 199 fps.

To ascertain whether the system would survive the impact at these velocities, the energy that could be absorbed by the entire system was calculated based on the material properties for the system components shown.

<u>Material</u>	<u>Ta-10%W</u>	<u>U-8%Mo</u>	<u>Ti-6%Al-4%V</u>
Density (lb/in. ³)	0.608	0.629	0.162
Melting point (°C)	3050	1130	1660
Coefficient of thermal expansion (°C ⁻¹)	5.6 (10) ⁻⁶	6.5 (10) ⁻⁶	5.6 (10) ⁻⁶
Thermal conductivity (cal/cm ² /cm/°C/sec)	0.130	0.071	0.103
Specific heat (cal/gm/°C)	0.036	0.032	0.120

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<u>Material</u>	<u>Ta-10%W</u>	<u>U-8% Mo</u>	<u>Ti-6%Al-4% V</u>
Electrical resistivity (microohms-cm)	17.4	70	175
Impact strength (ft-lb)	--	3.8 (RT)	15 to 18 (RT)
Yield strength (ksi)	100 (1300° F)	118 to 140 (RT)	131 (RT)
Ultimate strength (ksi)	110 (1300° F)	123 to 144 (RT)	135.5 (RT)
Elongation (%)	5 to 15 (RT-1800° F)	8.7 (RT)	12.4 (RT)

A calculation of the weights of the Ti-6%Al-4% V pressure vessel and the U-8% Mo shield indicated 125 and 285 pounds, respectively. To calculate the energy absorption capability of the system, the following equation was used.

$$U = \frac{1}{2} (\sigma_y + \sigma_u) \frac{\epsilon_m}{\rho}$$

where

U = energy absorption capability (in.-lb/lb)

σ_y = yield strength (lb/in.²)

σ_u = ultimate strength (lb/in.²)

ϵ_m = maximum strain

ρ = density (lb/in.³)

Using this equation yielded 7.7×10^5 ft-lb for the pressure vessel and 4.47×10^5 ft-lb for the shield. Additional energy absorbing capacity is provided by the aluminum pallet and clamp collar. The 278.5 pound pallet and collar was calculated to have an energy capacity of 1.11×10^6 ft-lb. The total for the three materials is 2.33×10^6 ft-lb. On the other hand, the kinetic energy of the system is described by:

$$K_1 E_1 = \frac{1}{2} \frac{W}{g} V_T^2$$

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where

- $K_1 E_1$ = available kinetic energy (ft-lb)
 W = system weight (lb)
 g = acceleration of gravity (32.2 fps²)
 V_T = terminal velocity (fps)

By setting the ultimate strain energy equal to some constant, a safety factor, times the kinetic energy, the terminal velocity becomes the velocity for a nonfailure system. The factors used were 3, 4 and 5. Figure 21 is a plot of velocity versus nominal dimension for a cubic pallet. As indicated in the figure, the safety factor is approximately 3.5 for a 36-inch cubic pallet.

As stated previously, the pallet is clamped and bolted to the generator to prevent separation under any credible circumstances. The bolts are situated in such a manner so as to prevent direct shear forces being exerted. If a shipping pallet of the type presented is utilized, air transportation of the SNAP 21A RTG is entirely feasible.

B. SYSTEM ENVIRONMENT

The operating environment of the SNAP 21A device is to be approximately 20,000 feet under the sea at an environmental temperature of 40°F. The pressure at this depth is approximately 8900 psi.

The system has been designed such that the pressure vessel and the fuel capsule will both withstand the pressure at these depths as separate systems. Compliance with this design specification will be demonstrated through hydrostatic testing at 10,000 psi at the U.S. Naval Research Laboratory.

Since seawater is corrosive, the pressure vessel and the fuel capsule must resist corrosion well enough to maintain fuel integrity for at least 300 years before collapsing through the effects of hydrostatic pressure. The corrosion rate of Ti-6%Al-4%V is less than 0.00001 in./yr while the rate for Ta-10%W is less than 0.0005 in./yr. Thus, the pressure vessel will easily withstand the pressure of deep sea immersion for the minimum 300-year period.

In the event of a breach of the pressure vessel, the fuel capsule, assuming a corrosion rate of 0.0005 in./yr, will have been reduced in wall thickness approximately 0.15 inch. The remaining wall thickness, approximately 0.346 inch will allow the capsule to withstand an external pressure of 17,500 psi as computed from the thick wall cylinder equation:

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Fig. 21. Velocity Versus Dimension for SNAP 21A Shipping Pallet

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$$p = \frac{\sigma_y}{2} \frac{R_o^2 - r_i^2}{R_o^2}$$

where

σ_y = yield stress (psi)

R_o = outside radius (in.)

r_i = inside radius (in.)

The system will thus withstand the stress imposed by the operating medium for the 300-year minimum period.

Compatibility of the strontium-90 fuel form with Ta-10% W was established through a laboratory test in which strontium-90 was placed in a capsule interposed with discs of tantalum. The capsule was heated in air for 25 hours at 1700° F. Microscopic examination of post-test specimens revealed no reaction between the two components. The compatibility of the capsule and the fuel form is effectively demonstrated.

C. TRANSPORTATION CONSIDERATIONS

Bureau of Explosives considerations include radiation level, thermal considerations, transport accidents including 30-foot drops and 100-mph collision incidents, and handling considerations.

While the generator is within the shipping pallet, the shielding remains intact during all incidents; while the shielding is intact, the radiation levels more than comply with the specifications requiring a 200 mr/hr surface level and 10 mr/hr one meter from the center. The maximum calculated dose rates are 170 mr/hr and 5.5 mr/hr at the surface and at one meter from the centerline, respectively. Thus, the radiostrontium is shielded sufficiently for shipment under ICC regulations. A person may work around the generator for 28 eight hour-days without exceeding his permissible quarterly whole body dose. He can work in direct contact, his hands on the side of the generator shield (with the rest of his body shielded) for 13 eight-hour days without exceeding his permissible quarterly dose. Dose rates are illustrated in Fig. 22. A standard one-hour, 1700° F fire does not raise the temperature of any of the system components to a point where the shielding will break down.

In the event of the pressure vessel suffering a standard fire, the temperature in the vicinity of the fuel capsule will not exceed 2500° F while the melting point of the Tantung alloy is 5500° F. In addition, the oxidation rate of the alloy at 2500° F is extremely low. Therefore, no fuel release will result from the standard one-hour fire.

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Fig. 22. Dose Rates for SNAP 21A Generator

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The energy that is available as a result of a 30-foot drop is calculated using the following equation:

$$S = v_o t + at^2/2$$

where

S = distance fallen

v_o = initial velocity

a = acceleration of gravity (32.2 fps^2)

t = time elapsed (sec)

to determine the velocity at impact and use this velocity in the kinetic energy equation. This impact energy for the pressure vessel and shield alone was calculated to be $5.26(10)^3$ ft-lb. This value is 0.43% of the $1.217(10)^6$ ft-lb of energy that previous calculations indicate may be absorbed without failure.

A collision which results in an impact of the generator against a rigid body at 100 mph will result in an impact energy of $2.25(10)^5$ ft-lb, 19.3% of the $1.217(10)^6$ ft-lb previously mentioned. Neither incident, therefore, will cause a failure of the system.

Tiedown provisions will be incorporated into the design to provide the necessary tiedown devices strong enough to support twice the loaded weight vertically and 10 times the loaded weight horizontally without exceeding the ultimate tensile strength of the material.

D. QUALITY CONTROL

Quality control throughout the manufacture of the system will be rigid to ensure RTG conformance to the specifications delineated after design finalization. All welds will be checked both destructively and nondestructively to ensure depth of penetration and overall weld quality.

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APPENDIX A

The thermoelectric analysis for the module of the SNAP 21A conversion system was performed by a digital program* developed by the Martin Company. The program input includes the conversion material properties, element lengths, electrical contact resistivities, and the hot and cold junction temperatures. The program output gives the thermoelectric efficiency and the number and size of thermoelectric elements required for a desired electrical output power.

The present couples in the SNAP 21A module consist of lead telluride and bismuth antimony telluride on the P leg and two segments of variably doped lead tin telluride on the N leg. The optimum temperature range of operation of the elements is determined from the figure of merit versus temperature curves (Figs. A-2, A-3). These curves were derived from the updated thermoelectric data curves (Figs. A-4 through A-15). The final couple configuration is shown in Fig. A-1. The temperatures at which the figure of merit curves intersect (T_M), which give the optimum overall temperature range, were determined to be 495° and 455° F for the N and P legs, respectively. The thermoelectric segment lengths were then calculated by the relations

$$\frac{\ell_2}{\ell_1} = \frac{k_2}{k_1} \frac{(T_M - T_C)}{(T_H - T_M)}$$

and

$$\ell_1 + \ell_2 = 0.525$$

where:

k_1 = average thermal conductivity of PbTe (or PbSnTe 0.062% PbI_2 for N leg) for the temperature range ($T_H - T_M$).

k_2 = average thermal conductivity of BiSbTe (or PbSnTe 0.03% PbI_2 for N leg) for the temperature range ($T_M - T_C$).

ℓ_1 = length of PbTe segment (or PbSnTe 0.062% PbI_2 for N leg).

ℓ_2 = length of BiSbTe segment (or PbSnTe 0.03% PbI_2 for N leg).

*"Development of a Thermoelectric Optimization Code, IBM 709,"
MND-P-2372, Martin Company.

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Fig. A-1. Final Couple Configuration

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Fig. A-2. Figure of Merit for N Leg

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Fig. A-3. Figure of Merit for P Leg

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Fig. A-4. Seebeck Coefficient

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Fig. A-5. Resistivity

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Fig. A-6. Thermal Conductivity

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Fig. A-7. Seebeck Coefficient

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Fig. A-8. Resistivity

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Fig. A-9. Conductivity

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Fig. A-10. Seebeck Coefficient

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Fig. A-11. Resistivity

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Fig. A-12. Thermal Conductivity

Fig. A-13. Seebeck Coefficient for P-Type

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Fig. A-14. Resistivity for P-Type

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Fig. A-15. Thermal Conductivity for P-Type

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T_H = hot junction temperature, 950° F

T_M = segment junction temperature, 455° F P leg
495° F N leg

T_C = cold junction temperature, 120° F

The contact resistivities adjusted for temperature and life degradation are taken as 420 and 780 microohms-cm² for the N and P elements, respectively. These values and the Seebeck voltage, resistivity and conductivity for the materials were used as code inputs. The resulting code output for the design point is given in Table A-I.

TABLE A-I

SNAP 21A Module Performance Characteristics

Junction Temperatures (° F)	
Hot	950
Cold	120
Contact Resistivities (microohm-cm ²)	
N leg	420
P leg	780
Thermoelectric Efficiency (%)	10.38
Load	
Voltage (volts)	2
Current (amp)	6
Resistance--external load (ohm)	0.333
Open Circuit Voltage (volts)	3.47
Internal Resistance (ohm)	0.245
Couples (No.)	16
Output Power (watts)	12
Diameter (in.)	
N element	0.319
P element	0.354

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APPENDIX B

COUPLE TEST ANALYSIS

Segmented couple test

N leg: BiSeTe, PbSnTe

P leg: BiSbTe, PbTe

Total length: 0.5 inch

Segment lengths (in.)

N leg: BiSeTe = 0.203

PbSnTe = 0.297

P leg: BiSbTe = 0.26

PbTe = 0.24

Junction temperatures (° F)

	N leg	P leg
Calculated	366	510
Measured		
Without insulation	371	553
With insulation	419	559

In order to find power analytically:

$$V_{oc} = N \alpha \Delta T$$

where:

V_{oc} = open circuit voltage

N = number couples = 1

α_T = integrated average of Seebeck coefficient for couple

$$\Delta T = T_{hot} - T_{cold} = 950^\circ \text{ F} - 115^\circ \text{ F} = 835^\circ \text{ F}$$

The integrated average of the Seebeck coefficient is given by:

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$$\bar{\alpha} = \frac{\sum_{i=1}^N \alpha_i - 1/2 (\alpha_1 + \alpha_N)}{N - 1}$$

where N = number of data points in N - 1 increments.

$$\bar{\alpha}_p = \frac{2107 - 135.5}{15} = 131.43 \mu\text{V}/^\circ\text{F}$$

$$= 236.57 \mu\text{V}/^\circ\text{C}$$

$$\bar{\alpha}_N = \frac{-1732 + 91}{15} = -109.4 \mu\text{V}/^\circ\text{F}$$

$$= -196.92 \mu\text{V}/^\circ\text{C}$$

$$\alpha_T = |\alpha_N| + \alpha_p = 196.92 + 236.57$$

$$= 433.49 \times 10^{-6} \text{ V}/^\circ\text{C}$$

Then

$$V_{oc} = (1) (433.49 \times 10^{-6}) (463) = 0.20 \text{ volt}$$

The integrated average of the resistivity is given by:

$$\bar{\rho} = \frac{\sum_{i=1}^N \rho_i - 1/2 (\rho_1 + \rho_N)}{N - 1}$$

where N = number of data points in N - 1 increments

$$\bar{\rho}_p = \frac{21,488 - 1710}{15} = 1318.5 \mu\Omega\text{-in.}$$

$$= 3349 \mu\Omega\text{-cm.}$$

$$\bar{\rho}_N = \frac{12,935 - 885}{15} = 863.3 \mu\Omega\text{-in.}$$

$$= 2040 \mu\Omega\text{-cm.}$$

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The shape factors are given by:

$$S = A/L$$

where:

A = element cross-sectional area

L = element length

$S_p = 0.306$ centimeter and $S_N = 0.15$ centimeter.

With the resistivities and shape factors, the element internal resistance is given by:

$$\begin{aligned} R_i &= \left[\rho_p / S_p + \rho_N / S_N \right] \\ &= 24.55 \times 10^{-3} \text{ ohm} \end{aligned}$$

The total contact resistance is given by:

$$\begin{aligned} R_c &= \left[C_p / S_p L_p + C_N / S_N L_N \right] \\ &= 4.2 \times 10^{-3} \text{ ohm} \end{aligned}$$

where:

$$\begin{aligned} C_p &= 780 \mu\Omega\text{-cm}^2 \\ C_N &= 420 \mu\Omega\text{-cm}^2 \end{aligned}$$

The total couple resistance is then

$$R_I = R_i + R_c = 28.7 \times 10^{-3} \text{ ohm}$$

The load resistance, R_L , will be varied to establish a peak power point analytically.

If R_L is 29.5×10^{-3} ohm

$$X = \frac{R_L}{R_I} = \frac{29.5}{28.7} = 1.025$$

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Then

$$V_L = \frac{X}{X+1} V_{oc} = 0.102 \text{ volt}$$

and

$$P_o = \frac{V_L^2}{R_L} = 344 \times 10^{-3} \text{ watt}$$

Repeating the process for $R_L = 28 \times 10^{-3} \text{ ohm}$

$$X = 0.976$$

$$V_L = 0.0989 \text{ volt}$$

$$P_o = 347 \times 10^{-3} \text{ watt}$$

For $R_L = 26 \times 10^{-3} \text{ ohm}$

$$X = 0.905$$

$$V_L = 0.095 \text{ volt}$$

$$P_o = 346 \times 10^{-3} \text{ watt}$$

For $R_L = 22 \times 10^{-3} \text{ ohm}$

$$X = 0.767$$

$$V_L = 0.0868 \text{ volt}$$

$$P_o = 341 \times 10^{-3} \text{ watt}$$

Plotting these values for couple output power shows a maximum power point for this case of $347 \times 10^{-3} \text{ watt}$, which agrees well with the peak experimental results of $333 \times 10^{-3} \text{ watt}$ (Figs. B-1 and B-2).

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Fig. B-1.

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Fig. B-2.

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C-1

APPENDIX C

STAINLESS STEEL-IRON STRUCTURAL ANALYSIS

The iron shoe on the P-leg is welded to the stainless steel. The purpose of the stainless steel is to force the iron to expand as the stainless steel does. The thermal expansion coefficient of the stainless steel is equal to that of the lead telluride. Thus, if the iron can be made to behave in the same way, there will be no thermal stresses induced in the lead telluride by a mismatch of thermal coefficients of expansion between the lead telluride and iron shoe which are joined at the hot junction by the tin telluride bonding agent. The configuration is shown.

To analyze this situation, it is assumed that symmetry exists and there are two iron shoes as shown in the sketch.

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For this situation, use is made of a solution existing NAA-SR-TDR6572. Figures C-1, C-2 and C-3 are reproduced from that report. To make the iron behave as the stainless steel, it would be desirable to have the iron yield at temperature but the stainless steel should not. The figures shown are based on equal moduli. However, since this is not the case, an equivalent modulus will be used. For the shear stress along the bond, the equivalent modulus is:

$$E_{\tau} = \left[2 - \frac{E_{\min}}{E_{\max}} \right] E_{\min}$$

Thus, if $E_{\min} = E_{\max} = E$; $E_{\tau} = E$ which is expected and if $E_{\min}/E_{\max}$ is very small, then $E_{\tau} \approx 2 E_{\min}$. This is reasonable to expect since the relationship between the elastic and shear moduli for a single material shows that the elastic modulus is approximately twice the shear modulus. Now from Fig. C-2 for direct loading in the members, for $C/\ell \geq 2$.

$$\left| \frac{\sigma y_1}{q} \right| + \left| \frac{\sigma y_2}{q} \right| = 1$$

And for compatibility of strain at the interface

$$\epsilon_1 + \epsilon_2 = (\alpha_1 - \alpha_2) \Delta T$$

or

$$\frac{\sigma y_1}{E_1} + \frac{\sigma y_2}{E_2} = (\alpha_1 - \alpha_2) \Delta T$$

since

$$q = E_e (\alpha_1 - \alpha_2) \Delta T$$

It may be written after reduction and equating the two expressions for $(\alpha_1 - \alpha_2) \Delta T$

$$E_e = \frac{E_1 E_2 \left(\frac{\sigma y_1}{\sigma y_2} + 1 \right)}{\frac{\sigma y_1}{\sigma y_2} E_2 + E_1}$$

and for $C/e \geq 2$ from Fig. C-2;

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C-3

Fig. C-1. Maximum Shear Stress

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Fig. C-2. Maximum Stress--Y Direction

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Fig. C-3. Maximum Stress--X Direction

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$$\frac{\sigma_{y1}}{\sigma_{y2}} = \frac{1 - a/l}{a/l} = Z$$

Therefore

$$E_e = \frac{E_1 E_2 (Z + 1)}{Z E_2 + E_1}$$

where

$$E_1 = E_{S/S}$$

and

$$E_2 = E_{Fe}$$

Now for the room temperature to 1000° F expansion

$$E_{S/S} = 24 \times 10^6 \text{ psi}$$

$$E_{Fe} = 12 \times 10^6 \text{ psi}$$

$$\alpha_{S/S} = 10 \times 10^{-6} \text{ in./in.} - ^\circ\text{F}$$

$$\alpha_{Fe} = 5 \times 10^{-6} \text{ in./in.} - ^\circ\text{F}$$

$$\sigma_{\text{yield } S/S} = 14,000 \text{ psi}$$

$$\sigma_{\text{yield } Fe} = 3000 \text{ psi}$$

Assume

$$E_e = 15 \times 10^6 \text{ psi}$$

Then to make the iron behave as stainless steel, the iron should yield but the stainless steel should not. This would be the required condition:

$$q = E_e \Delta \alpha \Delta T$$

$$q = 15 \times 10^6 \times 5 \times 10^{-6} \times 930 = 69.6 \times 10^3$$

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Then from Fig. C-2 at $\frac{\sigma y_1}{q} = \frac{14 \times 10^3}{69.6 \times 10^3} = 0.201$ and for $C/\ell > 2$

$$a/\ell = 0.80$$

Then

$$\frac{a}{\ell} = 0.80 = \frac{a}{a+t}$$

and

$$t = 0.0375 - 0.03 = 0.0075 \text{ inch}$$

The solution must be iterative since E_e was assumed; now using:

$$a = 0.03$$

$$\ell = 0.0375$$

$$Z = \frac{0.2}{0.8} = 0.25$$

$$E_e = \frac{(1.25)(12)(24)(10)^{12}}{(0.25)(12)(10^6) + (24)(10^6)} = 13.33 \times 10^6 \text{ psi}$$

Assuming

$$E_e = 13.33 \times 10^6$$

Then

$$\frac{\sigma y_1}{q} = 0.226$$

and

$$\frac{a}{\ell} = 0.78$$

$$t = 0.0084$$

and

$$Z = 0.282$$

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checking $E_e = 13.5 \times 10^6$ psi .'. close enough; therefore, to make the iron behave as the stainless steel, a maximum thickness of 0.008 inch is required. In addition for this to hold, the iron must be bonded continuously to the stainless steel and must be at dimensional equilibrium at room temperature. Also, the other condition assumed must be valid, namely, $C/\ell > 2$.

Checking for $C = 0.25$ and $\ell = 0.038$ shows that C/ℓ is greater than 2. Now for dimensional equilibrium at room temperature, it is required that both the stainless steel and iron be yielded during cool down from welding temperatures. Therefore:

$$\begin{aligned} q &= E_e \Delta \alpha \Delta T \\ &= -13.5 \times 10^6 \times 5 \times 10^{-6} (2750 - 70) = -181 \times 10^3 \end{aligned}$$

Then from Fig. C-2 at $a/\ell = 0.78$

$$\frac{\sigma y_1}{q} = -0.221; \sigma y_1 = 40,000 \text{ psi}$$

$$\frac{\sigma y_2}{q} = 0.779; \sigma y_2 = -141,000 \text{ psi}$$

Therefore, both members have yielded during this cycle and are at dimensional equilibrium at room temperature. This satisfies the first requirement. This method can be used to slave the iron to the stainless steel if a minimum number of temperature cycles are imposed upon the iron. It should be noted that this analysis only pertains to a metal-lurgically bonded P-leg; if there is no bond on the P-side, then the thermal expansion problem is nonexistent.

HOLDDOWN TABS FOR SHIELDING

The holddown tab cross section is shown in the following sketch.

The pressure vessel internal radius is $R_i = 4.57$ inches. The contact is then

$$\begin{array}{r} 4.785 \\ -4.57 \\ \hline 0.215 \text{ inch} \end{array}$$

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C-9

1. Shear of Tab

$$W \approx 500 \text{ pounds at } 6 \text{ g} = 3000 \text{ pound}$$

$$\tau = \frac{3000}{N R \theta t}$$

where

$$N = \text{number of tabs} = 8$$

$$R = \text{radius of contact} = 4.57 \text{ inches}$$

$$\theta = \text{angle of tab} = 60^\circ$$

$$t = \text{thickness of tab} = 1/4 \text{ inch}$$

Then

$$\tau = \frac{3000}{2.8 \times 4.57 \times \frac{\pi}{3} \times \frac{1}{4}} = \frac{9000}{28.7} = 314 \text{ psi}$$

This could take 60 g with no trouble.

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2. Crushing the Tab

The crushing area is $N (R_o^2 - R_i^2) \frac{\theta}{2}$

$$A_c = 8 (4.785^2 - 4.57^2) \frac{\pi}{6}$$

$$A_c = 8.175 \text{ in.}^2$$

The crushing stress is then

$$\sigma_b = \frac{3000}{8.175} = 367 \text{ psi at } 6 \text{ g loading}$$

This will also withstand 60 g loading

MODULE BELLOWS

The next analysis is made of the module bellows under design conditions of 1/2 atmosphere pressure and 1/8 inch deflection. For the configuration shown, the following quantities are defined:

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Now from figure of bellows

$$a = \frac{1.053}{2 \times 9} = 0.0585 \text{ inch}$$

$$\bar{R} = \frac{4.875 + 0.25}{2} = 2.563 \text{ inch}$$

$$t = 0.005 \text{ inch}$$

$$N = 4 \text{ convolutions minimum}$$

$$\delta = 1/8 \text{ inch design deflection}$$

$$p = 7 \text{ psi design pressure (external)}$$

$$h = 0.125 \text{ in.}$$

$$\text{Defining } X = 0.778 \sqrt{Rt} = 0.0881 \text{ in. (characteristic length)}$$

$$\frac{h}{X} = 1.42 \text{ say } 1.5$$

$$\frac{a}{h} = 0.468$$

$$\text{From Fig. C-4 at } \frac{a}{h} = 0.468 \text{ and } \frac{h}{X} = 1.5$$

$$C_2 = 0.78$$

The bending stress at 1/8-inch deflection is obtained from

$$\sigma_{bd} = \frac{E}{2\pi(1-\nu^2)} \cdot \frac{t}{h^2 C_2} \cdot \frac{\delta}{N}$$

$$\sigma_{bd} = \frac{24 \times 10^6}{6.28(1-0.09)} \cdot \frac{0.005}{(0.125)^2 (0.78)} \cdot \frac{0.125}{4} = 53,900 \text{ psi}$$

For 321 stainless steel this load may be applied 250 times at 1200° F or 500 times at 1000° F. Therefore, the maximum damage factor is

$$DF = \frac{50}{250} = 0.2 \text{ compared to an allowable of } 0.8$$

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Fig. C-4. Deflection Stress Coefficient

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For any number of convolutions greater than four, this stress reduced considerably.

For the membrane stresses, using Fig. C-5 at $\frac{a}{h} = 0.468$

$$\frac{\sigma_{mp}^t}{p_a} = 2.1$$

$$\frac{\sigma_{bp}^t}{6 p_a} = 1.0$$

Then

$$\sigma_{mp} = \frac{7 \times 0.0585 \times 2.1}{0.005} = 172 \text{ psi}$$

$$\sigma_{bp} = \frac{6 \times 7 \times 0.0585^2}{0.005^2} = 5750 \text{ psi}$$

These values are very low and present no problem.

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Fig. C-5. Pressure Stress Coefficients

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APPENDIX D

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Reliability Estimate of Thermoelectric Generator

Failure Rate Data				$R_1(t)$	$R_2(t)$
Couple Hours	Observed Failures	λ_1 (90% confidence) %/1000 hr	λ_2 (50% confidence) %/1000 hr		
17,200,000	1	0.0226	0.0098	0.853	0.934

*Thermoelectric Generator Reliability Configuration

Reliability Model

$$R_1(t) = e^{-n \lambda_1 t} = e^{-16 (0.0226 \times 10^{-5}) (4.38 \times 10^4)} = 0.853$$

$$R_2(t) = e^{-n \lambda_2 t} = e^{-16 (0.0098 \times 10^{-5}) (4.38 \times 10^4)} = 0.934$$

where:

$$n = 16$$

$$\lambda_1 = 0.0226\%/1000 \text{ hours}$$

$$\lambda_2 = 0.0098\%/1000 \text{ hours}$$

$$t = 5 \text{ years}$$

*Criteria for thermoelectric generator success: no functional block may fail

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Reliability Estimate for dc-to-dc Converter

Part/Type	Circuit Symbol	Duty Cycle	Operated Stress	T_N or T_{Rise}	MIL-HNBK-217		Minuteman	
			Rated Stress		λ_i (%/1000 hr)	Relative Complexity (%)	λ_i (%/1000 hr)	Relative Complexity (%)
Capacitor/solid tant	C_1	1.0	0.5		0.034	4.25	0.0035	1.624
Capacitor/solid tant	C_2	1.0	0.5		0.034	4.25	0.0035	1.624
Capacitor/paper	C_3	1.0	0.5		0.005	0.625	0.0015	0.695
Capacitor/paper	C_4	1.0	0.5		0.005	0.625	0.0015	0.696
Diode	CR_1	1.0	0.1	0.36	0.010	1.25	0.01	4.649
Diode	CR_2	1.0	0.1	0.36	0.020	2.50	0.003	1.392
Diode	CR_3	1.0	0.1	0.36	0.020	2.50	0.003	1.392
Resistor/film	R_1	1.0	0.1		0.021	2.625	0.0018	0.835
Resistor/film	R_2	1.0	0.1		0.021	2.625	0.0018	0.835
Resistor/film	R_3	1.0	0.1		0.021	2.625	0.0018	0.835
Resistor/WW	R_4	1.0	0.1		0.102	12.75	0.0007	0.325
Resistor/film	R_5	1.0	0.1		0.021	2.625	0.0018	0.835
Resistor/film	R_6	1.0	0.1		0.021	2.625	0.0018	0.835
Resistor/film	R_7	1.0	0.1		0.021	2.625	0.0018	0.835
Resistor/film	R_8	1.0	0.1		0.021	2.625	0.0018	0.835
Resistor/film	R_9	1.0	0.1		0.021	2.625	0.0018	0.835
Resistor/film	R_{10}	1.0	0.1		0.021	2.625	0.0018	0.835
Resistor/film	R_{11}	1.0	0.1		0.021	2.625	0.0018	0.835

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Reliability Estimate for dc-to-dc Converter (continued)

Part/Type	Circuit Symbol	Duty Cycle	Operated Stress	T _N or T _{Rise}	MIL-HNBK-217	Relative Complexity (%)	Minuteman	Relative Complexity (%)
			Rated Stress		λ_i (%/1000 hr)		λ_i (%/1000 hr)	
Resistor/film	R ₁₂	1.0	0.1		0.021	2.625	0.0018	0.835
Transistor	Q ₁	1.0	0.1	0.36	0.020	2.50	0.003	1.392
Transistor	Q ₂	1.0	0.1	0.36	0.020	2.50	0.003	1.392
2 transistors	Q ₃	1.0	0.1	0.36	0.040	5.00	0.006	2.784
2 transistors	Q ₄	1.0	0.1	0.36	0.040	5.00	0.006	2.784
Transistor	Q ₅	1.0	0.1	0.36	0.020	2.50	0.003	1.392
Transistor	Q ₆	1.0	0.1	0.36	0.020	2.50	0.003	1.392
Transistor	Q ₇	1.0	0.1	0.36	0.020	2.50	0.003	1.392
Transistor	Q ₈	1.0	0.1	0.36	0.020	2.50	0.003	1.392
Transformer	T ₁	1.0		0	0.050	6.25	0.050	23.200
Transformer	T ₂	1.0		0	0.050	6.25	0.050	23.200
54 solder joints					0.039	4.875	0.039	18.097
Totals				0.800			0.2155	

Remarks

$$R_{217} (5 \text{ yr}) = e^{-t \sum_i \lambda_i} = 0.705$$

$$R_{MM} (5 \text{ yr}) = e^{-t \sum_i \lambda_i} = 0.910$$

*Part selection is not finalized and parts will be selected to achieve optimum reliability versus performance tradeoff.

**Solder joints based on 2/3 total leads $\approx$ number of solder joints; all parts mounted on printed circuit board.

***Ambient temperature = 50° C.

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APPENDIX E

SNAP 21A HYDROSTATIC PRESSURE TEST SPECIFICATION

I. Equipment to be Tested

- (1) One each SNAP 21A pressure vessel (Drawing 453A0300000).
- (2) Two each SNAP 21A empty fuel capsules (Drawing 453A1200012).

II. Test Facility

Hydrostatic Test Facility, Naval Research Laboratory (NRL), Washington, D.C.

- (1) SNAP 21A Pressure Vessel--15 inch, 10,000 psi test tank under the cognizance of NRL Code 5506.1.
- (2) SNAP 21A Fuel Capsule--5-inch, 15,000 psi test tank under the cognizance of NRL Code 5540.

III. Schedule

October 12 through October 23, 1964.

IV. Objectives

The objectives of these hydrostatic tests are to demonstrate the integrity of the SNAP 21A pressure vessel and fuel capsule. Specifically, the objectives are to demonstrate that:

- (1) The pressure vessel will not collapse or experience yielding of the material under a 10,000 psi external pressure.
- (2) The unit will not leak with all Viton A O-rings installed.
- (3) The unit will not leak with the outer O-rings installed.
- (4) The unit will not leak with only the inner O-rings installed.

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- (5) The fuel capsules will not collapse or yield under a 15,000 psi external pressure.
- (6) The fuel capsules will not leak.

V. General

The hydrostatic test of the SNAP 21A pressure vessel is to be conducted in three phases. The first phase will consist of a five- to six-day steady-state test at 10,000 psi external pressure with all O-rings installed. After Phase 1 has been completed, the inner O-ring will be removed and the system subjected to 10,000 psi for 24 to 48 hours. The inner O-ring will then be removed and the outer O-ring replaced for a final 10,000 psi test. The SNAP 21A pressure vessel will be delivered with humidity sensors installed as an assistance in determining pressure tightness.

The fuel capsule test will also be conducted in three phases. The first one will consist of installing both fuel capsules within the pressure test facility, subjecting them to a 10,000-psi external pressure and then removing the capsules for dimensional measurements. Phases 2 and 3 will consist of individually testing the fuel capsules to 15,000-psi external pressure after which the fuel capsules will again be measured for evidence of yielding of the material. After the fuel capsules have been returned to Martin, they will be cut apart for metallographic examination of the weld area. It is understood that the 15,000-psi pressure test facility is under the cognizance of NRL Code 5540 but that necessary test arrangements and supervision of the test will be under the cognizance of Code 5506.1.

VI. Preparation of Pressure Vessel for Test

- (1) Pour one-half pound of water soluble dye (A679) into the NRL pressure tank.
- (2) Remove the SNAP 21A pressure vessel from the shipping pallet. Store the shipping pallet for future use. Install the SNAP 21A pressure vessel in the NRL test tank (weight ~ 175 pounds).
- (3) Secure the NRL test tank head in place. The test is ready to start.

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- (11) Cut the insulation from the humidity sensor leads, take a set of readings and then determine if there is evidence of leakage before proceeding further.
- (12) Remove the main flange bolts and the inner O-ring. Do not disturb the outer O-ring. Replace the top of the SNAP 21A pressure vessel in preparation for Phase 2 of the test.

B. Phase 2 (outer O-rings installed)

All steps spelled out in Phase 1 apply with the exception that the SNAP 21A pressure vessel shall be maintained at 10,000 psi for a minimum of 24 hours.

C. Phase 3 (inner O-rings installed)

All steps spelled out in Phase 1 apply with the exception that the SNAP 21A pressure vessel shall be maintained at 10,000 psi for a minimum of 24 hours.

VIII. Preparation for Test of the SNAP 21A Fuel Capsules

- (1) Measure the diameter and the length of the fuel capsules at a minimum of six referenced positions. These dimensions will be used for comparative values later in the test to determine whether there has been any yielding of the capsule material.
- (2) Install the SNAP 21A capsules in the pressure test facility. The test is ready to begin.

IX. Hydrostatic Test of the Capsules

- (1) Prior to the start of the test, NRL operators will cycle the test vessel as required to purge the test tank of all air.
- (2) Increase the pressure to 10,000 psi in 1000-psi steps. At each 1000-psi step, stop the pressure increase and monitor the pressure for five minutes for evidence of leakage.
- (3) Cycle the pressure between 0 and 10,000 psi three times.
- (4) Increase the pressure to 10,000 psi and hold for two to four hours.

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- (5) Reduce the pressure to zero, remove the fuel capsules from the test vessel and take dimensional measurements.
- (6) Install one of the capsules in the pressure test vessel and test in accordance with Steps (1) through (5) with the exception that the test pressure is to be 15,000 psi.
- (7) Install the other fuel capsule and test in accordance with Step (6).

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APPENDIX F

DC-TO-DC CONVERTER DESIGN FORMULAS

Total transistor losses P_t (watts)

$$P_t = P_D + P_S + P_C$$

where:

$$P_D = \text{base drive losses} = \frac{E_B I_C}{B}$$

$$P_S = \text{switching losses} = \frac{2 V_S I_C f t_S}{3}$$

$$P_C = \text{power conduction losses} = V_{CE \text{ sat.}} I_C (1 - 2 f t_S)$$

$$E_B = \text{base emitter voltage}$$

$$I_C = \text{collector current}$$

$$B = \text{d-c current gain}$$

$$V_S = \text{source voltage}$$

$$f = \text{frequency (cps)}$$

$$t_S = t_f + t_R = \text{switching time (sec)}$$

$$t_F = \text{fall time of square wave}$$

$$t_R = \text{rise time of square wave}$$

$$V_{CE \text{ sat.}} = \text{collector-emitter saturation voltage}$$

Rectifier losses P_R (watts)

$$P_R = E_R I_L$$

where:

$$E_R = \text{voltage across rectifier diodes}$$

$$I_L = \text{load current}$$

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Power transformer losses P_T (watts)

$$P_T = P_i + P_{cu}$$

where:

$$P_i = \text{magnetic iron losses} = \int_{B_1}^{B_2} H dB$$

(P_i is taken from manufacturer's data sheets)

$$P_{cu} = \text{magnetic wire losses} = I_L^2 R$$

H = magnetomotive force

B = flux density

R = resistance of magnetic wire

Optimum frequency

Optimum frequency occurs when

$$P_T = P_t = P_D + \frac{2 V_S I_c f t_s}{3} + V_{CE \text{ sat.}} I_c (1 - 2 f t_s)$$

$$f = \frac{P_T - P_D - V_{CE \text{ sat.}} I_c}{2 I_c t_s \left(\frac{V_S}{3} - V_{CE \text{ sat.}} \right)}$$

Transformers

Transformers are designed for square wave operation using the following relationship:

$$E_1 = 4 N A f B 10^{-8}$$

where:

N = primary turns (no.)

A = area (cm^2)

B = flux density in gauss

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